

Sound Radiated from Turbulent Flow over Two and Three-Dimensional Surface Discontinuities

Manuj Awasthi

Dissertation submitted to the faculty of the Virginia Polytechnic Institute and State University in partial fulfillment of the requirements for the degree of Doctor of Philosophy in Aerospace Engineering

William J. Devenport

William N. Alexander

Stewart Glegg

Kevin T. Lowe

Eric G. Paterson

September 23, 2015

Blacksburg, Virginia

Keywords: Forward step noise, gap noise, swept forward step noise, circular embossment noise, surface pressure fluctuations and unsteady surface force

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ABSTRACT

Measurements have been performed to understand the sound source mechanism in turbulent boundary layer flow over two and three-dimensional surface discontinuities whose height is smaller than the incoming boundary layer thickness. The work was performed in two different types of boundary layers: a wall-jet flow and a conventional high Reynolds boundary layer. In the wall-jet flow, measurements of far field sound from two-dimensional forward facing steps, gaps with rounded corners and swept forward facing steps with rounded corners were made. The sound from a forward facing step is shown to exhibit effects of non-compactness. Rounding the step corner results in consistent drop in sound levels but the directivity of the sound field remains unchanged. The sound from gaps is dominated by the forward step component and remains unaffected by rounding of the backward step portion. The sound from swept forward facing steps was found to approximately obey an acoustic sweep independence principle up to a sweep angle of 30° when the spanwise inhomogeneity in the flow is accounted for using a simple source distribution model. Sweep independence is also observed for steps with corner rounding radii up to 25% of the step height.

The work performed in the high Reynolds number boundary layer included measurements on forward facing steps with rounded corners and a three-dimensional circular embossment with the same height as the forward step. The highest Reynolds number based on discontinuity height achieved in this work was approximately 93,000. The results show that rounding the forward step corner has the same qualitative effect on far field sound as in the wall-jet boundary layer. Quantitatively, for similar boundary layer edge velocity the sound is higher than in the wall-jet flow. The near field measurements show that the separation bubble downstream of the step shrinks as the step corner is rounded while the bubble upstream remains unaffected by it. The unsteady surface force in the lower half of the vertical face of the step was found to be independent of corner rounding. The force on the downstream surface shows similar character within the separation bubble for each rounding but decays faster with increasing downstream distance due to reduced bubble size. The unsteady force measurements were applied to the theory of Glegg *et al.* (2014) and the resultant of the unsteady forces on the vertical face and downstream surface placed at the top corner of the step is shown to qualitatively describe the far field sound. The acoustic sweep independence principle was applied to the far field sound from the circular embossment and it has been shown that the sound from the three-dimensional geometry can be predicted with reasonable accuracy using sound from a two-dimensional forward step with the same span.

Acknowledgements

I would like to begin by thanking my academic adviser Dr. William Devenport. It has been a privilege and an honor to be mentored by him over the years. His passion for scientific research and his dedication to teaching is unparalleled. I have grown tremendously as a researcher and as a person under his guidance. I could not have asked for a better teacher and he is someone I hope to emulate one day. Thank you Dr. Devenport.

I am also thankful to my doctoral committee who, despite of their busy schedules, have taken the time to help me not only improve the quality of this work but also my understanding of the fields of fluid dynamics and aeroacoustics. I would like to thank Dr. Stewart Glegg for his numerous comments on this dissertation and for laying the theoretical foundation for much of the work described here. His contributions have helped improve the quality of this work greatly. I also thank Dr. Nathan Alexander for his help with the phased array measurements and for his critique of the measurements and their interpretation. I would also like to thank Dr. Todd Lowe and Dr. Eric Paterson for their inputs on this dissertation and for helping me to understand various aspects of turbulent flows. I would also like to thank the Office of Naval Research and Dr. Ron Joslin for their support through grants N00014-09-1-0315 and N00014-12-1-0374.

I must thank the members of Lab-7/CREATE who not only provided help with measurements from time to time but also provided an excellent work environment. I would specifically like to thank Nick Molinaro whose dedicated help during the measurements in Virginia Tech Stability Wind Tunnel was indispensable towards the completion of this dissertation. I would also like to thank Ian Clark for his help with the measurements in the wall-jet facility. I would also like to thank Liselle Joseph, Kenneth Brown, Henry Murray and Dan Cadell for making my graduate student life much more enjoyable.

I am also thankful to the entire Virginia Tech Stability Wind Tunnel staff including Dr. Aurelien Borgoltz, Bill Oetjens and Tim Meyers for their assistance with the wind tunnel operation and measurements. I would also like to thank Skyler Bryan for making the trip from warm and sunny Florida to freezing Blacksburg to help with the measurements in the Stability Wind Tunnel. I would also like to thank the entire staff at AOE machine shop for their help with fabrication of experimental hardware. Specifically, I owe a big thank you to Cameron Hollandsworth for bringing the designs on paper to life through his craftsmanship.

Outside of my academic circle there are several friends that also deserve a mention. I would like to thank Manish Upreti, Aniruddha Chaudhary, Aashish Bhattarai, Siddharth Sharma and Apurva Singh for their friendship and support over the years.

Last but not the least, I would like to thank my family. I would like to thank my late father Rajmitra Awasthi and my mother Sadhna Awasthi who dedicated their lives to help me pursue my dreams. Their sacrifice and commitment to my education has made me all I am today. I would also like to thank my brother Tanuj Awasthi for being my strength and taking care of things back home in my absence. I would also like to thank my little sister Megha Awasthi for being supportive and for bringing joy to my life. I would also like to thank my cousins Meenakshi Awasthi, Arjun Awasthi, Ishani Awasthi and Raaghav Awasthi for treating me like a big brother and for providing some unforgettable sibling moments which I have missed during my time away from home. I will always be indebted to my uncle Dr. Sanjay Awasthi and my aunt Dr. Sangeeta Awasthi for providing me a home away from home and for treating me like their own child. None of this would have been possible without their love and encouragement which has helped me overcome many obstacles. I dedicate this dissertation to all of you.

Thank you all,

Manuj Awasthi

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Nomenclature

Roman Lower Case

a	Width of a rectangular strip sensor
b	Width of nozzle opening in Virginia Tech anechoic wall-jet facility
c	Speed of sound
d	Separation between microphones in linear surface pressure array
f	Frequency in Hz
f_S	Non-dimensional frequency
$f_{destruct}$	Frequency in Hz where destructive interference occurs
h	Vertical height of discontinuity
h^+	Step height in wall units
i	Imaginary number
k	Acoustic Wavenumber
n_b	Number of binned records
p	Near field or far field fluctuating pressure
$p^\sim$	Attenuated surface pressure fluctuation
p_0	Ambient pressure
$p_{0,ref}$	Total free stream pressure
p_{ref}	Reference pressure used for normalization of far field sound, static free stream pressure
p_{rms}	Root mean square of surface pressure fluctuations
p_{SC}	Settling chamber pressure in Virginia Tech Stability Wind Tunnel
p_∞	Free stream static pressure
q_∞	Free stream dynamic pressure
r	Rounding radii of forward step corner
r_D	Radius of circular embossment

r'	Source to observer distance for swept steps and circular embossment
r_1	Rounding radii of backward step corner in gap configuration
r_2	Rounding radii of forward step corner in gap configuration
u_τ	Friction velocity
w	Gap width
w_{strip}	Sensitivity function for rectangular strip
x_r	Reattachment length downstream of forward step/circular embossment
x_s	Separation length upstream of forward step/circular embossment
x_{sD}	Point at which flow separates downstream of the vertical face of rounded step
y_r	Reattachment length on the vertical face of forward step
$y_{1/2}$	Half-height of wall-jet boundary layer
$\mathbf{x}(x_1, x_2, x_3)$	Far field coordinate system used for sound predictions
$\mathbf{y}(y_1, y_2, y_3)$	Near field coordinate system used for sound predictions
x, y, z	Coordinate system used for near field measurements
x', y', z'	Near field coordinate system for swept forward facing steps
x_n, y_n, z_n	Wall-jet coordinate system with origin at the nozzle exit
x_a, y_a, z_a	Surface pressure array local coordinate system

Roman Upper Case

AF	Array factor
C_f	Skin friction coefficient
C_p	Pressure coefficient
C_{ps}	Correction factor for settling chamber pressure in Virginia Tech Stability Wind Tunnel
F	Unsteady force acting on the step corner
F_I	Acoustic intensity factor

G	Green's function
H	Wavenumber Response Function of a microphone
I	Total mean square acoustic pressure
L	Length Scale
M	Mach number
N	Number of microphones in an array
Q	Acoustic source strength
R	Source to observer distance
R'	Source to observer distance for moving microphones in Virginia Tech anechoic wall-jet facility
R''	True source to observer distance for swept forward steps
Re_h	Reynolds number based on discontinuity height
Re_δ	Reynolds number based on boundary layer thickness
Re_θ	Reynolds number based on momentum thickness
Re_{xn}, Re_j, Re_m	Reynolds numbers based on different velocity and length scales in Virginia Tech anechoic wall-jet facility
S_{ref}	Sensitivity of reference microphone in calibrations
S	Sensitivity of a microphone
S_0	Constant used for normalization of wavenumber response function
T_0	Ambient temperature
U	Velocity scale
U_c	Convection velocity
U_j	Velocity in the nozzle exit plane of Virginia Tech anechoic wall-jet facility
U_n	Velocity normal to the face of the discontinuity
U_∞	Free stream velocity
W	Span width

$W(\kappa)$	Wavenumber response function of linear surface pressure microphone array
$W_{strip}(\kappa)$	Wavenumber response function of rectangular strip
X, Y, Z	Coordinate system for beamforming results

Greek Lower Case

α	Sweep angle
β_n	Directionality term for circular embossment
γ	Coherence
δ	Boundary layer thickness, uncertainty in a variable
δ^*	Displacement thickness
θ	Angular location of observer, momentum thickness
θ'	Angular location of observer for swept forward facing steps
κ	Wave-vector of surface pressure fluctuations
λ	Acoustic Wavelength
μ	Dynamic Viscosity
ν	Kinematic viscosity
ρ	Density of air, correlation of surface pressure fluctuations
ρ_∞	Free stream density
ϕ_{pp}	Spectral density of surface pressure fluctuations
$\phi_{pp}^\sim$	Spectral density of attenuated surface pressure fluctuations
ϕ_{ij}	Cross-spectral density between microphone signals
ψ	Azimuthal location of observer
ω	Radial frequency in rad/s

Greek Upper Case

Δ	Difference between two quantities

Σ	Summation
Φ_{pp}	Spectral density of far field acoustic pressure from surface discontinuity
Φ_{nn}	Spectral density of far field acoustic pressure in absence of surface discontinuity
Φ_{pps}	Non-dimensional spectral density of far field sound
$\Phi(\kappa_x, \kappa_z, \omega)$	Wavenumber-frequency spectrum of surface pressure fluctuations

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1 Introduction

1.1 Motivation

The surface of any aerodynamic or hydrodynamic vehicle is seldom continuous due to structural features such as uneven plate joints, doors, hatches, control surfaces, etc. These surface discontinuities disturb the incoming turbulent boundary layer by enhancing turbulence levels and can also lead to separation of boundary layer. This separated boundary layer reattaches to the surface some distance downstream and then travels some distance before reverting to its original state. The separation and subsequent reattachment process of the boundary layer is known to result in high levels of surface pressure fluctuations due to the modification of the turbulent sources within the boundary layer. The surface pressure fluctuations which are directly related to the unsteady loading on the surface represent dipole acoustic sources which in low Mach number flows are the dominant sources of radiated far field sound (Curle (1955)). Thus, an understanding of the manner in which a surface discontinuity modifies the incoming turbulent boundary layer to produce surface pressure fluctuations (or unsteady surface loading) is the key to establish the fundamental sound source in these flows. The details of the mechanism which relates the near field events to the far field sound in flow over surface discontinuities can aid in establishing tolerances and guidance for design of quieter hydrodynamic or aerodynamic surfaces. At present, the understanding of sound source mechanism in flow over surface discontinuities is poor due to the complex nature of separated flows and their interaction with the surface geometry and therefore more insight on this subject is needed.

The surface discontinuities found on practical surfaces are generally much smaller than the thickness of the incoming boundary layer and the Reynolds number of the flow can be quite large. The geometry of these discontinuities is different depending upon the reason for their existence. For example, a structural joint held together through some bonding material will result in an uneven surface which can be either a forward facing or a backward facing step. On the other hand, the seam between a control surface and the lifting surface is a combination of the two i.e. a gap type geometry. Furthermore, surface discontinuities can often be three-dimensional with a complex geometry such as a door or a hatch on the vehicle which could further complicate the flow field and the sound source mechanism.

The present work aims to better understand the sound source mechanism from turbulent boundary layer flow over variety of surface discontinuities through measurements of both far field sound and near field quantities of interest. The measurements have been made in two different facilities: Virginia Tech anechoic wall-jet facility and Virginia Tech Stability Wind Tunnel. The surface discontinuities include both two and three-dimensional geometries immersed in a turbulent boundary layer. On the two-dimensional side measurements have been made on flow over forward facing steps with and without rounding of the external corner, gaps with corner rounding of the backward step portion and gaps with corner rounding of the forward step portion. The three-dimensional geometries that have been considered include swept forward facing steps with and without rounding of the corner and a low aspect ratio (vertical height to diameter ratio) wall mounted circular embossment.

1.2 Background

The problem of sound radiated from turbulent flow over surface discontinuities is part of a broader class of problems which involve understanding the sound generated due to presence of rigid boundaries in the flow region. The theoretical foundation of the problem was first laid down by Curle (1955) who extended Lighthill's (1952) acoustic analogy to include the effects of rigid boundaries in the flow. The solution to Curle's (1955) equation in frequency domain as given by Goldstein (1976) is

$$p(\mathbf{x}, \omega) = - \int_S p(\mathbf{y}, \omega) \frac{\partial G(\mathbf{x}, \mathbf{y}, \omega)}{\partial n} dS(\mathbf{y}) + \int_V T_{ij}(\mathbf{y}, \omega) \frac{\partial^2 G(\mathbf{x}, \mathbf{y}, \omega)}{\partial y_i \partial y_j} dV(\mathbf{y}) \quad (1-1)$$

here the left hand side is the acoustic pressure in the region outside the flow which is a function of some far field coordinate $\mathbf{x}$ and frequency ω . The right hand side of the equation which provides the solution for the far field pressure involves an integral over the surface of the body S and an integral over the volume of the flow region V both of which are functions of $\mathbf{y}$ which is the near field, or flow, coordinate. Note that both $\mathbf{x}$ and $\mathbf{y}$ are vectors in the above equation. Each integral on the right hand side above consists of derivative of a function $G(\mathbf{x}, \mathbf{y}, \omega)$ which is known as the Green's function and is the solution to the inhomogeneous wave equation. The surface integral in the above equation involves knowledge of near field pressure $p(\mathbf{y}, \omega)$ and the Green's function while the volume integral involves the knowledge of T_{ij} which is known as the Lighthill stress tensor and is a result of fluctuating stresses within the flow region. The two integrals above represent two different types of acoustic sources with the surface integral representing a dipole source distribution on the rigid surface and the volume integral representing quadrupole type source distribution external to the surface i.e. within the flow region. As was shown by Lighthill (1952) the dipole sources scale on the 6th power of flow Mach number while the quadrupole type sources scale on the 8th power of Mach number. This means that in low Mach number flows, which is the flow regime of interest here, the quadrupole source term is much less efficient than the dipoles source term and therefore can be dropped. This leads to the following expression for the radiated sound in low Mach number flows over rigid surfaces

$$p(\mathbf{x}, \omega) = - \int_S p(\mathbf{y}, \omega) \frac{\partial G(\mathbf{x}, \mathbf{y}, \omega)}{\partial n} dS(\mathbf{y}) \quad (1-2)$$

This equation essentially means that for the problem at hand we are interested in the near field pressure on the surface i.e. the unsteady loading and a suitable Green's function which describes the scattering and propagation of this loading to the far field sound. Since the unsteady surface pressure is a consequence of the events within the boundary layer one has to gain an understanding of how the flow field is being modified by the presence of a rigid body in order to understand the far field sound behavior. It is because of this reason that in the next few sections we will review past studies on turbulent boundary layer flow over surface discontinuities as well as studies on sound radiation from these flows.

1.3 Past Studies on Sound Radiation from Two-Dimensional Surface Discontinuities

In this section we will consider the studies performed in the past on radiated sound from flow over two-dimensional surface discontinuities which include forward facing steps, forward facing steps with rounded corners, and gaps. Of particular interest here are studies where the vertical height of the discontinuity h is less than the incoming boundary layer thickness δ and the flow Mach number is low. Since an understanding of the flow structure is important to understand the behavior of the far field sound the following review also includes studies performed towards understanding the flow structure in presence of these geometries.

1.3.1 Forward Facing Step

The forward facing step is a commonly found geometry on the surfaces of aerodynamic vehicles and is a benchmark case of complex separated-reattaching boundary layer flow. The flow field around the forward facing step has been investigated by many authors in the past and some of its flow characteristics are well established. These flow characteristics have been demonstrated in Figure 1-1 which shows a time-mean picture of the flow over a forward facing step. The figure shows a turbulent boundary layer of thickness δ approaching a forward step of height h from the left. As the boundary layer nears the step it separates from the surface at some distance x_s upstream of the vertical face of the step due to insufficient momentum to overcome the adverse pressure gradient which develops in front of the step. This separation leads to formation of a separation bubble in front of the step with recirculating flow within it and a faster moving shear (mixing) layer on top of it with a dividing streamline separating the two regions. The flow

reattaches at some height above the surface on the step face and then separates again due to the sharp corner of the step forming another separation bubble on the top surface of the step. The dividing streamline between mixing layer and recirculating flow impinges on the top surface of the step at some distance x_r from the vertical face of the step. This distance x_r is called the reattachment point. Note that the upstream separation, reattachment on the face and the downstream surface are all unsteady in nature and can depend on variety of parameters such as step height to boundary layer thickness (h/δ) and Reynolds number based on step height (Re_h). The only fixed separation point in the figure below is the salient corner of the step.

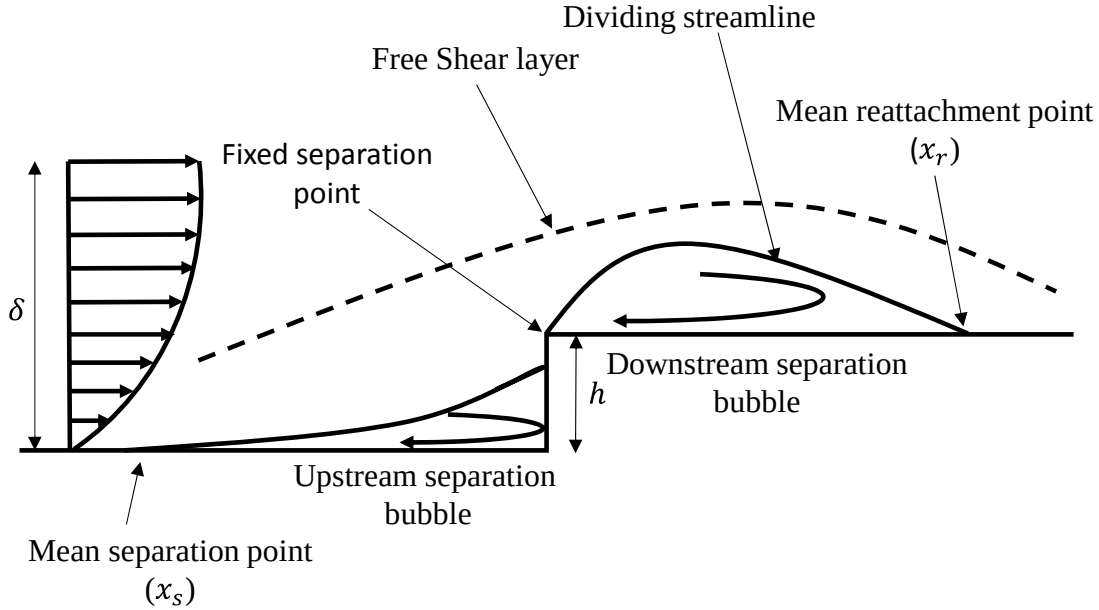


Figure 1-1 Mean flow features around a forward facing step.

The flow separation and reattachment in the vicinity of the step leads to enhanced turbulence levels (compared to an attached boundary layer) in this region which results in significant pressure fluctuations that are directly related to the far field sound. Previous measurements and computations (see for e.g. Farabee and Casarella (1984, 1986) and Ji and Wang (2010)) have shown that the behavior of the pressure fluctuations on the downstream surface of the step is different than on the upstream surface. This difference is due to the two separation bubbles being inherently different from one another. Therefore, before we discuss the pressure fluctuations it is worth spending some time investigating the behavior of the two separation bubbles.

Table 1-1 lists the mean separation lengths (x_s) and reattachment lengths (x_r) found by several studies on flow over forward facing steps with different h/δ ratios and Reynolds number. An inspection of this table reveals that while the length of the upstream separation bubble is generally reported to be between 1-2 step heights regardless of the flow conditions there is a significant variability in the downstream reattachment lengths reported in the past literature. Furthermore, it seems that in most cases the reattachments lengths tend to be larger than the upstream separation length. This variability shows that the downstream separation bubble is more sensitive to the flow conditions. The sensitivity of the separation and reattachment lengths is demonstrated in Figure 1-2 which shows the two lengths plotted as a function of h/δ (Figure 1-2 (a) and (b) and Re_h (Figure 1-2 (c) and (d)). Note that to cover the wide range of the Reynolds number in all the studies the abscissa of the plots are plotted on a log scale. Looking at Figure 1-2 (a) and Figure 1-2 (b) we can see that the upstream separation length seems to be independent of the step height and the Reynolds number. On the other hand, the reattachment lengths vary throughout the spectrum of Reynolds number and step height to boundary layer thickness ratio.

Table 1-1 Separation and reattachment lengths for forward step as determined by past studies

Author(s)	Re_h	h/δ	x_s	x_r
Castro and Dianat (1983)	50000	0.192		1.4
Farabee and Casarella (1986)	21000	0.417		3
Shah and Tachie (2007)	1874	0.13		1.67
	3150	0.22		1.5
Fiorentini et al (2007)	4400		-1	2
	26300		-1	3
Camussi et al (2008)	8800	0.2	-1	1.5
	26300	0.2	-1	2.1
Aglenichaab and Tachie	1920	0.107		4.1
Sherry et al (2009)	2000	0.25		1.1
	20000	1.11		4
Ji and Wang (2010)	1620	0.033		0
	6480	0.13		2.16
	25920	0.53		2.93
Hattori et al (2010)	900	0.333	-1.91	2.04
	1800		-1.81	1.82
	3000	0.666	-1.73	2.01
Pearson et al (2011)	20500	0.613	-1	2.5
Wu and Ren (2011, 2013)	3450	0.125		1.5
Awasthi <i>et al.</i> (2014)	6640	0.038	-1.9	2.3
	26600	0.151	-1.9	3.6
	53200	0.154	-1.7	3.7
	213000	0.615	-1.5	4.2
Shao	1600	1.132		1.67
	3200	0.526		1.83
	4800	0.2135		2.25

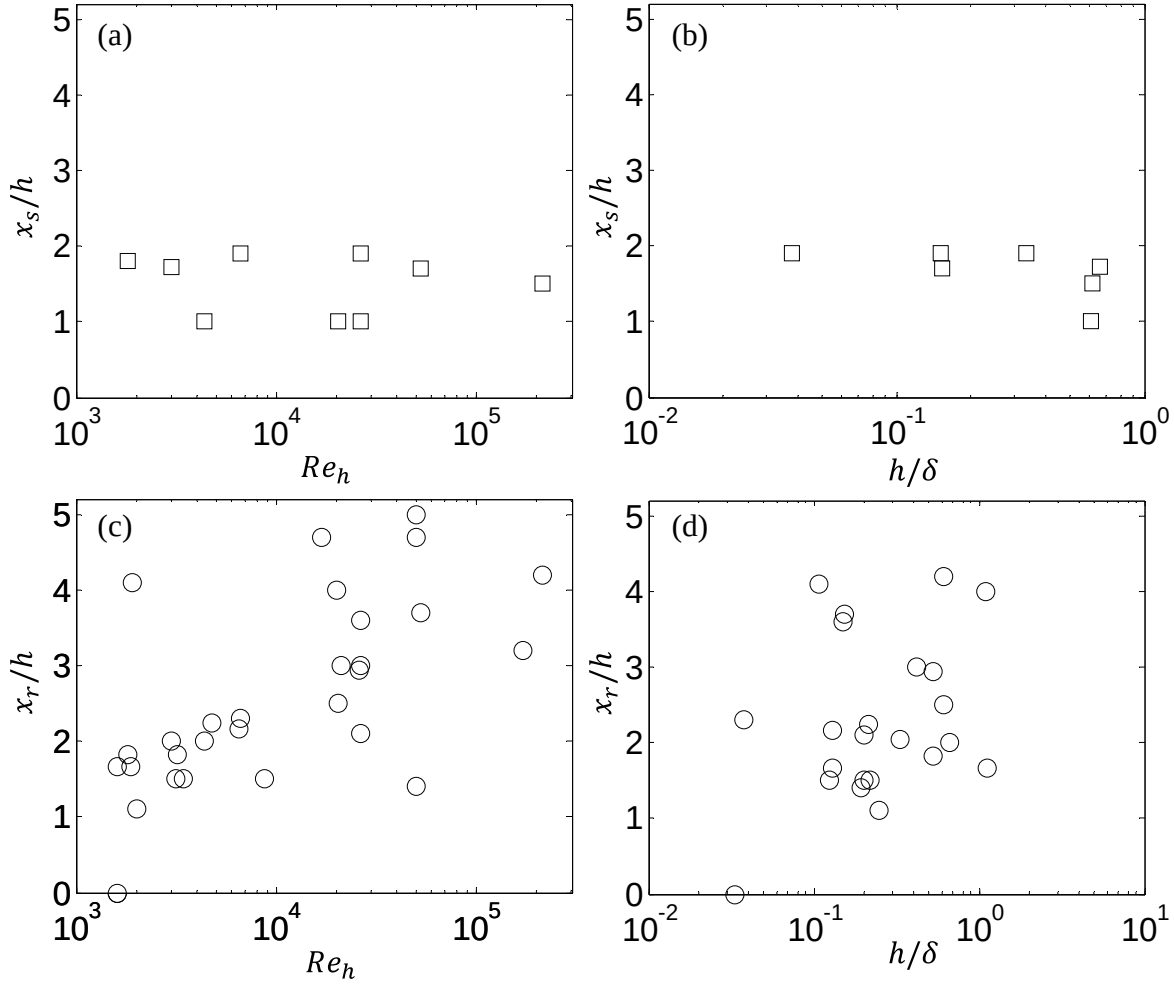


Figure 1-2. Separation and reattachment length for flow over forward steps from studies listed in table 1. (a) Upstream separation length as a function of Re_h , (b) Upstream separation length as a function of h/δ , (c) Downstream reattachment length as a function of Re_h and, (d) Downstream reattachment length as a function of h/δ . Note the log scale on abscissa.

The behavior of pressure fluctuations in the vicinity of forward steps have been studied by several authors in the past. These studies have shown that the pressure fluctuations immediately downstream of the step tend to be larger than those immediately upstream. Farabee and Casarella (1984) was one of the earlier studies to demonstrate the effect of forward facing step on pressure fluctuation field. They measured surface pressure fluctuations upstream and downstream of the step face and presented them in terms of root mean square (r.m.s) values. Their measurements were made for two different step heights of $h/\delta = 41.6\%$ and 62.5% for $Re_h = 21,000$ and $34,000$ respectively. They found that upstream of the step the fluctuating pressure begins to rise above the flat plate at about 3 step heights and is approximately twice that of the flat plate at the base of the step. Downstream of the step, they found the r.m.s values of pressure more than an order of magnitude larger than the flat plate values with the maximum occurring just upstream of the downstream reattachment location. Farabee and Casarella (1986) extended this work to show spectral characteristics of surface pressure fluctuations induced by flow over a forward step with $h/\delta = 41.6\%$ and $Re_h = 21,000$. Their measurements of pressure spectra upstream of the forward step shows low-frequency spectral levels rising above flat plate values beginning at about 6 step heights from the step. These levels continue to rise as one moves closer to the step while the higher frequencies are suppressed compared to flat plate case. This is because flow separation upstream of the step tends to lift the smaller turbulence

structures away from the wall and the near surface region is occupied by large scale recirculating motions. Their measurements of spectra within the separation bubble downstream of the step shows spectral levels enhanced throughout the frequency range and by as much as 25-30 dB compared to the flat plate for lower frequencies. The coherence between the pressure fluctuations downstream of the step suggests a convection velocity between 30-50% of the free stream velocity for lower frequencies while a velocity close to 60% for higher frequencies. This suggests that the higher energy low frequency pressure content is being generated by motions which are much slower than the conventional flat plate convection velocities (generally thought to be about 60% of the free stream velocity).

Camussi *et al.* (2008) measured surface pressure fluctuations in the region upstream and downstream of a forward facing steps with $h/\delta = 20\%$ and for $8800 \leq Re_h \leq 26,300$. They found that the maximum r.m.s pressure occurs about 2 step heights downstream of the step, which is very close to the mean reattachment positions they measured using PIV. Fiorentini *et al.* (2007) measured surface pressure fluctuations upstream and downstream of the face of a forward facing step for $4400 \leq Re_h \leq 26,300$. Their step height was twice the displacement thickness of the undisturbed boundary layer (they do not report actual value of δ but based on δ^* the step would have been immersed in the boundary layer). They found a plateau region in their r.m.s of pressure fluctuations within the downstream separation bubble which continued to the reattachment point and then decayed slowly after that. Their measurements also show that the maxima of pressure fluctuations occurs within the downstream separation bubble and it is higher than the upstream value by about 6-7 dB for a given Reynolds number.

Awasthi *et al.* (2014) studied surface pressure fluctuations upstream and downstream of forward steps with step height to undisturbed boundary layer ratios between $3.8\% \leq h/\delta \leq 61.5\%$ corresponding to Reynolds number between $6640 \leq Re_h \leq 213,000$. Their measurements were resolved up to very high frequencies (about 25 kHz) and the measurements were made at locations which ranged from 9 step heights upstream to over 150 step heights downstream of the step. They also show r.m.s values of pressure whose behavior is consistent with the studies discussed above. Furthermore they showed that the excess r.m.s pressure fluctuations produced by the steps, compared to undisturbed boundary layer, decay well downstream of reattachment as the $-7/6$ power of distance. The shape of their spectra upstream of the step shows enhancement of pressure fluctuations at lower frequencies and suppression at higher frequencies compared to undisturbed boundary layer, which is in agreement with results of Farabee and Casarella (1986). They also found that the downstream spectra are elevated throughout the frequency range and have a power law decay constant of $-7/3$ which they associate with the downstream region behaving more like wake than a boundary layer. They show that the pressure fluctuations downstream of the step is behave independently of the incoming boundary layer by subtracting the undisturbed spectrum from those due to the step flow and collapsing them to the same curve with the same $-7/3$ slope. This collapse however is only found for spectra measured further than 8 step heights downstream of the step, which suggests that the behavior upstream of that is possibly complicated by the unsteadiness of the separation-reattachment process. The pressure spectra measured by Awasthi *et al.* (2014) compare well with those calculated by Ji and Wang (2010, 2012) through Large Eddy simulations (LES) when accounting for the difference between the Reynolds numbers based on step height.

Based on the review of the studies discussed above a few general interpretations regarding the unsteady surface pressure field in flow over forward facing steps can be made. It is clear from most computations and experiments that the region within the downstream separation bubble is more efficient in generating surface pressure fluctuations than the region within the upstream separation bubble. The pressure fluctuations between step corner and downstream reattachment point show similar behavior with little or no decay in their energy. After reattachment there is a slow decay that occurs more-or-less independently of the surrounding boundary layer. The unsteady and complex behavior of the downstream separation bubble can be linked to the so called ‘flapping’ motion of the shear layer which is associated with flows that separate from a sharp corner. These flows include flow over a backward facing step and flow separated from leading edge of a blunt flat plate both of which have been studied extensively in the past. So, to better

understand the complex character of the downstream separation bubble which can be potentially the key to understanding the radiated far field sound a discussion on this ‘flapping’ mechanism is warranted.

Flapping of the shear layer refer to low frequency oscillation of the shear layer that leads to the growth and shrinking of a separation bubble which can lead to significant enhancement of turbulent energy in the flow and cause the reattachment point to fluctuate. Kiya and Sasaki (1983) studied the separation bubble downstream of a blunt flat plate in a flow with Reynolds number (based on free stream velocity and thickness of the plate) of approximately 26,000. Through the cross correlation of surface pressure and velocity fluctuations, they found evidence of two distinct types of vortex shedding from the separation bubble. The first was a regular shedding with a Strouhal number of 0.6 (based on free stream velocity and reattachment length) and the second was of a much larger scale with a Strouhal number of less than 0.2. They suggested that this large scale shedding was accompanied by the flapping of shear layer near separation and was responsible for the growth and shrinking of the separation bubble. They found that pressure fluctuations within the separation bubble were quite large and attributed them to the presence of large scale vortices within the bubble. Furthermore they also calculated approximate location of these large scale vortices and suggested that they are centered fairly close to the wall (20% of reattachment length) in the reattachment zone. The presence of such structures close to the wall within the separation bubble would explain the slow to lack of decay of surface pressure fluctuations within the separation bubble seen by studies such as Farabee and Casarella (1986) among many others.

Cherry *et al.* (1984) studied the separation bubble which forms downstream of a blunt flat plate in a free stream flow. Their Reynolds number based on the plate thickness was $Re_h = 32,000$. They found evidence of periodic shedding of large scale vortices from the separation bubble in their velocity correlation measurements. They suggest that the first 60% of the bubble length the shear layer characteristics are similar to those of a plain mixing layer and the effect of the surface start to appear past that. The implication here is that the shed vorticity from the flapping shear layer is dominating the behavior of pressure fluctuation downstream of the separation bubble while within it the mixing layer-recirculating flow and mixing layer-surface interactions might be a dominant phenomenon. Simpson (1985) also suggest the presence of large scale vortex shedding associated with the flapping shear layer with a streamwise separation of 60-80% of the reattachment length which is similar to the observation of Cherry *et al.* (1984). They have also linked this large structure shedding to the growth and shrinking of the separation bubble. Recently Hudy *et al.* (2003) studied the separated flow behind a splitter-plate fence geometry with $Re_h = 7900$ through measurements of surface pressure fluctuations. They found that the region immediately downstream of separation is dominated by structures with large time scales which are being convected upstream due to recirculation. On the other hand in the separated flow region beyond that ($x > 0.25x_r$) the pressure fluctuations are dominated by structures with smaller time scales convecting downstream. They found the transition between the two time scales to occur between 25-50% of the reattachment length. This results is consistent with Cherry *et al.*’s (1984) conclusion that the first half of the shear layer grows in isolation from the surface. If this is true then the pressure fluctuations in the region immediately downstream of the separation would be dominated by slower moving recirculation structures. On the other hand, the downstream region where the shear layer starts to curve down its interaction with the separation bubble would start to become significant resulting in pressure fluctuation which will be dominated by faster moving structures.

Though the flow over forward step has been studied extensively in the past, the studies of sound radiation from this flow are rare. One of the first studies to consider this problem from a theoretical standpoint was by Howe (1989) who tried to relate the problem of sound from smooth to rough-wall transition to that from a forward facing step. Howe (1989) derived an acoustically compact, or low frequency, Green’s function with a streamwise dipole directivity. Ji and Wang (2010) recently used Howe’s (1989) Green’s function and combined it with their LES simulations of near field behavior to calculate far field sound from forward and backward facing step geometries. They considered step heights ranging from 0.83% to 53% of the undisturbed boundary layer thickness which corresponded to Reynolds numbers (based

on step height) between 328 and 21,000 respectively. They found that for their largest step height the sound levels from the forward step exceeded those from a backward step by 19 dB. This difference in forward and backward facing step noise decreased as the step heights were lowered and the difference between the noise from the two steps for their smallest step height ($h/\delta = 0.83\%$) is only about 1.5 dB. Their results seem consistent with measurements of Farabee and Zoccolla (1998) who measured sound from a backward and forward facing steps that were approximately half of the boundary layer thickness and found that they could not distinguish the background sound of the facility from that of backward facing step. On the other hand the sound from a forward step was easily discernible and was above the facility sound between 10-15 dB throughout the frequency range. Catlett *et al.* (2014) who recently measured sound from backward and forward facing step in a wall-jet boundary layer have also found the forward step to be significantly louder (~10 dB) than a backward step. Ji and Wang (2010) performed analysis of the source field (governed by the acoustic sources in the flow) and the effects of diffraction (governed by Green's function) and attributed the loudness of the forward step (compared to backward step) to both the Green's function effect and flow modification by the geometry. On the other hand, they suggest that the backward step produces sound primarily by diffraction of the sources in the flow. Becker *et al.* (2005) performed both computational and experimental study on noise from forward step. Their measurements of far field sound from forward step showed discernible broadband spectrum which was up to 5 dB higher than undisturbed flow between 1 kHz and 10 kHz. Their computation results for the acoustic pressure distribution shows that while the sound field being generated has a dipole like behavior its directivity is determined by the sharp corner of the step.

Catlett *et al.* (2014) performed measurements of far field sound from forward steps whose height ranged from approximately 8.5%-100% of the boundary layer thickness which corresponded to Reynolds numbers (based on step height) in the range of 1870 to 22,430. Their results show that the sound spectra measured from above the step exhibit a dip at higher frequencies due to destructive interference caused by the acoustic reflection from the surface upstream of the step. Their results do not clearly show a streamwise dipole directivity and suggest that even the steps which are smaller than the boundary layer thickness in a low Mach number flow ($M \sim 0.06$ in their measurements) cannot be treated as acoustically compact sources. They also show that the spectral levels of sound from forward steps scale on the 5th power of undisturbed edge velocity and step height, while the frequency scales on the edge velocity and the undisturbed boundary layer thickness. However, in the region where the step non-compactness effects are observed this scaling does not collapse their spectra.

The implication of assuming the forward facing step to be a compact acoustic source was further explored by Hao *et al.* (2012) who performed LES over small forward facing steps and solved the acoustic analogy using both Howe's (1989) compact Green's function and a boundary element method. They examined three different Mach numbers of 0.01, 0.1 and 0.2 with two different step height to boundary layer ratio of 13% and 53%. They found that for their lowest Mach number the two solutions agreed suggesting a compact acoustic source. However, as the Mach number was increased the two solutions began to diverge starting at higher frequencies displaying non-compact behavior even for a Mach number as low as 0.1. The results of both Hao *et al.* (2011) and Catlett *et al.* (2014) suggest that one has to be cautious in assuming compactness of step noise source even in low Mach number air flow. These results also bring up an important point regarding extrapolation of wind tunnel measurements on forward step type geometries to hydrodynamic situations. In hydrodynamic flows the acoustic wavelength is more than four times the wavelength in air. So, a geometry which can be considered acoustically compact in hydrodynamic flows may not be so in aerodynamic flow.

At the present stage, the exact nature of the sound source in the flow over forward facing steps remains an unknown. Judging by the past studies done on pressure fluctuations in the vicinity of the step (discussed above) one can identify three regions of enhanced pressure fluctuations: the separated flow region upstream of the step, the vertical face of the step and the separated flow region immediately downstream of the step. Since the radiated sound is directly related to the pressure fluctuations, one or more of these three regions represent possible source regions. The surface immediately upstream of the step has

been shown to be less efficient in terms of generating intense pressure fluctuations than the downstream surface. For example, Farabee and Casarella (1986) show pressure spectra immediately downstream of the step to be approximately 10 dB higher than those upstream of the step. The surface immediately upstream of the step might therefore be assumed to have less or no contribution to the far field sound. Slomski (2011) used compressible LES to simulate flow over forward steps with $h/\delta = 11\%-50\%$ and $Re_h = 2152-33,332$ and then used the Ffowcs Williams-Hawkings equation to calculate the far field sound. He showed that the surface upstream of the step does not contribute significantly to the overall sound radiation. Slomski (2011) further showed, by isolating the downstream surface of the forward step, that most of the sound was being generated by the first 5 step heights of this surface. This result to some extent is supported by studies such as Farabee and Casarella (1984, 1986) and Awasthi *et al.* (2014) which show intense pressure fluctuations immediately downstream of the step. Glegg *et al.* (2012) used the surface pressure fluctuation measurements downstream of a forward facing step made by Awasthi *et al.* (2014) and made far field predictions. They compared these predictions to the measurement of far field sound made by Catlett *et al.* (2014) and found good agreement suggesting that the sound source might be behaving like a lift dipole. Slomski's (2011) computations, Catlett *et al.*'s (2014) measurements and Glegg *et al.*'s (2012) predictions combined together then support the hypothesis that the downstream surface plays an important role in sound generation.

Howe's (1989) Green's function approach models the step noise as a streamwise aligned dipole source which implicitly means that the sound is being governed by the force on the vertical face of the step. This type of directivity suggests that there must be a null directly above the step. However, as shown by measurements of Catlett *et al.* (2014) this is not the case. Furthermore, Slomski (2011) isolated the step face to calculate the sound assuming the source was a streamwise dipole and found that it was only a fraction of the overall sound being produced by the step. Becker *et al.* (2005) studied the flow over a forward step with $Re_h = 16,000-32,000$ through LES and then used finite element model to solve the acoustic analogy. Their results show that the forward step noise does have a dipole like behavior but instead of a streamwise aligned dipole behavior its directivity is determined by the top corner of the step. Unfortunately, to the author's knowledge, no measurements of pressure fluctuations on the face of the step (which would be responsible for a sound field with streamwise aligned dipole directivity) have been performed which would support or deny the involvement of this surface in sound generation.

The evidence presented above regarding the previous studies on forward step noise indicates that it is possible that the sound source may be some combination of the unsteady forces on both the vertical face and the downstream surface of the step. This idea had not been explored until recently when Glegg *et al.* (2014) suggested that the unsteady loading on the face of the step and the downstream surface are the same and it is their resultant placed at the exposed corner of the step which determines the far field sound and its directivity. The hypothesis of the directivity being governed by the exposed corner of the step is supported by results of Becker *et al.* (2005). The theory of Glegg *et al.* (2014) will now be described in some detail.

The central idea to the theory of Glegg *et al.* (2014) is that the unsteady force on the vertical face of the step and the downstream surface are the same and it is their resultant placed on the top corner of the step that is responsible for the radiated sound. Figure 1-3 shows a representation of this idea in a two-dimensional schematic. Here unsteady forces on the step face and downstream surface are the same and $F(\mathbf{y}, \omega)$ is their resultant which points in the downstream direction as shown in the figure. Note that in this theory the resultant force vector points in the downstream direction, R is the distance from inside corner of the step to the observer, h is the step height and $\mathbf{x}$ and $\mathbf{y}$ are the near field and far field coordinates as depicted in the figure. They derive the spectral density of the far field sound from the step geometry as

$$\Phi_{pp}(\mathbf{x}, \omega) = \frac{k^2 \phi_{FF}(\omega)}{(2\pi R)^2} \left| \frac{ix_1}{R} \cos\left(\frac{kx_2}{R}\right) + \frac{x_2}{R} \sin\left(\frac{kx_2}{R}\right) \right|^2 \quad (1-3)$$

Where k is the acoustic wavenumber, $\Phi_{pp}(\mathbf{x}, \omega)$ is the spectral density of far field sound and $\phi_{FF}(\omega)$ is the spectrum of the resultant force shown in the figure below. The squared term inside the brackets in equation (1-3) determines the directivity of the sound field and at low frequencies the first term in the bracket will dominate to yield a cosine squared i.e. dipole directivity which is the same as Howe's (1989) Green's function. However, at higher frequencies both the term in the brackets will become important which would result in a more complex directivity that as seen in measurements of Catlett *et al.* (2014) could be the case. Note that in the equation above the only term which is an unknown is the frequency spectrum of the unsteady loading. Thus, Glegg *et al.*'s (2014) theory takes into account the role played by the pressure fluctuations on both the face of the step and the downstream surface. Additionally, it does not assume a streamwise dipole directivity throughout the frequency range which as shown by measurements of Catlett *et al.* (2014) and computations of Hao *et al.* (2012) could be the case due to non-compactness of the step geometry at higher frequencies.

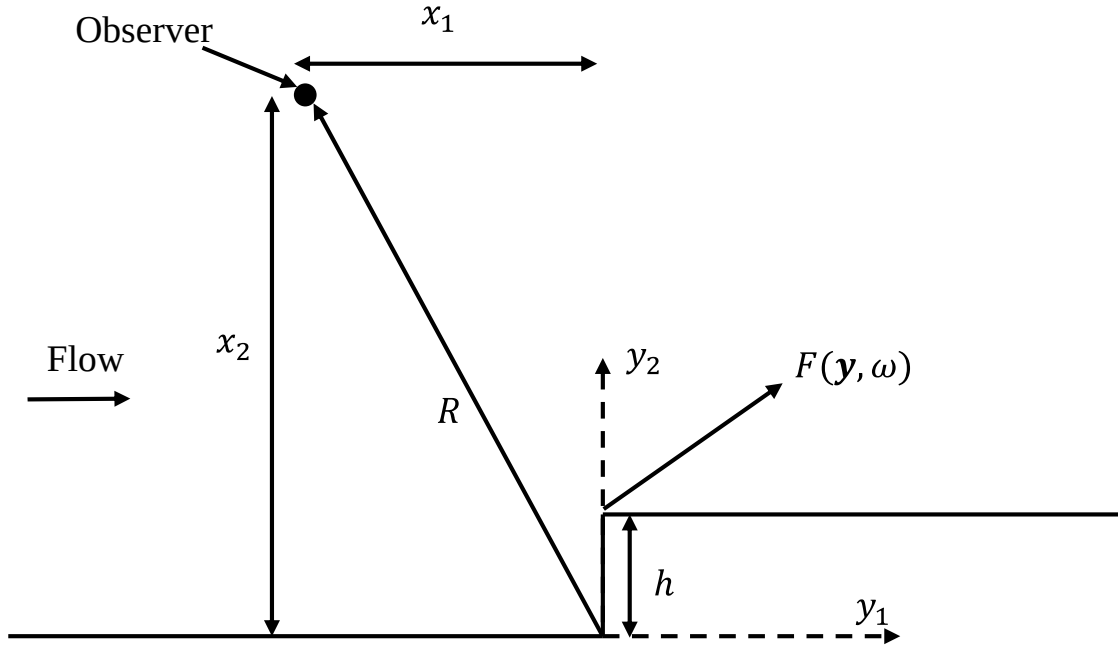


Figure 1-3 Illustration of sound source behavior in theory of Glegg *et al.* (2014).

1.3.1.1 Sound Radiation from Rounded Forward Facing Steps (Hao and Wang (2014))

In practice the idealized geometry of forward facing step with a sharp external corner does not always exist. In most practical geometries of interest, this corner is rounded to some degree to avoid high concentration of stress associated with sharp corners that can result in structural failure. At present, the impact of rounding a step or a gap corner on the fluid dynamics and radiated sound levels is unknown and not much work has been done on this problem.

One recent study which deals with flow over rounded forward steps is by Hao and Wang (2014) who studied the effect of corner rounding on the flow field through LES simulations. Their step height to undisturbed boundary layer ratio was $h/\delta = 26\%$ and the Reynolds number based on momentum thickness was $Re_\theta = 4755$. They considered five different roundings of the step corner with radius to step height ratios of $r/h = 0\%, 12.5\%, 25\%, 50\%$ and 100% . They found that the separation bubble downstream of the step corner was sensitive to corner rounding and reduces in size with increasing corner rounding. For $r/h = 100\%$ (i.e. rounding equal to step height) the downstream separation bubble was barely visible in their simulations. On the upstream side, they found that while rounding the step corner to $r/h = 12.5\%$ led to a slight reduction in the size of separation bubble (x_s) compared to an unrounded step, further rounding of

the corner had negligible effect on x_s . Their results also show that the separation point at the external step corner moves further downstream with increasing rounding leading due to a delayed interaction between the vorticity generated from upstream separation and the downstream shear layer.

Figure 1-4 (a) below shows a schematic of time mean flow structure for an unrounded step and Figure 1-4 (b) shows the possible modification of this flow structure based on interpretation of Hao and Wang's (2014) results. As seen in the figure, as the step corner is rounded the upstream separation bubble length x_s and the vertical reattachment length y_r on the step face remain unchanged. On the other hand, the downstream separation bubble shrinks both in the streamwise and step-normal direction. Also shown in the figure is the length x_{sD} which denotes the distance from the vertical face of the step to the point where the flow separates on the rounded surface. This separation length increases with corner rounding and according to Hao and Wang's (2014) result is equal to $x_{sD}/h = 0, 0.08, 0.19, 0.47$ and 1.03 for $r/h = 0\%, 12.5\%, 25\%, 50\%$ and 100% respectively. For an unrounded step the upstream separation lifts the near wall turbulence structures away from the wall which can then interact with the separated shear layer which forms on the upper surface of the step. If the picture shown in Figure 1-4 (b) is accurate then the delayed separation along with reduced intensity and size of the downstream bubble would lead to this interaction getting weaker as the step corner is rounded. In fact, their correlations of surface pressure fluctuations show that within the downstream separation bubble the correlation length decreases with increasing rounding. Far downstream (20 step heights) they show that while for an unrounded forward step correlations continue to grow the correlation for rounded steps return approximately to the undisturbed boundary layer form. Another interesting result from their study is that they found the peak r.m.s pressure on the downstream surface of the step actually increases with increasing rounding and its location moves upstream. The reason for this higher peak and upstream shift is evident in their turbulent kinetic energy results which show that the downstream mixing layer although lower in intensity and size is pulled closer to the wall as the rounding of the corner increases. This causes intense pressure fluctuations but their effective area is reduced and it leads to quicker recovery of pressure fluctuations to undisturbed boundary layer levels with increasing rounding.

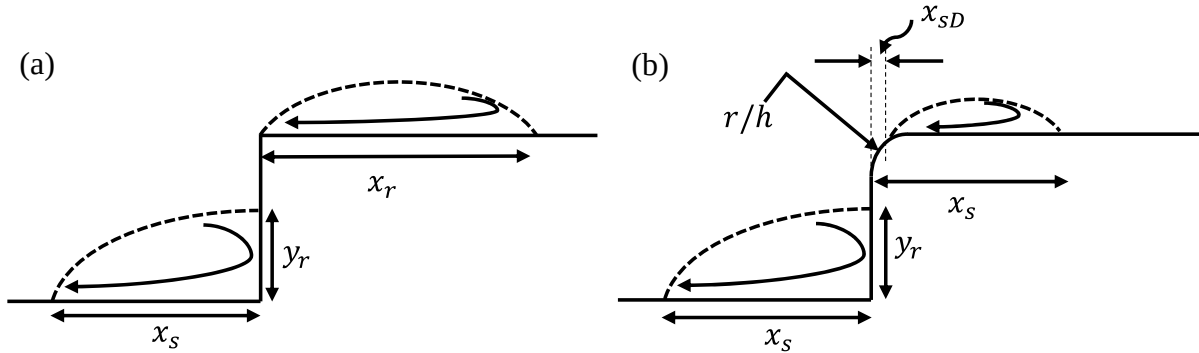


Figure 1-4 Flow structure in the vicinity of (a) Forward facing step and (b) Forward facing step with a rounded corner (based on interpretation of Hao and Wang's (2014) LES results).

Hao and Wang (2014) also calculated far field sound from rounded forward steps using their LES results as input to the acoustic analogy. They used the acoustically compact Green's function given by Howe (1989) to solve Lighthill's acoustic analogy and yield far field sound. Note that they use a hard-wall Green's function such that the term $\partial G / \partial n$ in equation (1-1) is zero and so they use the volume integral with knowledge of the Lighthill stress tensor from their LES simulations and the Green's function to solve for far field pressure. Their results show that as the step corner is rounded the shape of sound spectra remains similar but a drop in spectral levels across the frequency spectrum is seen. The attenuation of sound with rounding is more at higher frequencies than at lower frequencies. For their lowest rounding of $r/h = 12.5\%$

they see a peak level drop of 2.4 dB and about 5.5 dB at low and high frequencies respectively. They also found that despite of the spectral attenuation, the directivity of the radiated sound field remains nearly independent of the corner rounding. Furthermore, they used a BEM method to solve the acoustic analogy and found that at lower frequencies the solution is the same as that found with Howe's (1989) acoustically compact Green's function and the sound field displays a streamwise dipole directivity. However, as the frequency increases and the ratio of acoustic wavelength to step height (λ/h) drops the BEM solution does not show dipole like behavior indicating the non-compactness of the step geometry. This non-compact behavior at higher frequencies is seen for every rounding considered in their study. Hao and Wang (2014) also analyzed the behavior of the Lighthill stress field and the Green's function in the vicinity of the steps to deduce the reason for the reduction in sound field. They suggest that the reason for the sound reduction with increasing rounding of the corner is the reduction in strength and de-localization of the Green's function. For an unrounded step their Green's function is focused around the top corner but as the step is rounded it spreads spatially and its maximum value is reduced by two decades. They found this reduction in Green's function magnitude to be much larger than the reduction in maximum value of the Lighthill stress field and hence attributed the sound reduction to Green's function effect.

1.3.2 Sound Radiation from Two-Dimensional Gaps

Another commonly found discontinuity on aero/hydrodynamics surfaces are gaps which form due to presence of a forward facing step downstream of a backward facing step. A similar geometry to gap is the flow over cavity which has been studied widely. However, the cavity is different from a gap in that it has a small spanwise length to height ratio and small streamwise separation to height ratio compared to a gap. These small aspect ratios make the problem of cavities three-dimensional and the radiated sound is characterized by resonance and the reflections of wake from the forward facing step geometry (see Grace (2001)). Another critical difference between a cavity and gap is their height relative to the incoming boundary layer thickness. The cavities which have been studied in the past tend to be higher than the thickness of the incoming boundary layer. On the other hand, a gap configuration would generally be smaller than the boundary layer thickness. This smaller height could also lead to the gap sound being dominated by the fluid dynamic sources rather than the acoustic effects.

While there have been many studies on the flow over a cavity the problem of gaps with large spanwise (span to gap height ratio) and streamwise (gap width to height ratio) aspect ratios has received less attention. Gaps with large spanwise aspect ratios can be treated as two-dimensional combination of forward and backward facing steps. The characteristic length scale in this case becomes the streamwise aspect ratio or the separation between the two steps to step height ratio. If the gap width is large enough where the separated flow from backward facing step is allowed to recover to its equilibrium state before reaching the forward facing step then the two steps can be isolated. However, as shown by Farabee and Casarella (1984, 1986) the backward facing step can disturb the flow far downstream of it. Their pressure fluctuations results show disturbances above undisturbed boundary layer levels as far as 72 step heights downstream of the backward step. This means that the gap width has to be quite large for the problem to be considered truly isolated which might not be the case in most practical geometries. Instead, the forward facing step geometry would receive a non-equilibrium flow due to the separated shear layer from the backward step. The interaction of the forward step with the separated shear layer from backward step would then depend strongly on the separation between the two steps. If the gap width is small enough so that the separated flow from backward facing step is not allowed to reattach before it reaches forward step some of the flow characteristics described in Figure 1-1 for forward steps may not even exist. The reattachment length downstream of backward facing step has been studied by many authors in the past and most have reported a length between 6-8 step heights (Devenport (1989)). The separated shear layer has been found to be unsteady by many studies in the past (e.g. Eaton and Johnston (1981), Driver *et al.* (1987), Lee and Sung (2001) etc.) and exhibits flapping motion which causes the instantaneous reattachment length to fluctuate in the streamwise direction. The region near the reattachment point has been found to be the most energetic in terms of surface pressure fluctuations (Lee and Sung, 2001) and as evident by Farabee and

Casarella's (1984, 1986) results the boundary layer has to travel a large distance before recovery. Thus for the case where the forward facing step is within 6-8 step heights downstream of the backward step the separated shear layer will interact directly with it. Eaton and Johnston (1981) found through their time mean streamline results that the dividing streamline between recirculating flow and the shear layer for backward steps is approximately straight for about half the distance of the separation bubble and curves sharply after that. This suggests that if the forward facing step is too close to the backward step ($< 3-4$ step heights) then the dividing streamline might flow over the entire gap geometry. On the other hand if the forward step is in the latter half of the separation bubble where the shear layer curves down it will interact with the forward step and can cause complex flow structures to develop.

The far field sound from two-dimensional gaps has been studied widely in the form of cavity sound. The flow over cavities is characterized as fluid-dynamic, fluid-resonant and fluid-elastic (Grace (2001)). In present work we are interested in flow over gaps whose height is smaller than the boundary layer thickness and the gap width is greater than or of the same order as the gap height. Under these conditions the flow is characterized as fluid-dynamic (Grace (2001)) and the dominant acoustic sources are flow related rather than those related to acoustic feedback or resonance. Therefore, we will restrict our following discussion on far field sound from gaps to review of studies where the fluid dynamic sound source is dominant.

Catlett *et al.* (2014) studied sound radiated from gaps with $h/\delta = 66\%$ and four different gap widths of $w = 0.5h, 1h, 2h$ and $8h$. This gap configuration has been shown in Figure 1-5 (a). They found that for their narrowest gap configurations ($w = 0.5h$) the sound behaves almost like a backward facing step alone of the same height. As the gap width increases to 1 step height they found the sound levels rise, with the lower frequencies approaching the levels of a forward step alone configuration of the same height. For $w = 2h$ the low frequency content of their far field spectrum (up to about 3 kHz) is nearly identical to a forward step spectrum while the higher frequencies are suppressed. Their far field sound results for the widest gap configuration of 8 step heights showed sound spectra which was similar to the forward step spectra throughout the frequency range. These results suggest that, acoustically speaking, the gap behaves like a forward step as long as the forward step portion of the gap is outside the wake of the backward step. This suggests that for narrow gaps the flow inside the gap does not full develop and so the forward step plays little role in sound generation. In fact, Hao *et al.* (2011) who performed LES simulations over small gaps with similar gap width ($h/\delta = 13.3\%$, $w = 1h, 2h, 4h$ and $8h$) found that for $w \leq 2h$ the separated shear layer from backward step just flows over the forward step and there is a single recirculation bubble inside the gap. As their gap width increased to $4h$, the flow within the gap started to develop more with the separation bubble within the gap resembling that found in backward step flows. For their widest gap configuration ($w = 8h$) which is the same as Catlett *et al.* (2014) they found that the two steps are decoupled fluid i.e. the features in isolated backward and forward step flows are both observed in this configuration. Since the forward step is known to be much louder than backward step, a complete development of flow around it in wider gap configurations leads to the sound resembling a forward step noise as seen in measurements of Catlett *et al.* (2014). Hao *et al.*'s (2011) far field sound computations done using acoustically compact Green's function are consistent with Catlett *et al.*'s (2014) measurements.

Catlett *et al.* (2014) also measured sound from asymmetric gaps (different heights of forward and backward step portions) with two different gap widths of $w = 1h$ and $8h$. For both of these gap widths their backward step height was fixed at $h_1/\delta = 66\%$ and the forward step height was varied from $h_2/\delta = 8.5\%$ to 100% . This configuration is shown in Figure 1-5 (b) and (c) below. They found that for the narrow asymmetric gaps ($w = 1h$) the sound from $h_2 < h_1$ configuration i.e. forward step portion of less height than the backward step portion mimics that from a backward step alone configuration. As the forward step rises to the level of backward step (Figure 1-5 (c)) the sound rises rapidly and for $h_2 > h_1$ it closely resembles forward steps sound except some scalloping at highest frequencies due to acoustic reflections from the gap. For their wide asymmetric gap ($w = 8h$) the sound from each configuration resembles the sound from a forward step of the same height regardless of how small the forward step height is. Their results from these asymmetric gaps suggest that for narrow gaps the sound from the forward step component

becomes dominant and resembles forward step noise only if it is not shielded by the backward step. On the other hand, for the wide asymmetric gaps the sound from even the smallest forward step dominates. The reason for this is that for $w = 8h$ the separated flow from backward step is allowed to reattach within the cavity before it reaches the forward step portion and hence the sound is dominated by the forward step portion. Hao *et al.*'s (2011) LES simulations over similar gap configurations show that for narrow gaps with $h_2 < h_1$ (Figure 1-5 (b)) the separated shear layer from the backward step impinges on the downstream surface of the step and the forward step is completely immersed within this separated layer. On the other hand for $h_2 > h_1$ (Figure 1-5 (c)) for the same gap width the separated shear layer impinges on the forward step face and a separation bubble is also seen to form downstream of the forward step.

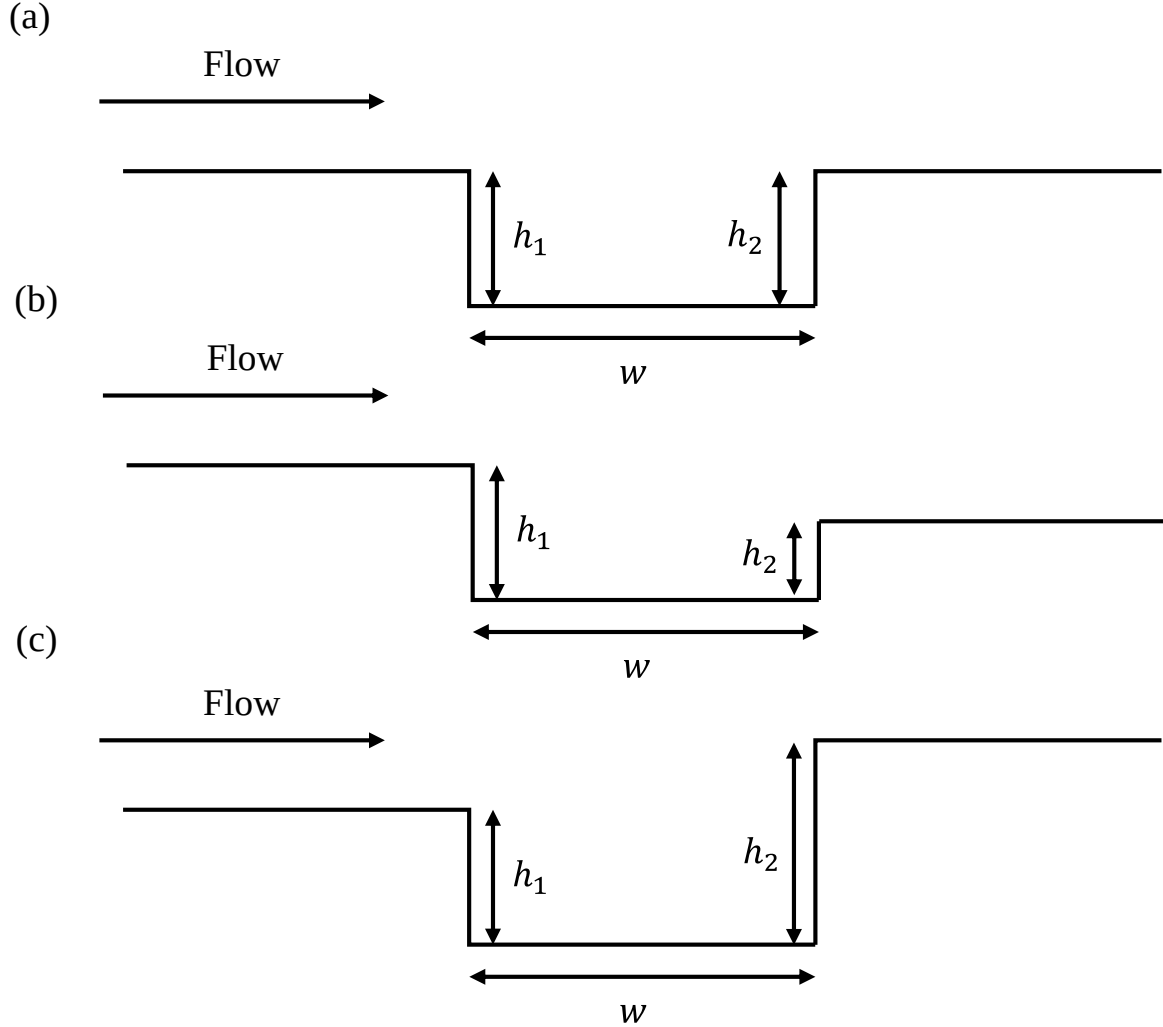


Figure 1-5 Illustration of gap configurations studied by Catlett *et al.* (2014). (a) Symmetric gaps with $w = 0.5h, 1h, 2h$ and $8h$, (b) Asymmetric gap with $h_1 > h_2$, $w = 1h$ and $8h$ and (c) Asymmetric gap with $h_2 < h_1$, $w = 1h$ and $8h$.

Besides providing some insight into gap noise the studies done by Catlett *et al.* (2014) and Hao *et al.* (2011) also provide us with some clues (and potential issues) regarding the sound source mechanism of the forward step itself. For example, if we consider narrow gaps where the separated flow from the backward step does not reattach before it reaches the forward step the sound in Catlett *et al.*'s (2014) measurements does not rise up to levels of forward step until $h_2 > h_1$ which is also the case where a separation bubble downstream of forward step appears in Hao *et al.*'s (2011) simulations. Furthermore, the streamwise length of their separation bubble is about 2.3 step heights which is close to the length usually

reported in literature for a forward step alone configuration. This suggests that the downstream surface is contributing to the far field sound. However, it must also be remembered that in this configuration the face of the step is getting more exposure to the separated shear layer so its contribution cannot be ignored. The problem with disregarding the step face as potential noise source becomes even clearer if we consider the results for Hao *et al.* (2011) for the wide asymmetric gaps. We know from Catlett *et al.*'s (2014) measurements that for this configuration, regardless of the forward step height, the sound resembles a forward step alone configuration of the same height. However, Hao *et al.*'s (2011) simulations show that for this configuration the length of the separation bubble downstream of forward step is much smaller than a forward step alone configuration. This suggests that the equivalency of the wide gap and forward step alone configuration sound does not correlate with the behavior of the separation bubble downstream of the forward step. Hence, the face of the step cannot be disregarded as a potential sound source.

1.4 Recent Studies on Sound from Three-dimensional Surface Discontinuities

The surface discontinuities in practical applications may not always be two-dimensional. Three-dimensionality in an already complex separated flow field can further complicate the understanding of sound source mechanism. However, in some instances the behavior of a surface discontinuity in a two-dimensional flow can be extended to three-dimensional cases. One such example is the case of a swept forward facing step. Early studies by several authors (for e.g. Prandtl (1946) and Jones (1947)) demonstrated that in a laminar flow over a yawed flat plate the chordwise flow is decoupled from the spanwise flow and this is termed as the *sweep independence principle*. However, it has been shown by some studies (see for e.g. Selby (1983), Barkley Wolf (1987)) that the applicability of this principle to turbulent boundary layer flows is limited in terms of the yaw angle. This limited application of the independence principle to turbulent boundary layer flows has been attributed to the coupling of the Reynolds stress tensor in the chordwise and spanwise equations (Selby (1983)). Nonetheless, it has been found that at low sweep angles some parameters associated with flow separation are sweep independent. For example, Barkley Wolf (1987) studied a swept blunt flat plate and found the mean reattachment length behind the leading edge of the plate and the velocity profiles within the separation bubble to be sweep independent up to sweep angle α of 45° . Selby (1983) who measured the reattachment length behind a swept backward step also found this length to be sweep independent up to about $\alpha = 38^\circ$.

Though the case of a swept step has been studied aerodynamically for backward steps and a yawed plate there had not been an acoustic study of a swept forward step configuration until recently when Hao and Wang (2013) used LES simulations combined with a Green's function solution to Lighthill's acoustic analogy to demonstrate what they termed as the *acoustic sweep independence principle*. They studied the acoustics of a swept forward facing step for sweep angles of $\alpha = 0^\circ, 15^\circ, 30^\circ$ and 45° . Their step height to undisturbed boundary thickness ratio was $h/\delta = 13\%$ and the Reynolds number based on momentum thickness was 4755. They found in their LES simulations that increasing the sweep angle led to weaker pressure fluctuations and a faster flow recovery downstream of the step. They were able to demonstrate an approximate sweep independence of the reattachment lengths, mean pressure and skin friction coefficients using normalization on the step-normal component of the velocity up to a sweep angle of $\alpha = 45^\circ$. Their pressure fluctuation calculations downstream of the step also show sweep independence but it is restricted to sweep angles up to $\alpha = 30^\circ$ and at low frequencies. Their calculation of far field sound also showed an approximate sweep independence up to 30° at low frequencies. In their calculations they assumed a spanwise homogenous flow on the swept step. In practice this might not be the case since a streamwise velocity gradient can exist in many flows of interest. Therefore, their hypothesis of sweep independence in far field acoustics remains to be tested in a spanwise inhomogeneous flows.

The potential existence of a sweep independence principle in far field sound suggests a possibility that this principle could be applied to some other three-dimensional geometries as well. One such potential application of the sweep independence principle is to a low aspect ratio wall mounted circular cylinder (effectively a circular-planform step). The aeroacoustics of a wall mounted circular cylinder with large

aspect ratios (AR) $h_D/r_D > 1$ (r_D being the diameter and h_D being the height of the cylinder) has been studied in the past by some authors. It has been shown that in large AR wall mounted cylinder with $h_D > \delta$ Karman vortex shedding is the dominant feature which produces tonal sound (Moreau and Doolan (2013)). On the other hand when the cylinder AR is low the vortex shedding becomes less dominant and the end effects (at cylinder-wall junction) begin to dominate the noise production which leads to the spectra becoming more broadband in nature. It is this latter case of $h_D < \delta$ where the flow separation-reattachment is expected to dominate the noise being produced that the sweep independence principle might find a potential application. Bryan *et al.* (2015) recently studied the flow around a low AR ($h_d/r_d = 0.08$) wall mounted cylinder (referred to by them as a circular embossment) through RANS simulations. Their results show that the flow separation/reattachment region at the leading edge of the embossment resembles the separation/reattachment region found in flow over a forward step except it conforms to the circular geometry of the leading edge up to some angle α . They also found (through zero crossing of wall shear stress) that the reattachment length downstream of the leading edge of the cylinder remains approximately the same up to an angle of $\alpha = 50^\circ$ beyond which it increases. Their results suggest that if the separated flow structure at the leading edge is similar to that of a forward step then the separated flow structure at the trailing edge of the embossment must be similar found in backward step flows. Since forward steps are known to be significantly louder than backward steps this also suggests that in case of low AR wall mounted embossments the leading edge noise may be dominant. Furthermore, the variation of flow properties along the span of the embossment shown in Bryan *et al.*'s (2015) simulations suggests that they might scale with the step-normal component of the incoming velocity vector. If so, the sweep independence principle of Hao and Wang (2013) might be applied to predict the sound from such a geometry using information on behavior of sound generated by flow over two-dimensional forward facing steps.

1.5 Objectives

In light of the existing knowledge base of sound source mechanism in two and three-dimensional surface discontinuities the present work attempts to accomplish the following tasks through series of measurements.

- i. Determine the sound source mechanism in two-dimensional forward facing steps immersed in a turbulent boundary layer
- ii. Explore the effects on the source region and the radiated sound field when the step corner is rounded to different fractions of the step height.
- iii. Investigate the effects of gap configurations with rounded corners on the far field sound.
- iv. Determine the relationship between sound radiation from two-dimensional and three-dimensional surface discontinuities by: a) Employing a swept forward step geometry and, b) Employing a fully three-dimensional wall mounted circular embossment.

1.6 Dissertation Structure

The organization of the dissertation can be broadly broken down into two main sections. The first section of the work details the measurements of far field sound performed in the Virginia Tech anechoic wall-jet facility while the second section deals with both near and far field measurements on similar geometries in much larger Virginia Tech Stability Wind Tunnel. The detailed organization is as follows

- o Chapter 2.1 describes the setup of surface discontinuities in the Virginia Tech anechoic wall-jet facility and related instrumentation used to measure far field sound. The surface discontinuities include rounded forward facing steps, gaps with rounding of backward step corner, gaps with rounding of the forward step corner and swept forward facing steps with and without rounding of the corner.
- o Chapter 2.2 discusses the far field sound behavior in flow over aforementioned discontinuities. The results presented in this section and discussed include far field sound spectra and

- directivities from two-dimensional steps/gaps, sound spectra from swept forward steps with rounded corners and the application of sweep independence principle.
- o Chapter 3.1 describes the setup and related instrumentation used to measure near field and far field quantities of interest in a conventional high Reynolds number boundary layer flow (Virginia Tech Stability Wind Tunnel) over two and three-dimensional surface discontinuities. The two-dimensional surface discontinuities include forward facing steps with four different rounding of the corner which are the same fraction of the step height as those deployed in wall-jet boundary layer. The three-dimensional surface discontinuity is a low aspect ratio wall mounted circular embossment.
 - o Chapter 3.2 discusses results from measurements on flow over surface discontinuities made in the Virginia Tech Stability Wind Tunnel. The relevant measurements which were made and will be discussed are mean pressure, oil flow visualization, single and two point surface pressure fluctuations statistics, unsteady surface forces and far field sound.
 - o Chapter 4 finally concludes this dissertation.

2 Surface Discontinuities in a Wall-Jet Boundary Layer Flow

2.1 Apparatus and Instrumentation

2.1.1 Virginia Tech Anechoic Wall-Jet Facility

The first part of the present work was performed in an anechoic wall-jet facility at Virginia Tech. This facility is well suited to measure boundary layer noise due to low background noise. Figure 2-1 shows a schematic of this facility. The facility is powered by a variable speed centrifugal fan which is connected to an acoustically treated settling chamber. The flow from the settling chamber is exhausted through a 12.7 mm high nozzle onto a 3058 mm x 1600 mm x 9.5 mm aluminum test plate on which the geometry of interest is installed. This test plate is surrounded by a 4060 mm x 1930 mm x 2427 mm chamber which is acoustically treated to minimize acoustic contamination from the laboratory environment. The downstream end of the test plate has a rounded geometry to eliminate trailing edge noise. The far field sound in this facility is measured by installing instrumentation within the surrounding anechoic chamber and in the region outside the flow. The far field sound instrumentation (to be described later in §2.1.6) is shielded by the nozzle exit noise by blocking the line of sight from the nozzle exit through the use of two acoustic baffles. The first baffle is a horizontal shelf treated with acoustic grade foam which spans the entire width of the anechoic chamber and is positioned so that there is a 300 mm gap between it and the nozzle. This gap was created for the present work to enhance the two-dimensional core of the wall-jet boundary layer through entrainment of the still air on top by the boundary layer. This gap thus serves an aerodynamic purpose but acoustically it suffers from a drawback because it also leaves some of the far field sound instrumentation exposed to the nozzle exit noise. Therefore, a second 330 mm wide vertical baffle as shown in Figure 2-1 (a) was installed on top of this horizontal baffle to shield the instrumentation from this noise. Figure 2-1 (b) above shows a close up view of the wall-jet nozzle and test plate configuration along with the location of the horizontal baffle above the flow surface. Seen in the figure are the 12.7 mm opening at the nozzle exit plane where the flow enters the test plate and the horizontal acoustic baffle situated with its leading edge 300 mm downstream of this exit plane. Also shown in this figure is the coordinate system which will shortly be used to describe the boundary layer flow characteristics of this facility. The origin of this coordinate system is located on the surface of the test plate at the nozzle exit plane with x_n and y_n being the streamwise and wall-normal coordinates and z_n (not shown in the figure) is the spanwise coordinate pointing out of the page according to the right hand rule.

The wall-jet facility has been used extensively in the recent past to measure boundary layer noise from various types of geometries. A detailed description of the aerodynamic and acoustic character of the facility itself can be found in Grissom (2007), Smith (2008), Alexander (2011), Devenport *et al.* (2011) and Clark (2014).

2.1.1.1 Undisturbed Boundary Layer Flow Characteristics

The boundary layer flow characteristics of this facility will now be described. It must be noted that the measurements used to obtain these characteristics were part of a different study and were made by Clark (2014) using a flattened pitot tube. The details of the instrumentation used for these measurements can therefore be found there. The boundary layer structure of the wall-jet flow is different than a conventional boundary layer in that it is not bounded by a free stream flow on the top. Instead, in wall-jet flow the velocity rises as one moves away from the wall to a maximum value after which it decreases until the flow merges with the stagnant air on top of the flow region. Therefore, before we proceed to discuss the flow characteristics it is important to describe a few important boundary layer parameters for a wall-jet flow which are different than those for a conventional boundary layer bounded by a free stream flow. Figure 2-2 shows a typical boundary layer velocity profile observed in a wall-jet flow. As seen in this figure the streamwise velocity rises to a maximum value U_m at a distance $y = \delta$ normal to the wall and then begins to decrease. This profile shape essentially divides the wall-jet boundary layer into two regions: an inner region

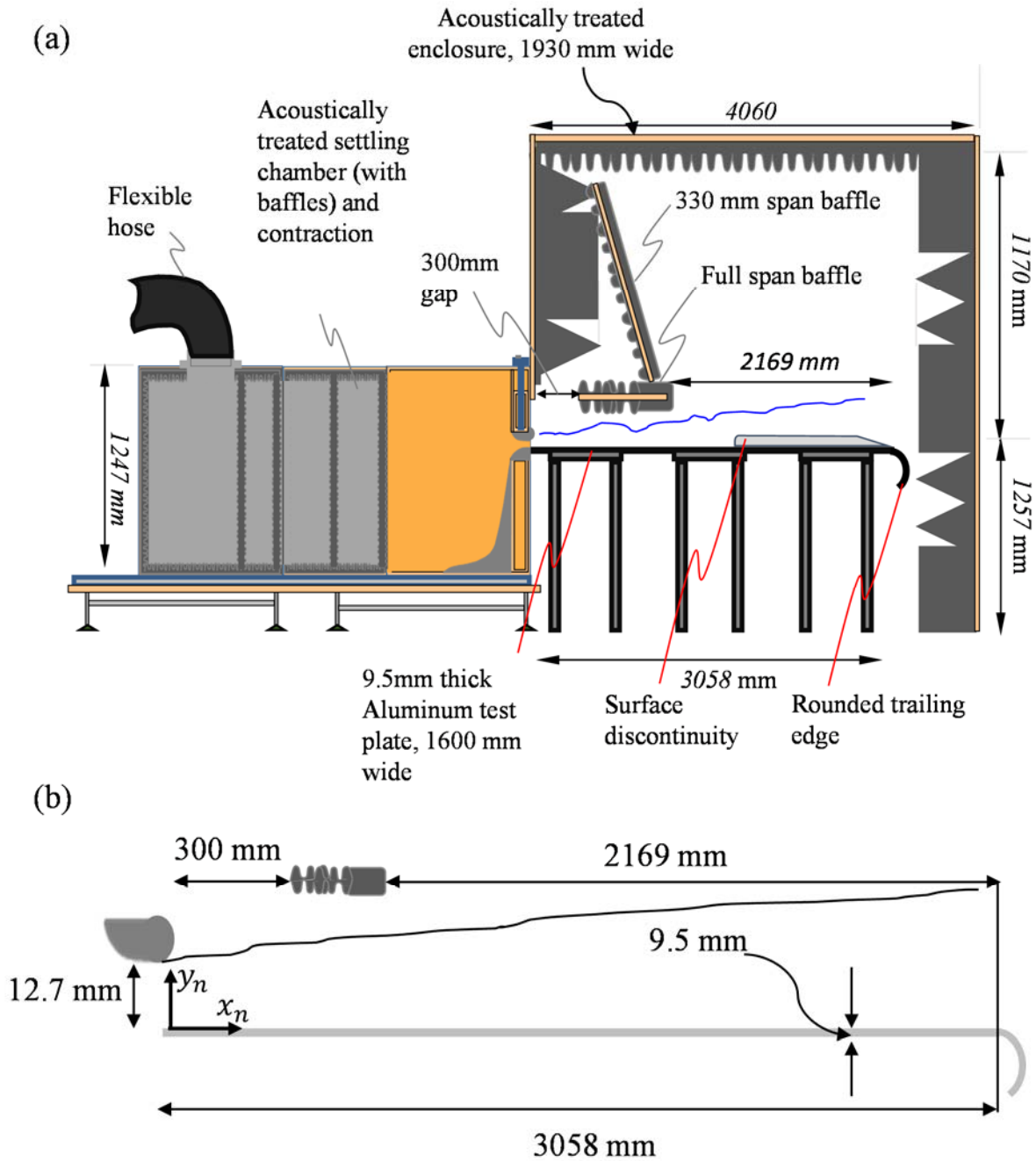


Figure 2-1 Description of Virginia Tech anechoic wall-jet facility: (a) Schematic of the entire facility and (b) Close up schematic showing the nozzle, the horizontal baffle and the coordinate system used to describe the boundary layer characteristics of the facility.

for $y \leq \delta$ (where δ is defined as the boundary layer thickness in wall-jet flow) and an outer region for $y > \delta$. While the inner region behaves much like a conventional boundary layer, the outer region behavior is influenced by the mixing between the flow and the still air on the top. An important length scale for the outer region flow is half-height designated as $y_{1/2}$ which is defined as the distance above the wall at which the maximum velocity decreases to half its value. This half-height definition is shown in Figure 2-2. It has been shown by previous studies (e.g. Grissom (2007)) as well as the measurements made by Clark (2014)

with the facility in its present state that when the boundary layer profiles at a particular streamwise station in wall-jet flow are normalized on $y_{1/2}$ and U_m values at that station, the velocity profiles are self-similar and collapse to a single curve.

The velocities at which measurements in present work were performed were specified by the flow velocities at the nozzle exit plane (U_j) which then determines the undisturbed edge velocity U_m and other boundary layer parameters at a particular location downstream of the nozzle. The velocity U_j was monitored by measuring the settling chamber and the static pressure in the test section using a Setra 239 pressure transducer with a range of ± 3.75 kPa. The flow temperature was monitored using a thermocouple placed near the nozzle exit which is connected to an Omega DP86T temperature meter. Present measurements for the far field sound were all made at three different velocities of $U_j = 30$ m/s, 45 m/s and 60 m/s.

Clark (2014) have measured the undisturbed boundary layer profiles at several different locations downstream of the nozzle exit plane for U_j between 20 and 60 m/s. From these measurements they were able to determine different boundary layer parameters at those locations. In present work, the location of interest is $x_n = 1372$ mm which, as will be seen later in §2.1.2, was where the vertical face of the forward facing step configuration was installed. The undisturbed boundary layer parameters for four different value of U_j at this location as measured by Clark (2014) have been shown in Table 2-1. In this table Re_{x_n} is the Reynolds number based on nozzle exit velocity and the streamwise distance where profiles were measured ($x_n = 1372$ mm), Re_j is the Reynolds number based on the nozzle exit velocity and nozzle exit height ($b = 12.7$ mm) and Re_m is the Reynolds number based on the maximum velocity U_m at the measurement location and nozzle exit height. The momentum thickness θ and displacement thickness δ^* were calculated according to following equations

$$\theta = \int_0^\delta \left(1 - \frac{U}{U_m}\right) \frac{U}{U_m} dy_n \quad (2-1)$$

$$\delta^* = \int_0^\delta \left(1 - \frac{U}{U_m}\right) dy_n \quad (2-2)$$

Since the measurement data is available only up to a finite distance above the wall, the data was extrapolated to the wall in order to evaluate the above integrals. Previous work on wall-jet flows (Wynanski *et al.* (1992) and Narasimha *et al.* (1973)) have shown that the boundary layer parameters in a fully developed wall-jet flow can be described by simple algebraic relationships involving nozzle opening height b and Reynolds number Re_{x_n} . These relationships for maximum velocity, displacement thickness, half height, boundary layer thickness and momentum thickness are shown in equations (2-3) to (2-7) respectively. In these equations $A_u, A_\delta, A_{y_{1/2}}, A, B, m, n$ and p are constants which can be found via measurements. Clark (2014) used their measurements and these relationships to determine the values of the constants that provided good fit to their measured data. The value of these constants as determined by them have been shown in Table 2-2 below.

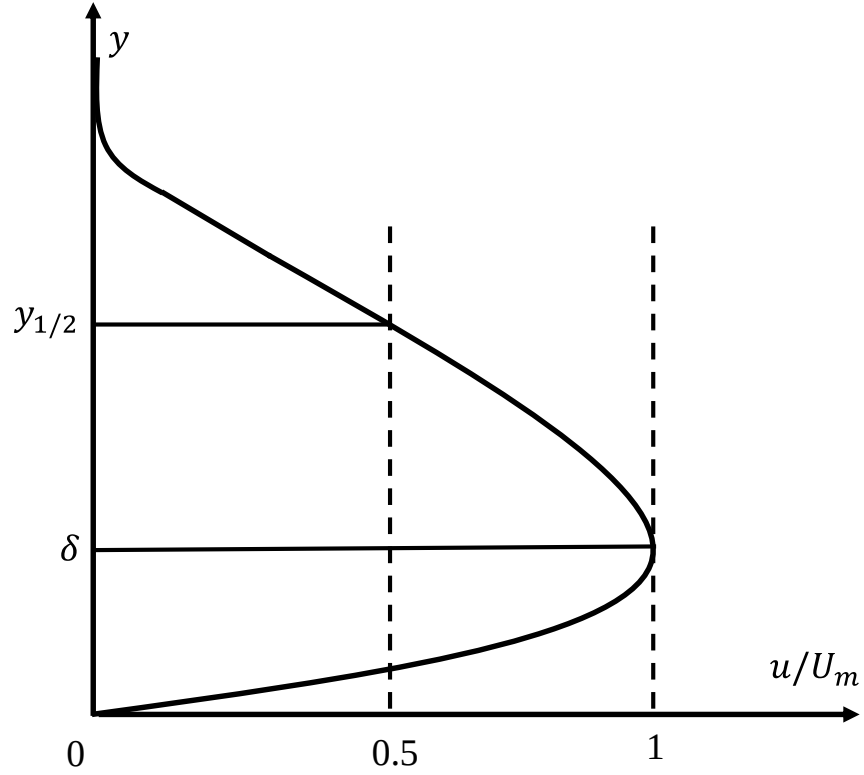


Figure 2-2 Structure of the mean velocity profile in a wall-jet flow.

Table 2-1 Undisturbed boundary layer parameters by Clark (2014).

$x_n(mm)$	$U_j(m/s)$	$U_m(m/s)$	$\delta(mm)$	$\delta^*(mm)$	$\theta(mm)$	Re_j	Re_{x_n}	Re_m	$y_{1/2}$
1372	30	10.3	16.3	0.9	0.8	23,356	2,522,442	10,291	106.4
1372	40	14.0	15.4	0.9	0.7	30,994	3,347,300	13,147	106.9
1372	50	17.6	15.7	0.8	0.7	38,413	4,148,617	16,711	107.8
1372	60	21.3	15.2	0.8	0.7	45,677	4,933,148	19,426	110.6

$$U_m = A_u U_j Re_j^{n+1} Re_{x_n}^n \quad (2-3)$$

$$\delta^* = A_\delta b Re_j^{p-2} Re_{x_n}^p \quad (2-4)$$

$$y_{1/2} = A_{y_{1/2}} b Re_j^{m-2} Re_{x_n}^m \quad (2-5)$$

$$\frac{\delta}{\delta^*} = A \quad (2-6)$$

$$\frac{\theta}{\delta} = B \quad (2-7)$$

Table 2-2 Empirical constants for equations (2-3) to (2-7) by Clark (2014).

A_u	A_δ	$A_{y_{1/2}}$	A	B	m	n	p
1.682	0.006	0.015	18.296	0.835	-0.47	0.909	1.065

The relationships in equations (2-3) to (2-7) along with the empirical constants in Table 2-2 were used to determine the boundary layer parameters for $U_j = 45$ m/s at which measurements of far field sound were made in the present work but no data was available. Table 2-3 below shows the undisturbed boundary layer parameters for nozzle exit velocities at which measurements were made in present work. Note that the data for $U_j = 30$ m/s and 60 m/s is the same as the measured data shown in Table 2-1.

Table 2-3 Undisturbed boundary layer flow parameters at velocities used in present measurements.

$x_n(mm)$	$U_j(m/s)$	$U_m(m/s)$	$\delta(mm)$	$\delta^*(mm)$	$\theta(mm)$	Re_j	Re_{x_n}	Re_m	$y_{1/2}(mm)$
1372	30	10.3	16.3	0.9	0.8	23,356	2,522,442	10,291	106
1372	45	15.6	15.7	0.9	0.7	35,034	3,783,663	15,437	109
1372	60	21.3	15.2	0.8	0.7	45,677	4,933,148	19,426	111

As mentioned earlier there is a 300 mm gap between the nozzle exit plane and the horizontal acoustic baffle (see Figure 2-1 (b)) the purpose of which was to enhance the two-dimensional core of the wall-jet. Figure 2-3 shows the spanwise velocity profile at $x_n = 1372$ mm downstream of the nozzle measured at $U_j = 60$ m/s for three different gaps between the baffle and the nozzle exit. The profile was measured at a distance $y_n = 6.4$ mm above the wall and the velocities shown have been normalized on the nozzle exit velocity. Looking at this figure it can be seen that for no gap between the nozzle and the baffle the two-dimensional core is only about 500 mm. However, as the gap width increases so does the extent of the two-dimensional core and for the gap width of 300 mm used in the present work the core is approximately 800 mm wide.

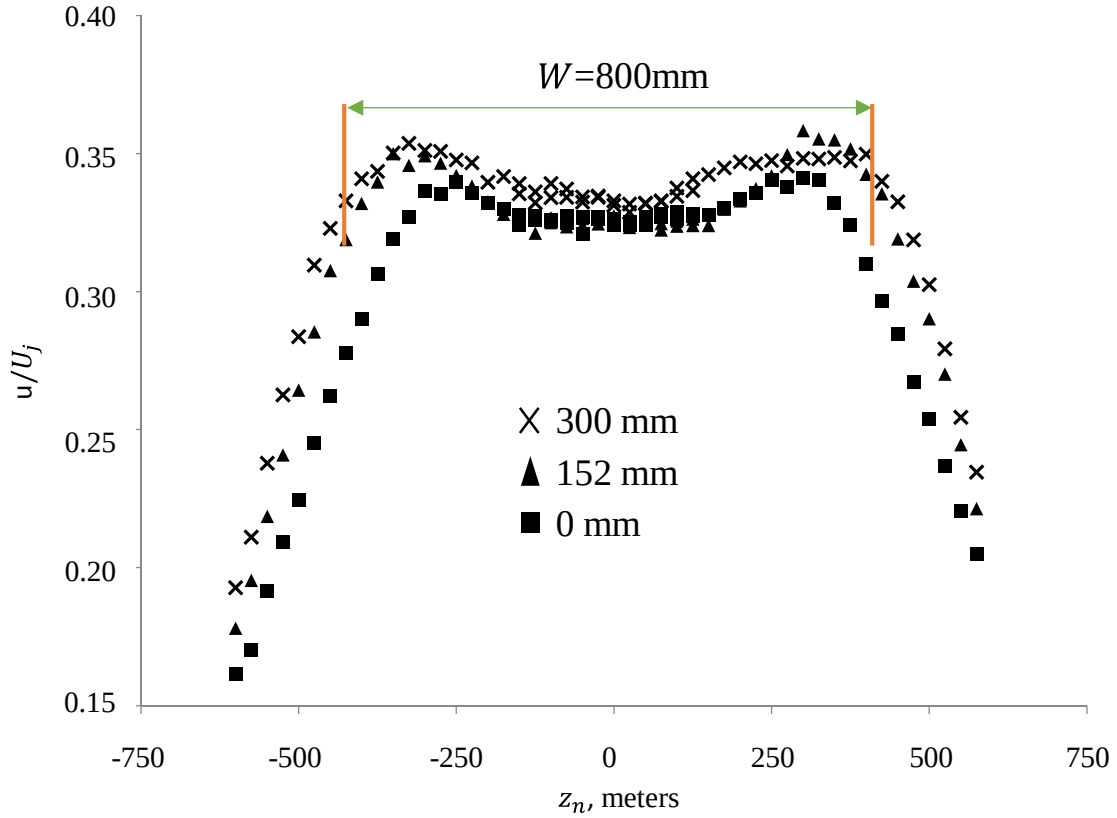


Figure 2-3. Spanwise undisturbed velocity profiles at $x_n = 1372$ mm and $y_n = 6.4$ mm for nozzle exit velocity of $U_j = 60$ m/s.

2.1.2 Surface Discontinuities

Measurements of far field sound were made for the following geometries:

1. Forward facing steps with rounded corners: six different rounded steps with rounding radii to step height ratios of $r/h = 0\%$, 6.25% , 12.5% , 25% and 100% . The step height for each case was $h = 12.7$ mm.
2. Gaps with rounded corners: Two types of gap geometries were considered. The first, known as type I, was where the forward facing step was kept unrounded while the backward facing step corner was rounded to $r_1/h = 0\%$, 12.5% and 50% . For this gap configuration four different gap width of $w = 1.12, 2, 4$ and 8 step heights were considered. The second type, known as type II, was where the backward facing step corner was kept unrounded while the forward facing step corner was rounded to $r_2/h = 0\%$, 6.25% , 12.5% , 25% , 50% and 100% . For this type II configuration only a gap width of 4 step heights was considered. The gap height for both the types was kept constant and same as forward facing steps i.e. $h = 12.7$ mm.
3. Swept Forward Facing Step: Four different sweep angles relative to the incoming boundary layer of $\alpha = 0^\circ, 5^\circ, 15^\circ$ and 30° were considered. Here $\alpha = 0^\circ$ refers to the unswept forward facing step. For each sweep angle four different rounding radii to step height ratio of $r/h = 0\%$, 12.5% , 25% and 50% were considered. The step height was the same as the two-dimensional steps and gaps i.e. $h = 12.7$ mm.

For each geometry being considered here the discontinuity height to undisturbed boundary layer thickness ratios are $h/\delta = 78\%$, 81% and 84% for undisturbed edge velocities of $U_m = 10.3$ m/s, 15.6 m/s and 21.3 m/s respectively. The Reynolds numbers based on the discontinuity height are $Re_h = 8,300, 12,450$

and 16,800 and flow Mach numbers for the three velocities (based on sound speed at 15°C) are $M = 0.03$, 0.045 and 0.06. The setup of each of these discontinuities will now be described in detail.

2.1.3 Rounded Forward Facing Step Configurations

The forward facing step setup used in the present work had a height of 12.7 mm. The setup was configured so that the inside corner of the step (regardless of rounding) was always $x_n = 1372$ mm from the nozzle exit. The undisturbed boundary layer statistics at this location for all three nozzle exit plane velocities at which measurements were made have already been shown in Table 2-3.

Figure 2-4 shows a two-dimensional view (in the streamwise/wall-normal plane) of the setup used to create an unrounded forward facing step on the wall-jet test plate. Note that in contrast to Figure 2-1 the flow in this figure is from right to left. This setup spanned the entire width of the wall-jet (1600 mm). The setup consists of two 12.7 mm high and 25.4 mm wide aluminum bars which are placed upstream of a 1219 mm long Lexan sheet of the same height. The Lexan sheet is held to the test plate with clamps placed along spanwise edges of the test plate outside the flow. Figure 2-5 (a) shows two of these clamps pictured as they were installed during measurements. Measurements with and without the clamps in position showed that their presence had no influence on the far field sound. Both the aluminum bars were attached to the test plate using countersunk bolts. The seams between the two aluminum bars and the aluminum bar and Lexan sheet were sealed using a 0.05 mm thick clear tape. The trailing edge of this assembly consists of a ramp with gentle gradient to ramp the flow back down to the wall-jet test plate. This trailing edge ramp is made of 0.8 mm thick aluminum shim which is held to the Lexan sheet and the test plate using 0.1 mm thick aluminum foil tape. As will be shown in §2.2.3 the use of this trailing edge ramp has minimal impact on the far field sound measurements since the flow velocity in this region is near zero due to its large distance from the nozzle. Figure 2-5 (b) shows this trailing edge ramp as installed during the measurements. Also shown is the aluminum foil tape used to hold this ramp to the wall-jet test surface.

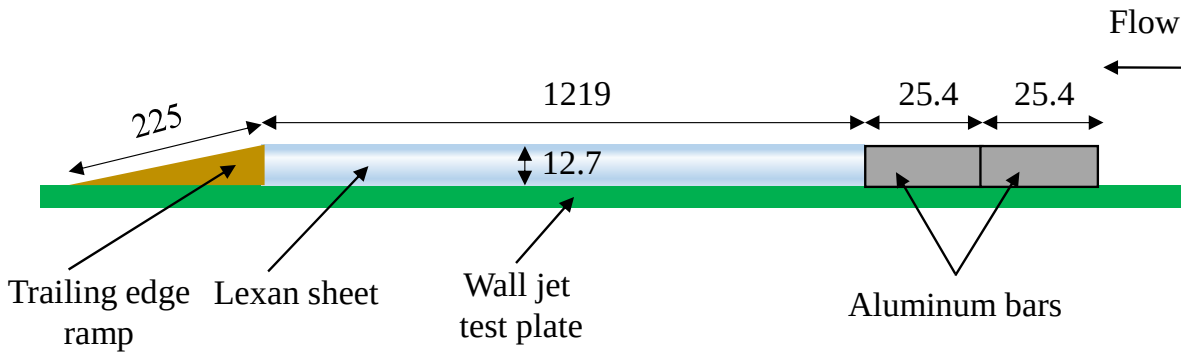


Figure 2-4 Schematic of forward facing step setup in Virginia Tech anechoic wall-jet facility (dimensions in mm).

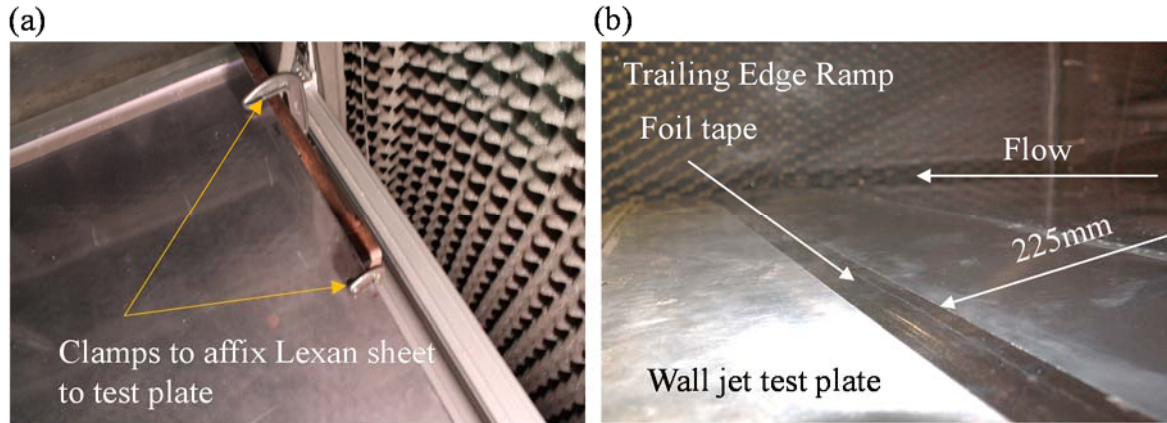


Figure 2-5 (a) clamps to affix Lexan sheet to the test plate and (b) trailing edge ramp.

Figure 2-6 offers a perspective view of the forward step assembly installed on the test plate to show its configuration in the spanwise direction. As seen in the figure the aluminum bars shown in Figure 2-4 are actually split into three which when combined together cover the entire span of the wall-jet test plate. The forward set of three bar sections were replaced to create rounding of the step corner. The bars used to create the rounding of the step corner have been shown in Figure 2-7. It should be noted that of the six rounding radii only $r/h = 0\%$ and 100% used a dedicated bar set. The other four radii were machined into diagonally opposite edges of two bar sets. This is illustrated in Figure 2-7 where the leading edge bar for $r/h = 50\%$ and 6.25% (machined edges on the top right and bottom left of the image) and that for $r/h = 12.5\%$ and 25% are shown.

Figure 2-8 shows a fully rounded forward step ($r/h = 100\%$) installed on the wall-jet test plate pictured during measurements. In this image the two sets of Aluminum bars, one which forms the rounded leading edge and another which is located immediately downstream of it can be seen. The Lexan sheet which forms the rest of the downstream surface of the step can also be seen in this figure.

The flow field coordinate system which will be followed throughout the text for rounded forward steps in the wall-jet boundary layer flow is depicted in Figure 2-9. The origin of the coordinate system lies at the inside corner of the forward step and at the spanwise center of the wall-jet test plate. The streamwise coordinate x is positive in the flow direction, the wall-normal coordinate y is positive away from the wall and the spanwise coordinate z completes the coordinate system according to the right hand rule.

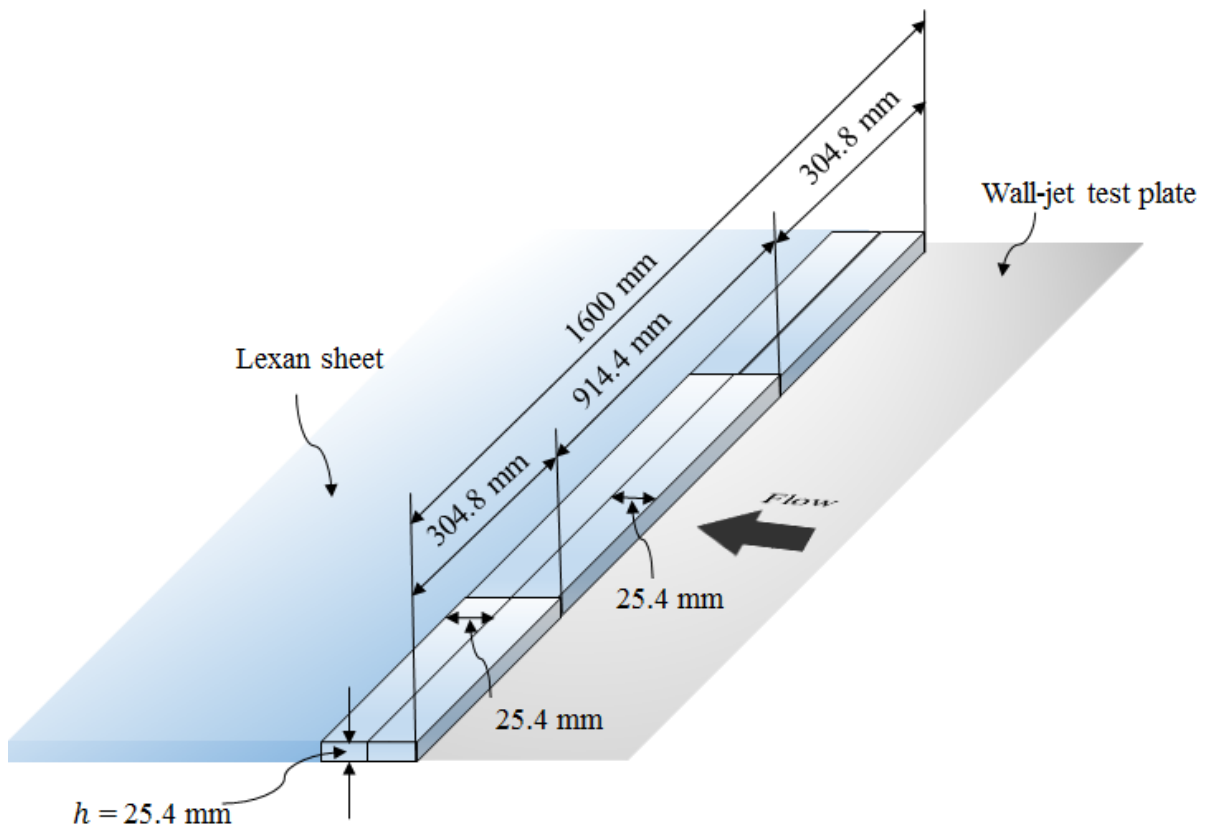


Figure 2-6 Perspective view of the forward step assembly (dimensions in mm).

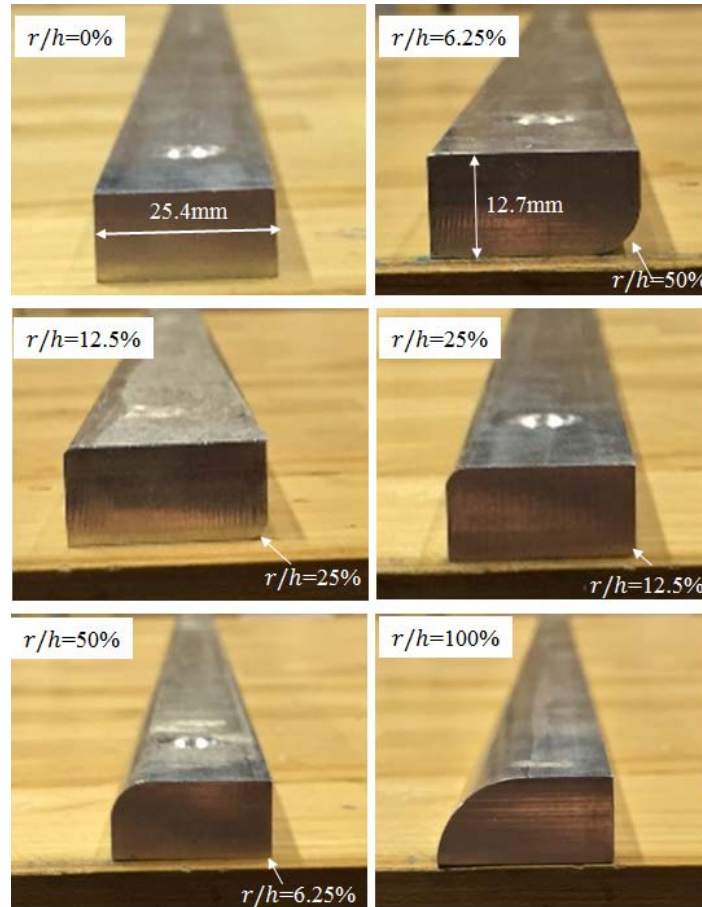


Figure 2-7 Leading edge aluminum bars used to create rounding of forward facing step corner.

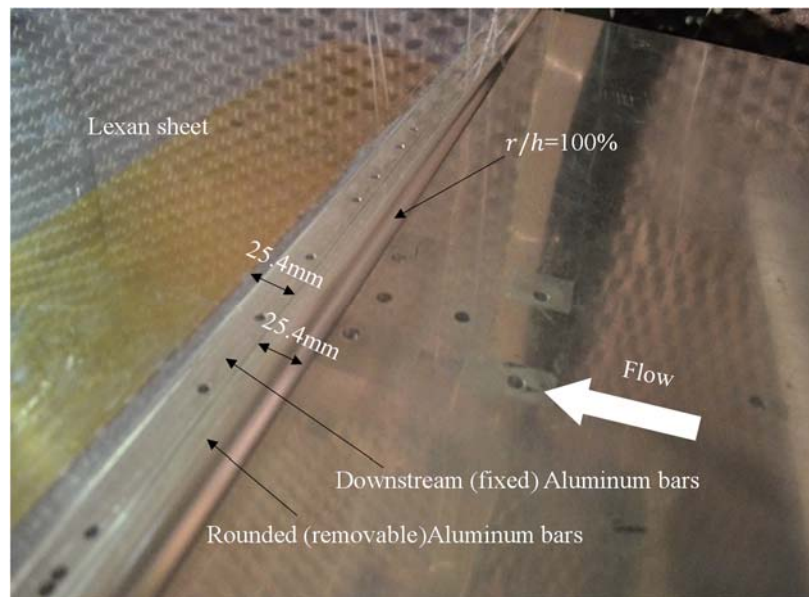


Figure 2-8 Forward facing step with rounding radii same as step height ($r/h=100\%$) setup on the wall-jet test plate.

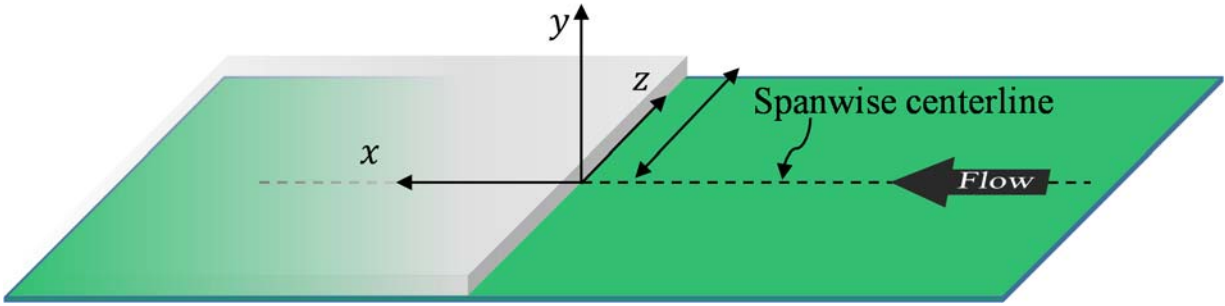


Figure 2-9 Near field coordinate system definition for rounded forward steps in wall-jet flow.

2.1.4 Rounded Gap Configurations

Since a two-dimensional gap is just a backward step placed upstream of the forward step, to create the gap configurations the forward step assembly described in the previous section was kept intact and a backward facing step configuration of the same height was installed upstream of it. Figure 2-10 shows a two-dimensional view of the gap configuration. The backward step portion of the gap is formed using a 305 mm long curved transition ramp with a 50.8 mm long flat surface downstream of which two aluminum bars with width of 25.4 mm and 44.6 mm are installed to extend the total flat surface upstream of the backward step to 121 mm (about 9.5 step heights). The transition ramp is made of 0.8 mm thick stainless steel and has a gentle gradient in the streamwise direction to ramp up the flow with minimum self-noise. The negligible effect of using this transition ramp on the far field sound will be described in §2.2.5.1. The trailing edge of this backward facing step configuration is similar to the leading edge of the forward step (see Figure 2-6) allowing the rounded bars to be used on either edge. Bar sections were bolted down to the wall-jet test plate using countersunk flat head bolts with seams sealed using 0.05 mm thick clear tape.

The leading edge of the transition ramp was double sided taped on the underside and taped to the wall-jet plate on the flow side using a 0.1 mm thick aluminum foil tape. The transition piece was held to the plate by clamping it to the wall-jet test plate with the clamps positioned so that they were outside the flow region. The seam between the transition ramp and the 44.6 mm wide aluminum bar was sealed using a 0.05 mm thick clear tape.

The gap width w shown in the figure was changed by keeping the forward facing geometry intact and moving the backward step segment upstream towards the wall-jet nozzle. The gap configuration shown in Figure 2-10 was modified to produce two types of rounded gap. Type I gaps with rounding of the backward facing step are illustrated in Figure 2-11 (a). Rounding radii to step height ratios considered are $r_1/h = 0\%$, 12.5% and 50%. For these gaps the gap width was also changed by moving the backward step setup upstream by $w = 1.12, 2, 4$ and 8 step heights. Type II gaps, with rounding of the forward step are shown in Figure 2-11 (b). In this case rounding radii considered were $r_2/h = 0\%$, 6.25%, 12.5%, 25%, 50% and 100%. The gap width for these gaps was kept fixed at 4 step heights.

The flow coordinate system which will be used for rounded gaps in wall-jet flow is the same as that used for forward steps (shown in Figure 2-9) since the forward step remains intact in each gap configuration. Table 2-4 shows a summary of all the two-dimensional steps and gaps configurations on which measurements were made in the present work.

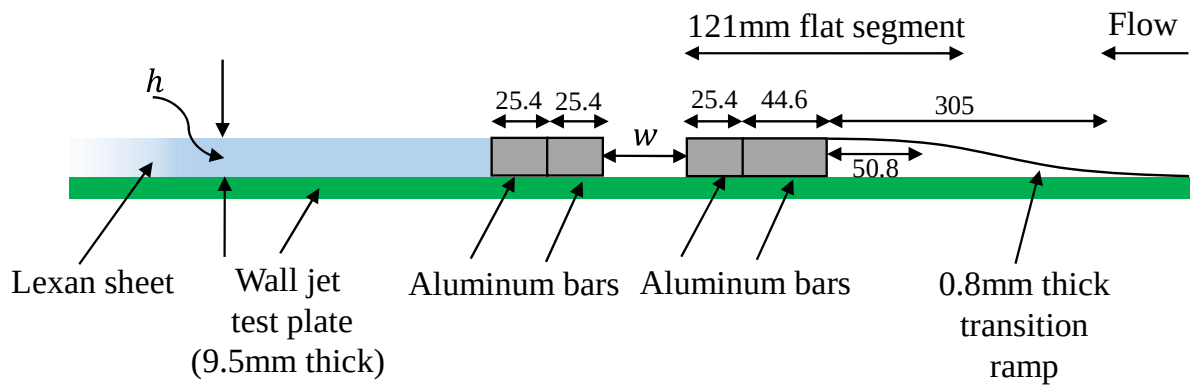


Figure 2-10 Schematic of gap setup in Virginia Tech anechoic wall-jet facility (dimensions in mm).

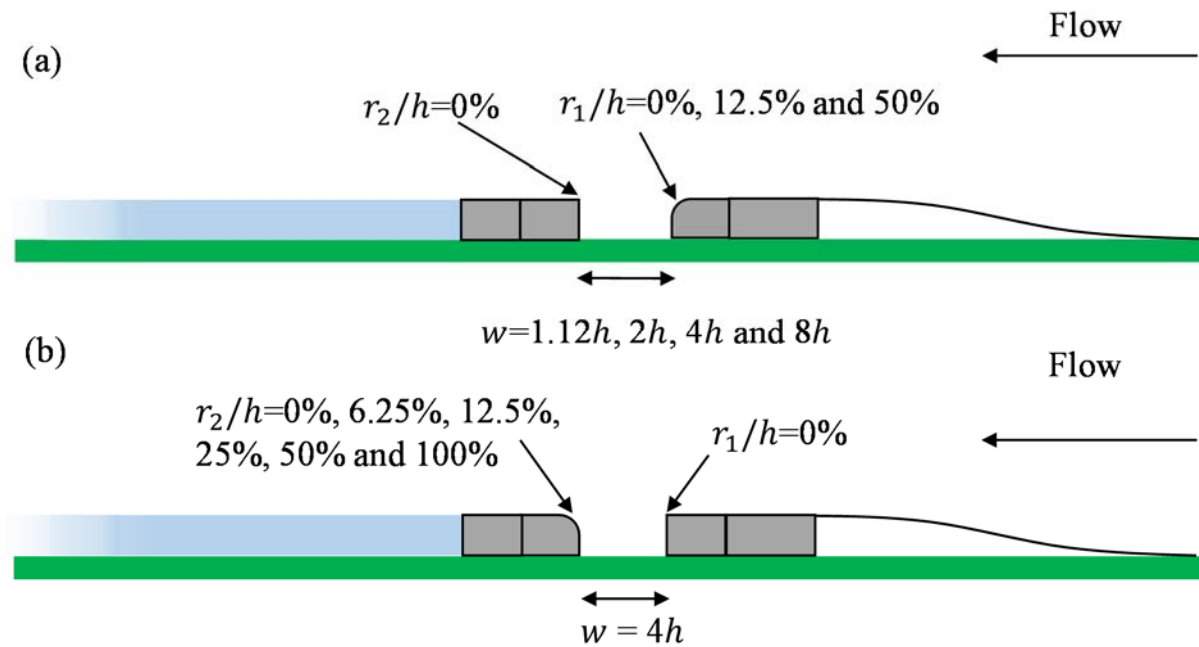


Figure 2-11 Schematic of gap configuration: (a) Type I gaps and (b) Type II gaps.

Table 2-4 Summary of 2D rounded steps and gaps configuration.

Rounded Forward Steps		Type I Gaps			
		$h(mm)$	$r_1/h(\%)$	$r_2/h(\%)$	w/h
$h(mm)$	$r/h(\%)$	12.7	0	0	1.12, 2, 4, 8
12.7	0	12.7	12.5	0	1.12, 2, 4, 8
12.7	6.25	12.7	50	0	1.12, 2, 4, 8
12.7	12.5	Type II Gaps			
12.7	25	$h(mm)$	$r_1h(\%)$	$r_2/h(\%)$	w/h
12.7	50	12.7	0	0	4
12.7	100	12.7	0	6.25	4
		12.7	0	12.5	4
		12.7	0	25	4
		12.7	0	50	4
		12.7	0	100	4

2.1.5 Swept Forward Facing Step Configurations

Rounded swept forward steps were created essentially by reversing the assembly used to generate the backstep portion of the gap geometry, and moving it downstream to place the step face at $x_n = 1372$ mm. This assembly is shown in Figure 2-12. This streamwise-limited step geometry (as compared to the baseline forward step of Figure 2-4) provided room to create sweep angles up to $\alpha = 30^\circ$ with respect to the incoming mean velocity vector without interfering with the walls of anechoic chamber. To increase the streamwise length of the forward step produced in this way, additional aluminum bars were installed between the removable leading edge and the transition ramp. As will be shown in §2.2.6.1, upon measurements of far field sound it was found that a 12 step height long flat surface downstream of the step location was sufficient to reproduce the sound being produced by the baseline forward step. The aluminum bar sections of the swept step were once again bolted in place and seams sealed using 0.05 mm thick clear tape. The side edges of transition ramp were clamped to the wall-jet test plate and its trailing edge was attached to the test plate using a double sided tape underneath and a 0.1 mm thick aluminum foil tape on the flow side. The seam between the ramp and the aluminum bar upstream of it was sealed using 0.05 mm thick clear tape.

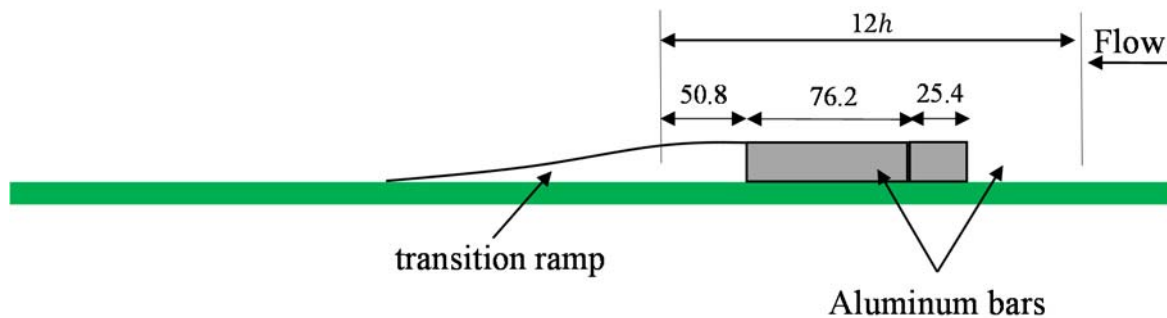


Figure 2-12. Illustration of finite streamwise geometry used to create swept forward steps in wall-jet facility (dimensions in mm).

To sweep the configuration depicted in Figure 2-12, the entire assembly was pivoted about a point 1 step height downstream of the unswept step face at the spanwise center of the wall-jet test plate (see Figure 2-13 and Figure 2-14). Sweeping the step brings one half of the step span upstream while the other half moves downstream. This means that for swept forward steps used in the present work there was a spanwise velocity gradient due to the varying distance of each point along the span from the nozzle exit plane. Sweeping the step also draws the outer edges (along the span) of the step assembly inboard, potentially exposing them to parts of the wall-jet flow. All the measurements of sound from swept forward facing steps were made by covering these exposed edges with Mylar. The effects of spanwise non-uniformity and the possible effects of the spanwise edges of the step are addressed as part of the discussion of results in §2.2.6.

The coordinate system that will be followed for swept forward facing steps is shown in Figure 2-15. The figure shows the spanwise line corresponding to the position of the vertical face of the unswept step as well as the rotation of this line when the step is swept. The unswept or tunnel fixed coordinates are the same as those for two-dimensional rounded forward steps described in Figure 2-9. The swept step coordinate system is denoted by x', y', z' and is also shown in Figure 2-15. The origin of this system is the inside corner of the swept step at the wall-jet centerline. The x' is the streamwise coordinate normal to the swept step vertical face and is positive downstream, y' is the wall-normal coordinate which is in the same direction as the unswept wall-normal coordinate y . The spanwise swept coordinate runs along the span of the swept step. Note that since the origin of swept coordinate system is always at the spanwise center of the wall-jet surface and the step is not swept at the leading edge the origin actually shifts slightly upstream with each sweep angle by a factor of step height and sweep angle as shown in the figure.

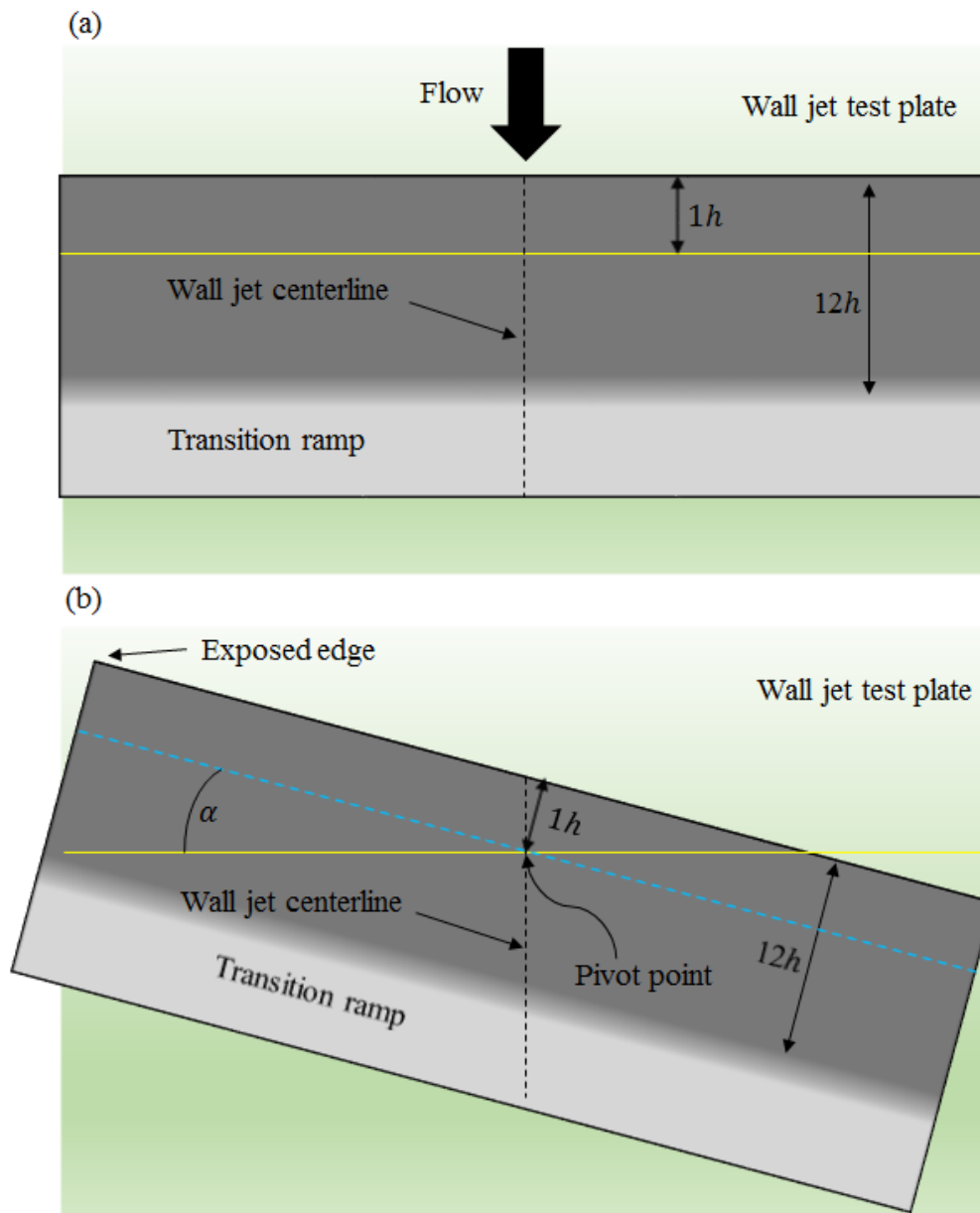


Figure 2-13 Illustration of rotating the streamwise finite forward step geometry to create swept forward facing steps. (a) Top view schematic of the unswept step. (b) Swept forward facing step obtained by rotating the entire assembly about a point which is located 1 step height downstream and at the spanwise center of the wall-jet test plate.

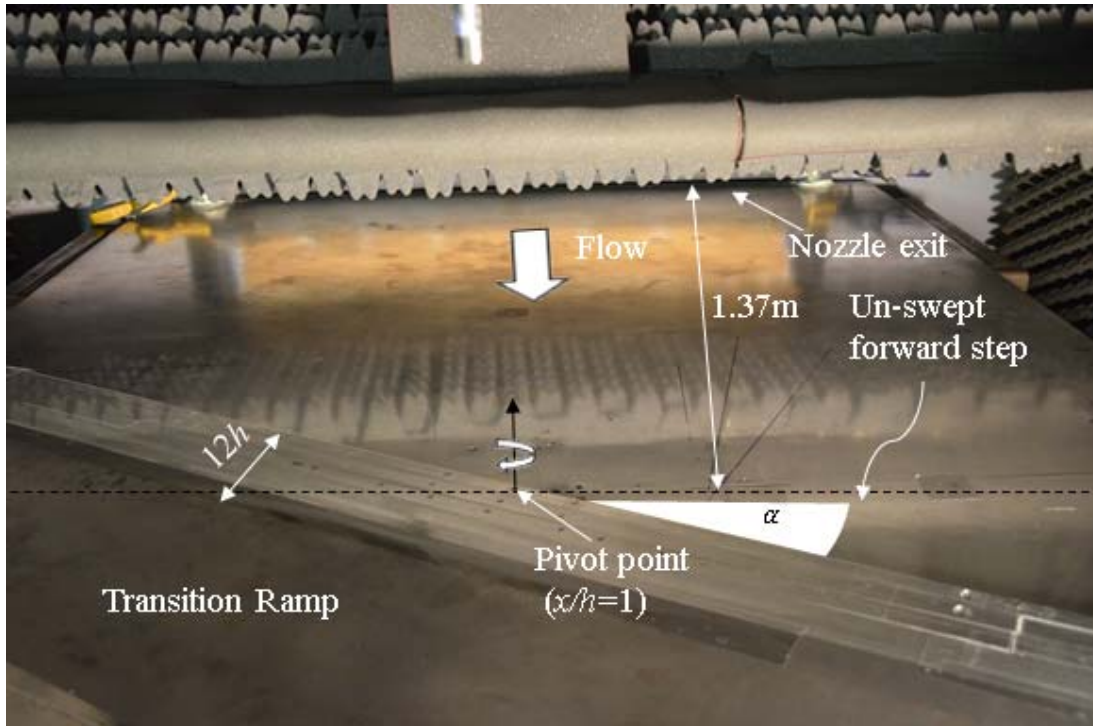


Figure 2-14. A swept forward facing step.

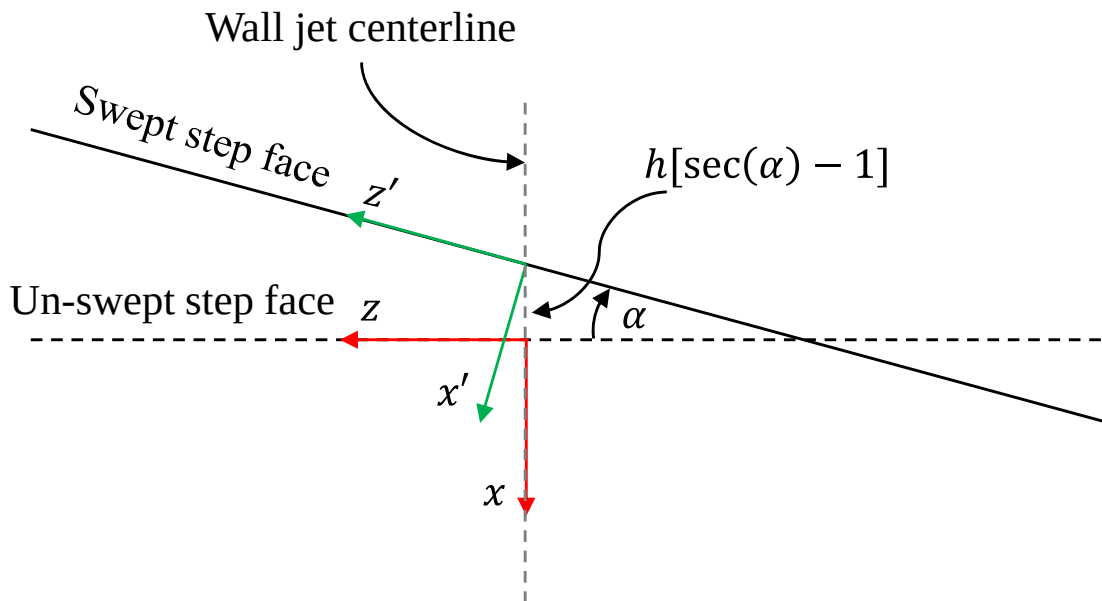


Figure 2-15 Coordinate system description for swept forward facing steps.

2.1.6 Far Field Sound Instrumentation

2.1.6.1 Rounded Forward Facing Steps and Gaps

Sound was measured using an array of 8 B&K 4190 microphones. These microphones have a diameter of 12.7 mm with the safety grid on (the actual diaphragm diameter is 11.1 mm), a flat frequency response between 3.2 Hz and 20 kHz (± 2 dB), and a dynamic range between 14.6 and 146 dB. The

sensitivity of each microphone was measured using a B&K type 4228 pistonphone operating at 251.2 Hz. The signal measured by these microphones was acquired using B&K Pulse LAN-XI modules. The signal was sampled using a sampling rate of 65536 Hz for 32 seconds. The sampled time series data was then divided into blocks of 8192 record length, a Hanning window was applied to each record with an overlap of 50% between each record. Each record was then Fourier transformed and averaged to yield the spectral densities of far field sound pressure.

Figure 2-16 (a) shows the location of each microphone in the far field array with respect to the forward step geometry. The entire array was installed such that the geometric center of each microphone lies in the x - y plane at center span, $z = 0$. The array configuration shown here was also used to measure far field sound from the gap configurations. As shown in the figure, a total of 8 microphones arranged in an arc with the same radial distance ($R = 578 \pm 1$ mm) from a common origin (inside corner of the forward step) were used to measure sound from two-dimensional steps and gaps. The directivity of the microphone location is defined such that $\theta = 0^\circ$ lies downstream of the step. Note that each microphone is installed in the region within the anechoic chamber where the flow velocity is zero and the uncertainty in the angular location of each microphone is $\delta\theta = 0.5^\circ$.

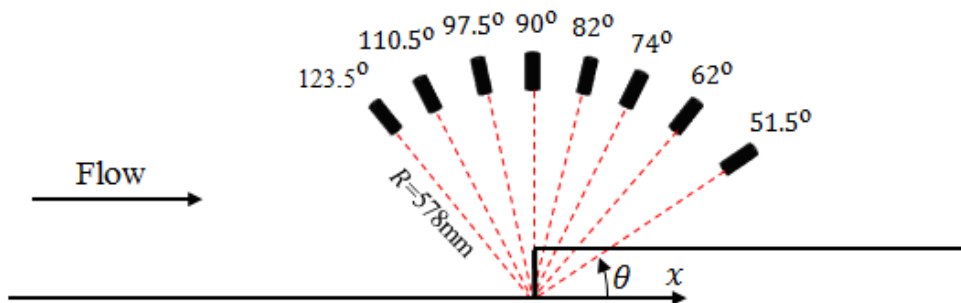
Figure 2-16 (b) shows the 8 B&K 4190 microphones installed in the wall-jet facility. As shown in the figure, the microphones are held by microphone holders provided by B&K which themselves are fastened to a gantry which is built using t-slotted Aluminum beams. This gantry is covered with acoustic grade foam to minimize any acoustic reflections. The microphone holders are such that the angle of the microphone (θ) can be changed by pivoting them about a point. The radial distance of the microphone (R) was controlled by changing the length of the stem of the holder. A combination of a ruler and a digital inclinometer was then used carefully to set the radial distance and the angle of each microphone. Note that the radial distance and angle mentioned here are to the center of the diaphragm of each microphone.

2.1.6.2 Swept Forward Steps

Measurements were again made using B&K 4190 microphones. Similar to the two-dimensional steps and gaps, the pressure signal was acquired using a sampling rate of 65536 Hz for 32 seconds and was Fourier transformed according to the scheme already described in §2.1.6.1. There were two separate microphone configurations that were used to measure the far field sound. The first configuration which will be referred to as the *fixed microphone arc* is the same configuration used for rounded forward facing steps and gaps described in §2.1.6.1 and shown in Figure 2-16. This microphone configuration is tunnel fixed i.e. it does not change with the sweep angle of the step. The second configuration which will be referred to as *moving microphone arc* (see Figure 2-17) consists of two microphones whose locations change with the sweep angle of the step and so they remain in a sweep local coordinates. The first of the moving microphones is positioned so that it is always directed in a plane perpendicular to the step face regardless of the sweep angle. This microphone is located upstream of the step at a radial distance of $R' = 578 \pm 1$ mm and its line of sight is directed to the inside corner of the step at a spanwise position (in sweep local coordinates) of $z' = 11h$. The observer angle of this microphone with respect to the step-normal streamwise coordinate x' is $\theta' = 123.5^\circ \pm 0.5^\circ$. Note that the location of this microphone in sweep local coordinates is analogous to the most upstream microphone in the *fixed microphone arc*. Figure 2-17 shows the swept forward step with the fixed microphone arc and the microphone perpendicular to the step face. The figure also shows the streamwise sweep local flow coordinate x' , the spanwise sweep local flow coordinate z' and the second moving microphone to the side. This second moving microphone was positioned so that it was always in the plane of the swept step face. This microphone also points to the inside corner of the swept step at a point which is 11 step heights along the span of the swept step ($z' = 11h$). The radial distance from the inside corner of the swept step to this microphone is also $R' = 578$ mm. Figure 2-18 shows a perspective view schematic of the moving and fixed microphone positions with respect to the swept step. The figure also shows the definition of the $R' - \theta'$ coordinate system used to describe the positions of these microphones. In the figure the 8 fixed microphones are aligned with the flow direction and the two microphones (shown in red) are normal and parallel to the swept step face plane. The angle θ' is defined as

the angle between the moving microphone line of sight and the line normal to the step face while the angle ψ refers to the angle between the spanwise coordinate z' and the line of sight of the in-plane microphone.

(a)



(b)

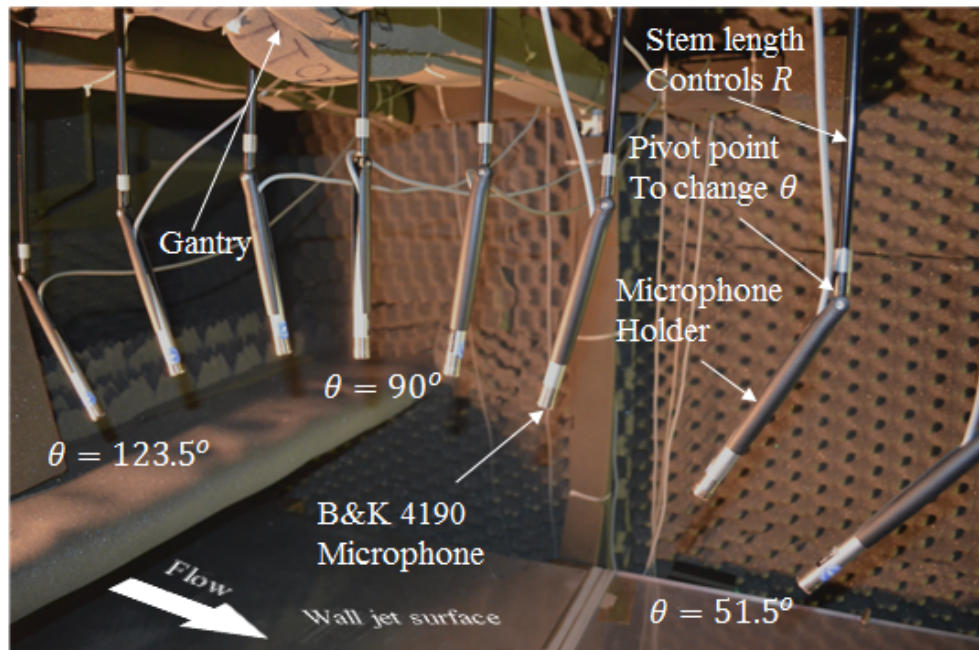


Figure 2-16 Microphone array used to measure far field sound from rounded forward steps and gaps: (a) Radial and angular location of each microphone with respect to the forward step, the geometric center of each microphone lies in the same plane and (b) Microphones installed in the wall-jet facility.

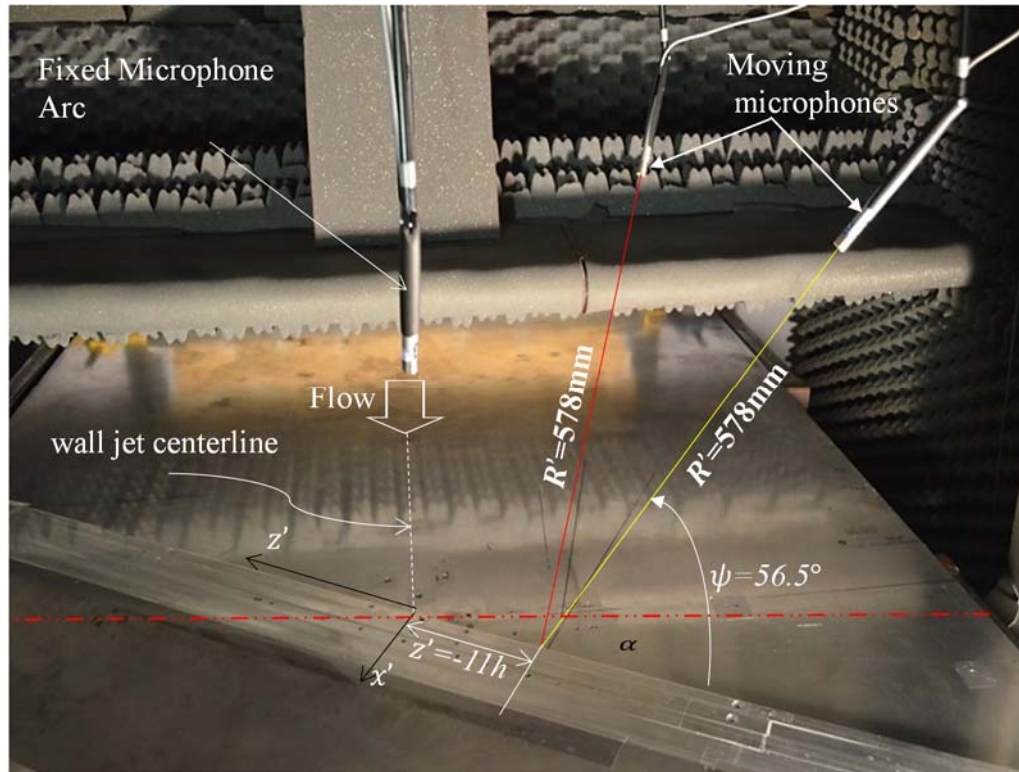


Figure 2-17. Far field sound measurement setup for swept forward steps.

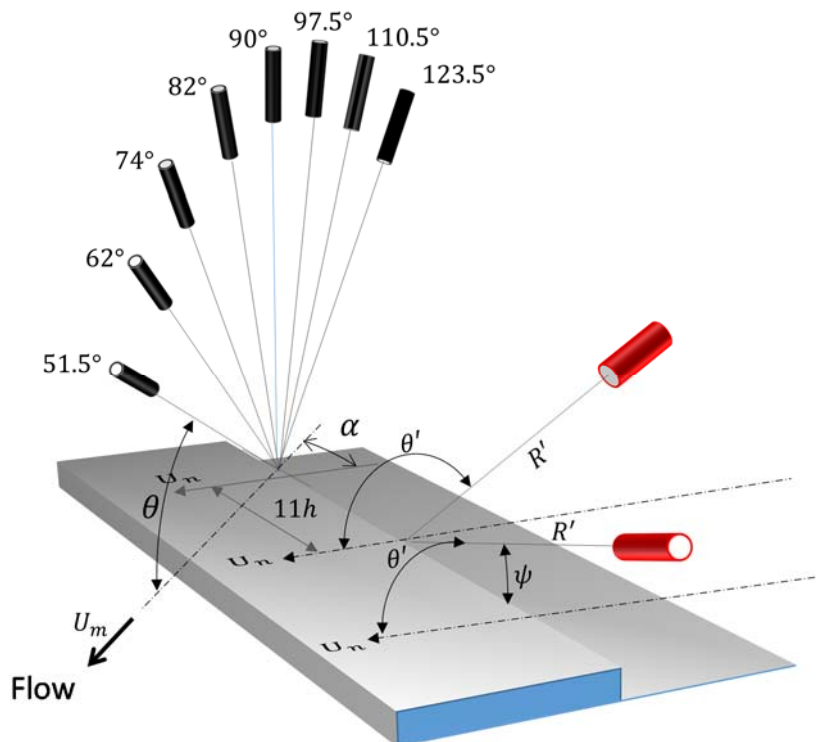


Figure 2-18. A perspective view schematic showing the moving microphones which remain in sweep local coordinates and fixed arc microphones which remain in the tunnel fixed coordinates.

2.2 Results and Discussion

2.2.1 Rounded Forward Steps Flow Structure

The flow over a forward facing step experiences formation of a separation bubble upstream of the step and a separation bubble downstream of it (see §1.2 for a detailed description of forward facing step flow structure). The streamwise length of these separation bubble and their dependence on the corner rounding is of interest since it is this separated flow region which is believed to be the fundamental sound source in step noise. The separation and reattachment lengths for rounded forward steps were measured using a tuft wand. This wand consisted of a thin rod which has a sharp needle point end to which a fluorescent thread of 0.04 mm diameter was attached. The wand was traversed in the vicinity of the step location under UV light and its motion was visually observed to determine the separation and reattachment lengths. All measurements were only made for a single velocity of $U_m = 21.3$ m/s.

To determine the downstream reattachment length the wand was traversed from the downstream surface of the step where the flow is attached towards the expected separation bubble on top surface of the step. The point where the reversed flow first appeared forcing the thread in the upstream direction was taken to be the reattachment point. On the upstream side, the wand was traversed from the inside corner of the step towards the upstream direction and the first appearance of downstream flow was taken to be the upstream separation point.

Table 2-5 shows the separation length and reattachment lengths in step heights for each rounded forward step along with the uncertainties (denoted by Δ) in step height associated with each measurement. Also shown in the table are the reattachment lengths calculated by Hao and Wang (2014) using LES. Note that as listed at the top of the table their step height (in terms of boundary layer thickness δ) is more than 3.5 times smaller than the present work while their Reynolds number (based on momentum thickness θ) is more than 5 times larger. Looking at the table it can be seen that as the step is rounded slightly to $r/h = 6.25\%$ the upstream separation length decreases slightly but rounding the step corner further has negligible effect on this length. Although Hao and Wang (2014) do not show the upstream separation lengths as a function of rounding, they do mention a similar independence of corner rounding on the separation lengths. Unlike the separation length the reattachment lengths are a strong function of rounding and they drop consistently as the rounding of the corner is increased. Note that for the three larger rounding shown here ($r/h \geq 25\%$) the uncertainties in the measurement of reattachment length are quite large due to a weaker separation bubble. The reattachment lengths for $r/h = 0\%$ and 12.5% are in close agreement with Hao and Wang's (2014) results and any differences might be a result of difference in the Reynolds number between measurements and computation. For $r/h \geq 25\%$ the agreement is slightly poorer possibly due to high uncertainties in the measured reattachment lengths.

Table 2-5 Separation and Reattachment lengths for rounded forward steps at $U_m = 21.3$ m/s.

$h/\delta = 84\%, Re_\theta = 926$					$h/\delta = 26\%, Re_\theta = 4755$
r/h	x_S/h	$\Delta x_S/h$	x_R/h	$\Delta x_R/h$	x_R/h Hao & Wang (2014)
0%	2	0.4	2.7	0.2	2.44
6.25%	1.6	0.4	1.6	0.2	-
12.5%	1.6	0.4	1.2	0.2	1.33
25%	1.7	0.4	1	0.5	0.86
50%	1.7	0.4	0.9	0.5	0.55
100%	1.6	0.4	0.9	0.5	0.47

2.2.2 Far Field Sound Data Presentation Convention

The results include spectral densities of far field sound pressure. Therefore, a brief discussion of how this quantity and others derived from it will be presented is warranted. The spectral densities were estimated by Fourier transforming the time series pressure data from the microphones using periodogram method. We will use the symbol $\Phi_{pp}(f)$ to denote the power spectral density measured by the far field microphones as a function of frequency. The autospectral density of far field sound pressure is given by

$$\Phi_{pp} = \frac{1}{T} E[P(f)P^*(f)] \quad (2-8)$$

where * represents the complex conjugate and $P(f)$ is the Fourier transform of the time series pressure signal, f is frequency in Hz, $E[]$ is the expected value operator and T is the time period. The computation of equation (2-8) from measurement data involves ensemble averaging to reduce uncertainty. Note that the units of spectral densities Φ_{pp} is Pa^2/Hz . The far field spectral densities will be presented on a log scale with the abscissa representing frequencies and the ordinate representing the spectral densities on a dB scale. If the spectral densities are shown as Sound Pressure Levels (SPL) then they are referenced to a pressure of $p_{ref} = 20 \mu\text{Pa}$, i.e.

$$\text{SPL} = 10 \log_{10} \left[\frac{\Phi_{pp}(f)}{p_{ref}^2} \right] \quad (2-9)$$

If the spectral density is not being represented as SPL then the reference pressure is not used. For example the ratio in dB between spectral densities from two microphones denoted microphone 1 and microphone 2 is given by

$$\text{SNR} = 10 \log_{10} \left[\frac{\Phi_{pp_1}(f)}{\Phi_{pp_2}(f)} \right] \quad (2-10)$$

The anechoic wall-jet facility used in present work has a high SNR as demonstrated by some previous studies made in this facility (e.g. Devenport *et al.* (2010) and Catlett *et al.* (2014)). The SNR is defined as

$$\text{SNR} = 10 \log_{10} \left[\frac{\Phi_{pp}(f)}{\Phi_{nn}(f)} \right] \quad (2-11)$$

where Φ_{pp} is the spectral density measured with the discontinuity installed on the wall-jet surface and Φ_{nn} is the spectral density measured without it present on the wall-jet test surface. Figure 2-19 shows the signal to noise ratio for sound measured from an unrounded ($r/h = 0\%$) forward step by the most upstream microphone ($\theta = 123.5^\circ$) for nozzle exit velocity of $U_j = 60 \text{ m/s}$ which corresponds to the undisturbed edge velocity of $U_m = 21.3 \text{ m/s}$ at the step location. These details have been listed in the caption of the figure. Throughout the discussion the details of the measurement conditions will be presented in this manner i.e. the caption will list the type of surface discontinuity, the undisturbed edge velocity at step location and the observer position. Also note that the frequency axis is restricted between 200 Hz and 20 kHz which is the region of interest in present work and all sound spectra shown in present work will be bound between these limits. As seen in Figure 2-19, at frequencies above about 450 Hz the SNR is greater than 1dB (SNR values less than 1dB have been shown in gray on the figure) and at the highest frequencies SNR can reach as high as 14-15 dB.

As suggested by Devenport *et al.* (2010) the sound from the undisturbed flow in the wall-jet facility, i.e. the sound from the jet and/or nozzle, may be assumed uncorrelated with the sound from the test artifact

placed on the wall-jet surface. The sound spectrum from the undisturbed flow can then simply be subtracted from the sound spectrum measured with the test artifact to decontaminate it. This subtraction process is demonstrated in Figure 2-20 which shows the sound spectrum from the forward facing step and the undisturbed boundary layer along with the subtracted sound spectrum. Note that the subtracted spectrum is only shown where the SNR is above 1 dB. Any spectral data points which lie below 1 dB threshold have not been shown in this figure. The subtracted spectrum shown in Figure 2-20 is given by

$$\Phi_{pp,sub}(f) = 10 \log_{10} \left[\frac{\Phi_{pp}(f) - \Phi_{nn}(f)}{p_{ref}^2} \right] \quad (2-12)$$

To further reduce the uncertainty in the subtracted spectrum the data from both the surface discontinuity and undisturbed boundary layer was binned with 12 bins per octave before the subtraction was performed (the results being scaled back to Pa²/Hz after binning). The binned background subtraction procedure is demonstrated in Figure 2-21. This shows that after binning the background subtracted sound from forward facing step is clearly visible through a broad frequency range. Unless otherwise stated all the far field sound results from surface discontinuities in wall-jet flow will be presented as background subtracted spectra with subtraction performed after binning the data with 12 bins per octave.

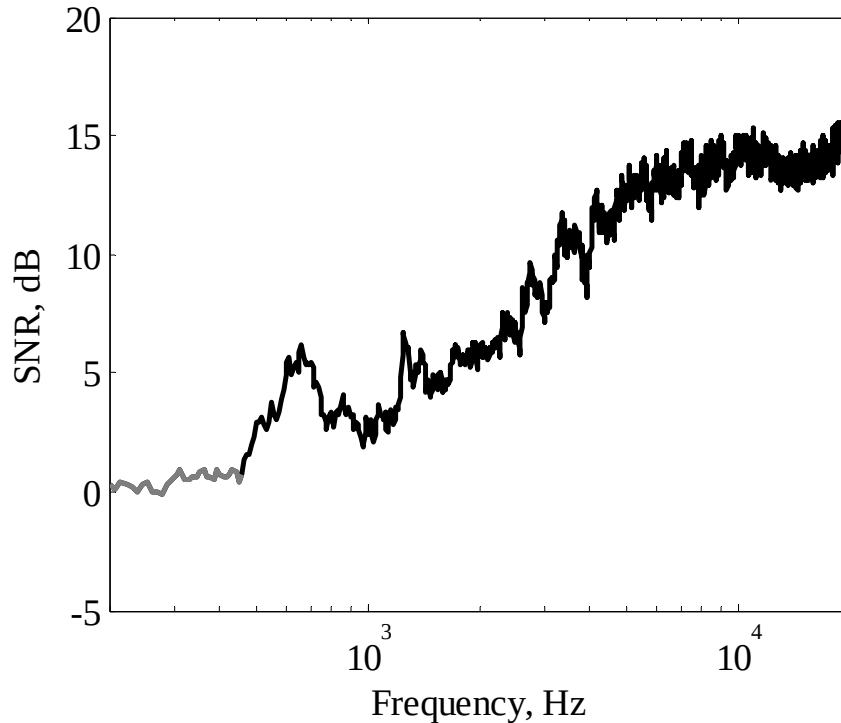


Figure 2-19 Signal to noise ratio for sound measurement for a forward facing step (SNR < 1dB highlighted in gray) $r/h = 0\%$, $U_m = 21.3$ m/s, $\theta = 123.5^\circ$.

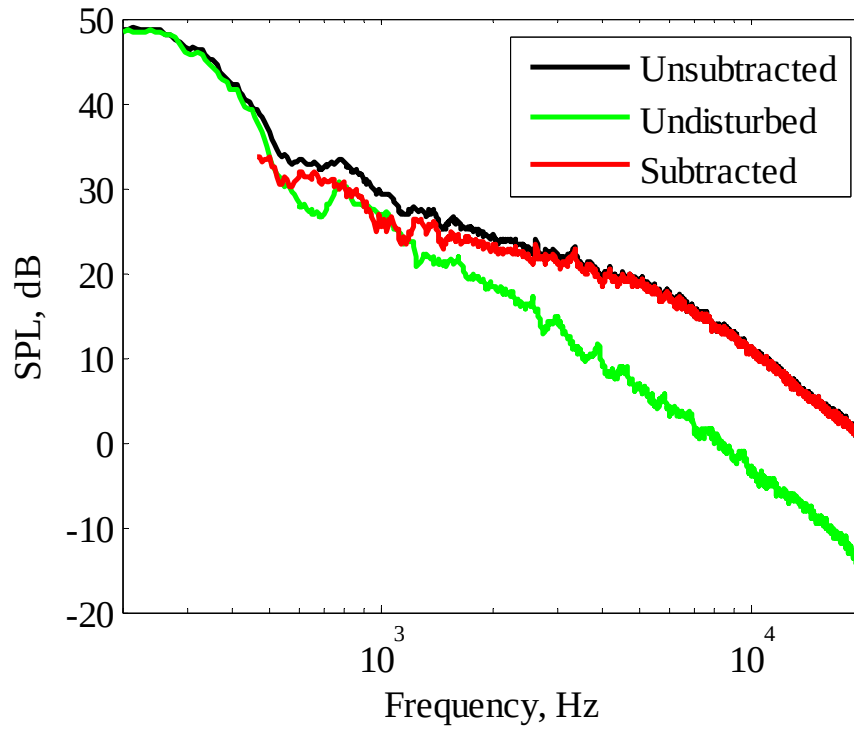


Figure 2-20 Sound spectrum measured for forward facing step, undisturbed boundary layer and background subtracted spectrum, subtracted spectrum only shown for frequencies where SNR > 1dB. $r/h = 0\%$, $U_m = 21.3$ m/s, $\theta = 123.5^\circ$.

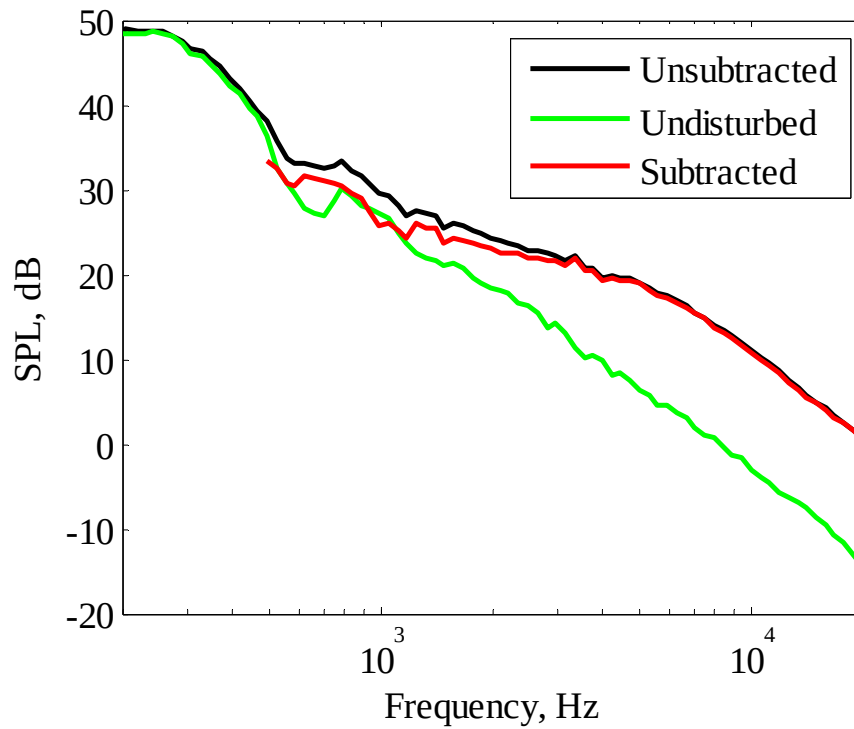


Figure 2-21 Background subtraction after binning both forward facing step and undisturbed boundary layer sound spectrum with 12 bins per octave. Subtracted spectrum only shown for SNR > 1dB. $r/h = 0\%$, $U_m = 21.3$ m/s, $\theta = 123.5^\circ$.

Setting the background subtraction criteria to $\text{SNR} < 1\text{dB}$ helps get rid of most of background sound. The residual uncertainty in the subtracted spectra comes from the spectral density estimation of both the spectra measured with a surface discontinuity installed and the background spectra. The total uncertainty in the subtracted spectrum is given by

$$\delta[\Phi_{ss}(f)] = \sqrt{\left\{ \frac{\partial \Phi_{ss}(f)}{\partial \Phi_{pp}(f)} \delta[\Phi_{pp}(f)] \right\}^2 + \left\{ \frac{\partial \Phi_{ss}(f)}{\partial \Phi_{nn}(f)} \delta[\Phi_{nn}(f)] \right\}^2} \quad (2-13)$$

here Φ_{ss} represents the subtracted sound spectrum, Φ_{pp} the spectrum measured with a discontinuity in place, Φ_{nn} the background sound spectrum, and $\delta[\Phi_{pp}(f)]$ and $\delta[\Phi_{nn}(f)]$ the uncertainties in these spectral values. At a particular frequency these uncertainties can be expected to behave as the uncertainty in the mean square pressure, i.e.

$$\delta[\Phi_{pp}(f)] = \frac{4\Phi_{pp}(f)}{\sqrt{N}} \quad (2-14)$$

$$\delta[\Phi_{nn}(f)] = \frac{4\Phi_{nn}(f)}{\sqrt{N}} \quad (2-15)$$

where N is the number of independent records used in the ensemble averaging to obtain estimates of spectral density. For the present work the number of independent records used in averaging is 256. However, once the data is bin combined N becomes a function of frequency and is given by

$$N(f) = N_{rec}n_b(f) \quad (2-16)$$

Where $N_{rec} = 256$ and n_b refers to the number of frequency estimates combined together to form a $1/12^{\text{th}}$ octave bin. The value of N then increases with frequency, lowering uncertainties in particular at higher frequencies. To find the uncertainty in dB of the subtracted spectra we can now differentiate equation (2-13) with respect to Φ_{pp} and Φ_{nn} and substitute equations (2-14)-(2-16) in the result to obtain the total uncertainty in the subtracted spectrum as

$$\delta[\Phi_{ss}(f)]_{dB} = \frac{40}{\ln(10)\sqrt{N}} \frac{\sqrt{\Phi_{pp}^2(f) + \Phi_{nn}^2(f)}}{\Phi_{pp}(f) - \Phi_{nn}(f)} \quad (2-17)$$

The uncertainty in the post-binning background-subtracted spectrum shown in Figure 2-21 found using equation (2-17) is shown in Figure 2-22. Note that similar to the subtracted spectrum the uncertainty is only shown where $\text{SNR} > 1\text{dB}$. As shown in the figure the uncertainty for this particular spectrum is quite low and remains below about 2 dB throughout the frequency range. Also note that as expected the uncertainty decreases with increasing frequency. Though for this particular case the uncertainty is low it can vary depending on the SNR for different cases.

Uncertainty values were calculated for all the background subtracted sound spectra presented in this chapter using equation (2-17). The subtracted sound spectra in the discussion to follow will only be shown for frequencies where the computed uncertainty was less than 2dB.

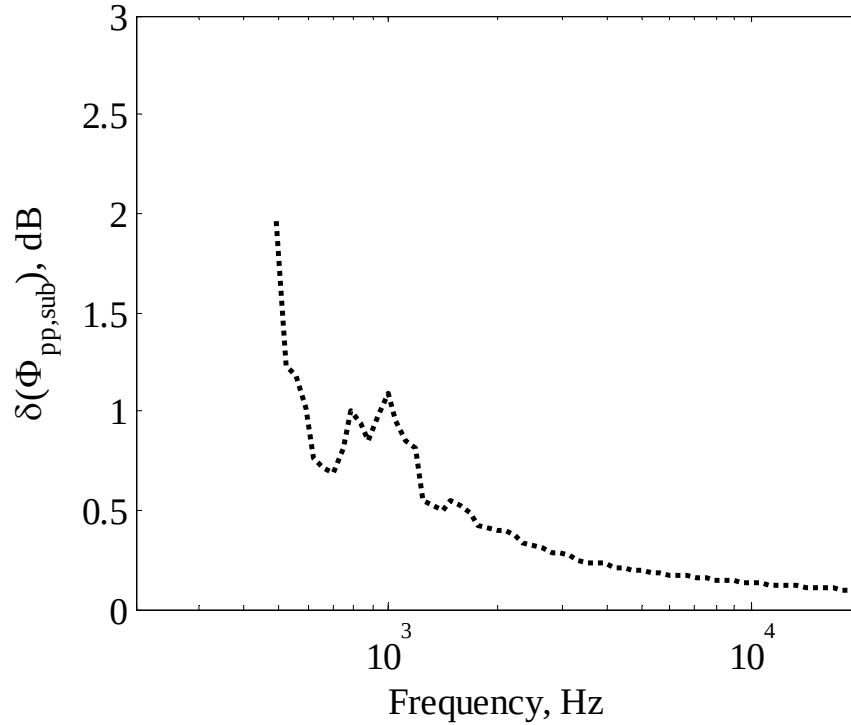


Figure 2-22 Uncertainty in the subtracted sound spectrum (in dB) of Figure 2-21.

2.2.3 Sound from Outside the Two-Dimensional Core and the Trailing Edge Fairing

2.2.3.1 Sound from outside the Two-Dimensional Core

As highlighted in §2.1.1.1 the two-dimensional core of the wall-jet covered only about 50% of the 1600-mm span of two-dimensional step or gap configurations. Therefore it was necessary to demonstrate that no significant sound was generated by the portions outside this region.

To verify this, first an unrounded forward step which spanned the entire width of the wall-jet plate was installed and far field sound was measured using the microphone array. As mentioned in §2.1.3 and shown in Figure 2-6, the leading edge of the step configuration was made of 3 different aluminum bar sections, one bar with a 914.4 mm span was placed at the center of the plate and two bars with a 304.8 mm span each were placed on either side. Sound was then measured with the two 304.8 mm bars at the spanwise limits of the unrounded step replaced with bars producing fully rounded ($r/h = 100\%$) steps here. This configuration is depicted in Figure 2-23. Finally, sound was measured with the junctions between unrounded and rounded sections of step were masked smoothly using Mylar.

The sound spectra measured by the most upstream and downstream microphones in the arc ($\theta = 123.5^\circ, 51.5^\circ$) for the three configuration just described are shown in Figure 2-24 (a) and (b) respectively. Note that the spectra shown here are not background subtracted. At either observer location the spectra from the three configurations are identical. This suggests that the region outside the central 914-mm of the step span ($z = \pm 457$ mm) was not producing significant sound. As shown in Figure 2-3, the central 914-mm of the step span is dominated by the two-dimensional portion of the wall-jet, suggesting that the three-dimensional portions of the wall-jet also were not contributing significantly to the far field sound. This is consistent with the earlier study of step noise in this facility by Catlett *et al.* (2010).

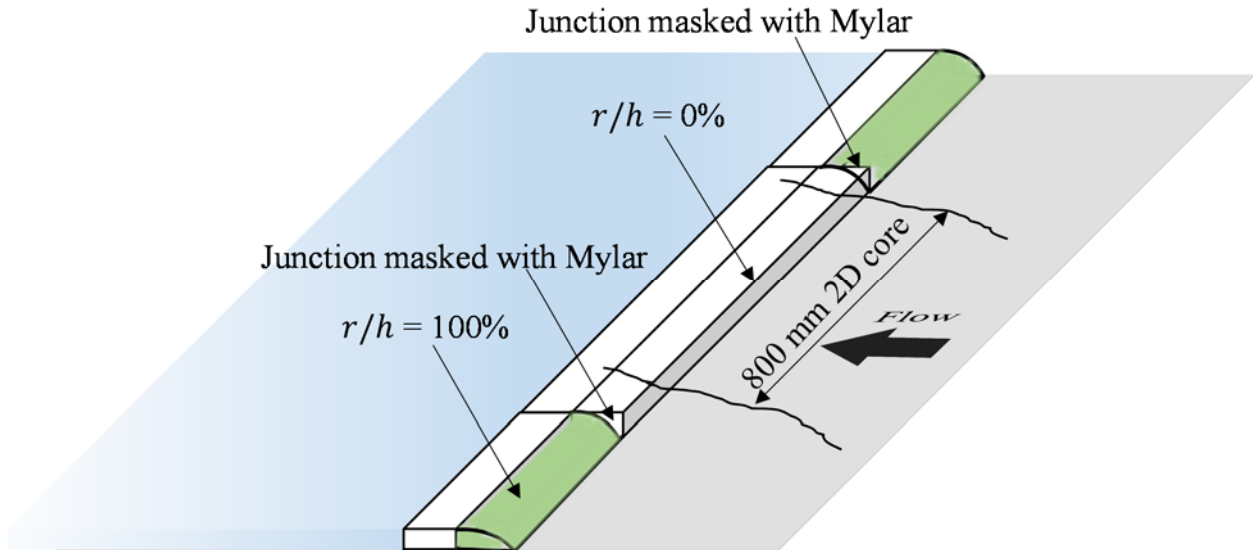


Figure 2-23 Forward step setup to measure the influence of flow region outside the two-dimensional core of wall-jet on far field sound.

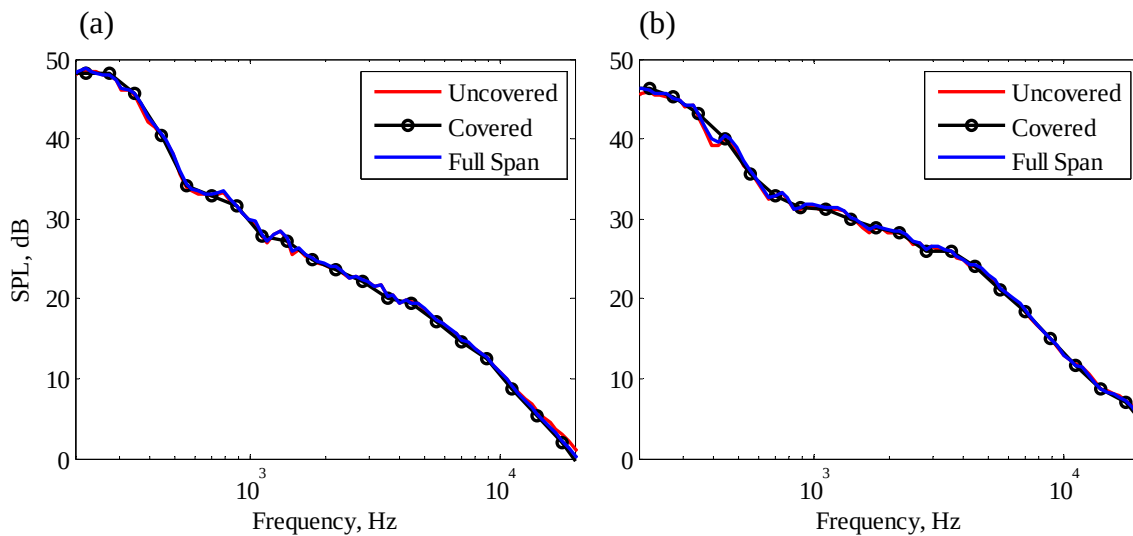


Figure 2-24 Influence of limited two-dimensional core of wall-jet boundary layer on far field sound. $r/h = 0\%$, $U_m = 21.3$ m/s. (a) $\theta = 123.5^\circ$, (b) $\theta = 51.5^\circ$.

2.2.3.2 Sound from the Trailing Edge Fairing

The assembly used to study forward step flows (discussed in §2.1.3) included a trailing edge fairing which was used to ramp down the flow smoothly to the wall-jet test plate in order to eliminate any sound that might be created by the sudden discontinuity there. Note that the streamwise length of the upper surface of the step before the flow is ramped down is approximately 1270 mm or 100 step heights. In order to test the effects of using this trailing edge ramp, far field sound was measured for the unrounded step with and without this ramp. Figure 2-25 (a) and (b) show the unsubtracted sound spectra measured with and without the ramp at $\theta = 123.5^\circ$ and 51.5° respectively. Looking at this figure it can be seen that at either observer location the use of ramp has negligible effect on the measured sound levels throughout the frequency range. All measurements for unrounded and rounded forward steps were made with this fairing installed.

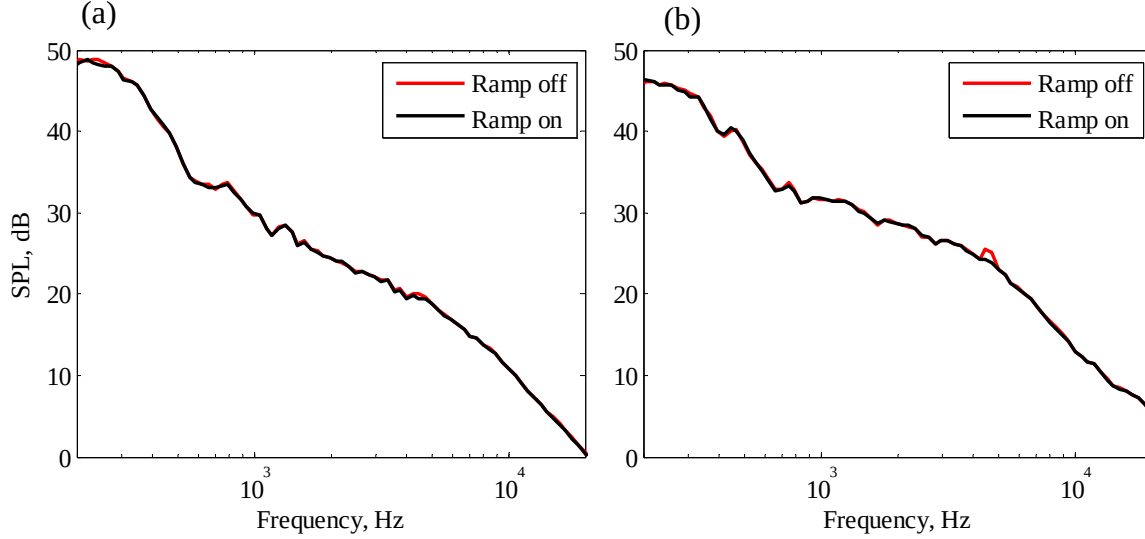


Figure 2-25. Effect of trailing edge ramp used in forward facing step setup, $r/h = 0\%$, $U_m = 21.3$ m/s. (a) $\theta = 123.5^\circ$, (b) $\theta = 51.5^\circ$.

2.2.4 Far Field Sound from Rounded Forward Steps

2.2.4.1 The Unrounded Forward Step (Reference Case)

Figure 2-26 shows the subtracted far field sound spectrum for the forward facing step with no rounding ($r/h = 0\%$) measured at three different velocities at the most upstream observer location ($\theta = 123.5^\circ$). It can be seen that the sound is broadband and has the same qualitative form for all three velocities. Each spectra exhibit two broad regions of different slopes: a mid-frequency region with a slope of -1 and a higher frequency region with steeper slope of approximately -3.2.

We can scale the spectra shown in Figure 2-26 based on the velocity to the 5th power scaling proposed by Cole (1980) which is based on the dimensional analysis of Curle's (1955) equation for radiated far field sound in presence of solid boundaries. This scaling for the spectral levels is given by

$$\Phi_{pps} = \frac{\Phi_{pp}(f)c^2}{\rho^2 U^5 L} \quad (2-18)$$

where Φ_{pps} is now the scaled spectral level, Φ_{pp} is the measured far field sound spectrum, c is the sound speed, ρ is the fluid density, U is some characteristic velocity and L is some characteristic length scale. The frequency scaling f_s is given by

$$f_s = \frac{fL}{U} \quad (2-19)$$

Note that in this scaling the spectral levels are scaled on the 5th power of velocity but the mean square pressure which would be the integral of the spectral density curve scales on the 6th power of velocity which is the scaling for dipole sound sources in Curle's (1955) equation. To scale the spectra shown in Figure 2-26 we choose the length scale L to be the step height and characteristic velocity scale U to be the undisturbed edge velocity at the step location, U_m i.e.

$$\Phi_{pps} = \frac{\Phi_{pp}(f)c^2}{\rho^2 U_m^5 h} \quad (2-20)$$

$$f_s = \frac{fh}{U_m} \quad (2-21)$$

The spectra of Figure 2-26 scaled in this manner are shown in Figure 2-27. It can be seen that this scaling collapses the spectra to within 2 dB for non-dimensional frequencies greater than 1. Below this frequency there is slightly more scatter of the scaled spectra (approximately 4 dB) with the lowest velocity case showing more scalloping than the two higher velocities. A similar observation was also made by Catlett *et al.* (2014) when they used the same scaling as given in equations (2-20) and (2-21) to scale sound from a particular step height for three different velocities. Catlett *et al.* (2014) also measured sound from several different step heights and proposed a mixed scaling which collapsed their far field sound spectra for different step heights quite well. In their mixed scaling the spectral scaling is same as equation (2-20) but the frequency axis is scaled on the undisturbed boundary layer thickness at the step location i.e.

$$f_s = \frac{f\delta}{U_m} \quad (2-22)$$

This mixed scaling applied to the present measurements is shown in Figure 2-28. This scaling does not offer any improvement over the step height scaling of Figure 2-27. This is because the relative change between the boundary layer thicknesses for the three velocities is small.

Figure 2-29 (a)-(d) shows the background subtracted far field sound spectra measured at four additional observer positions downstream of $\theta = 123.5^\circ$ for which results were presented in Figure 2-26. At $\theta = 110.5^\circ$ (Figure 2-29 (a)) we see a spectrum similar to that at $\theta = 123.5^\circ$ in that it is broadband and increases consistently with increasing velocity. However, the mid and high frequency decay slopes are slightly shallower than those seen further upstream. Furthermore, at the highest frequencies (above approximately 12 kHz) the spectrum begins to form a plateau for the two higher velocities and for the lowest velocity there is no audible sound. This trend continues further downstream where the sound spectra directly above the step ($\theta = 90^\circ$, Figure 2-29 (b)) develops a hump like structure with a much flatter shape in the mid-frequency region and a dip at the highest frequencies. The dip appears to be due to the destructive interference caused by the acoustic reflection of the sound the surface upstream of the step. This becomes evident if one considers the frequency at which the half wavelength required for destructive interference occurs. For a given speed of sound c and the step height h this frequency is given by

$$f_{destruct} = \frac{c}{2h} \quad (2-23)$$

For the step height used in present work this frequency is about 13.5 kHz which is approximately where the spectra of Figure 2-29 (b) shows a dip. Catlett *et al.* (2014) also observed similar spectral shapes and directivity which they also attributed to non-compactness of the step geometry. In general, non-compactness refers to the characteristic length scale of the source region being comparable or greater than the wavelength of radiated sound field.

The sound spectra begin to revert back to a form similar to that seen upstream as the observer moves downstream of the step to $\theta = 74^\circ$ (Figure 2-29 (c)) with the effect of non-compactness and destructive interference still visible but less intense. At the farthest downstream observer location ($\theta = 51.5^\circ$) shown in Figure 2-29 (d) the sound spectra have nearly the same shape (similar power law decay) as

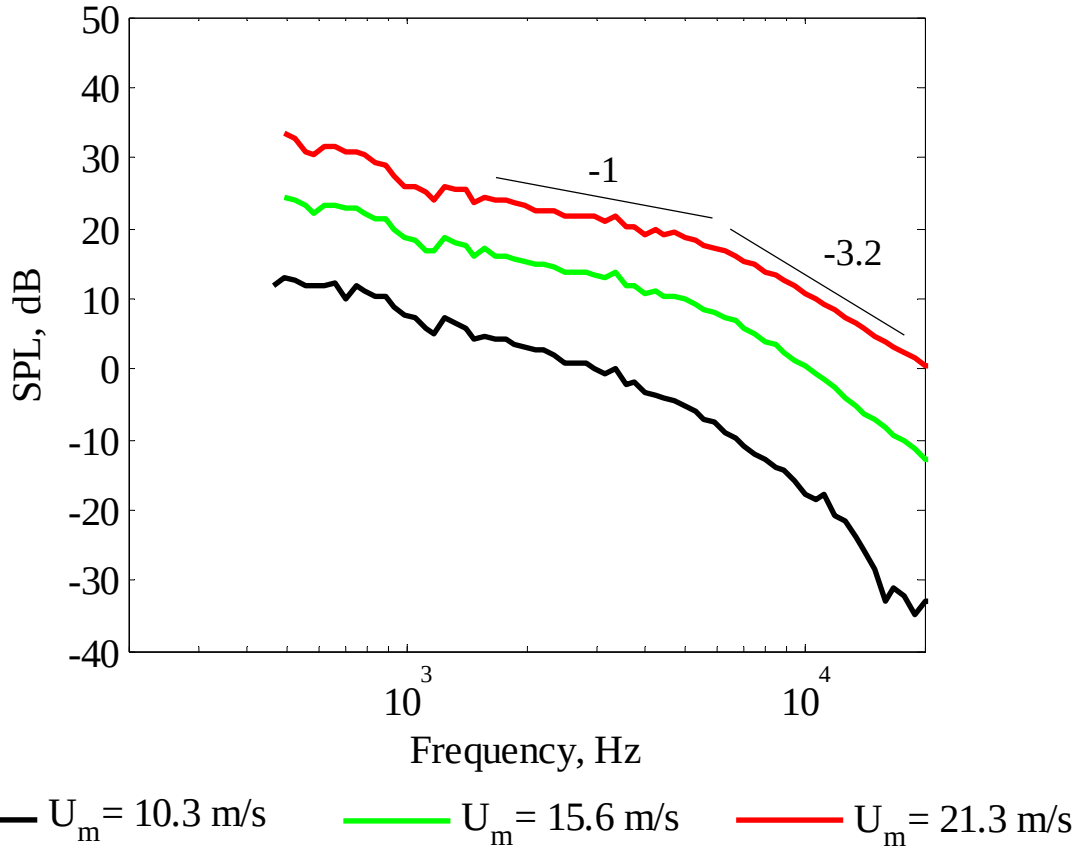


Figure 2-26 Far field sound from an unrounded forward facing step ($r/h = 0\%$) at three different edge velocities at $\theta = 123.5^\circ$.

those found at the most upstream location (Figure 2-26). One notable difference is that the spectra here have a lower SNR at highest frequencies (evident by the absence of sound for the two lower velocity cases) compared to the spectra measured by the most upstream microphone.

The spectra of Figure 2-29 (a)-(d) can also be scaled according to the scaling based on step height and undisturbed edge velocity at the step location (equations (2-20) and (2-21)). The scaled spectra are shown in Figure 2-30 (a)-(d). At $\theta = 110.5^\circ$ (Figure 2-30 (a)) the spectra for the higher velocities show a good collapse up to non-dimensional frequency of approximately 4. Above this frequency the spectra do not collapse as well presumably because of non-compactness effects. The spectrum for $U_m = 10.3$ m/s sits two to 3 dB below those measured at the higher speeds, particularly at low frequencies. Moving further downstream to directly above the step ($\theta = 90^\circ$, Figure 2-30 (b)) we see that, while the lower frequencies $fh/U_m < 4$ still show good collapse for the two higher velocities, the effects of non-compactness become more severe at higher frequencies. Downstream of the step, at $\theta = 74^\circ$ and 51.5° (Figure 2-30 (c) and Figure 2-30 (d)) respectively, we see that the normalized spectra again start to come together for all three velocities. At the last downstream observer location the scaling based on step height shows good collapse in much the same fashion as the most upstream observer location. The results shown in Figure 2-30 suggest that the scaling based on step height and 5th power of the undisturbed edge velocity at the step location works quite well when observer is some distance upstream and downstream of the step. Directly above the step this scaling only seems to work at lower frequencies because the step becoming acoustically non-compact at higher frequencies where the wavelengths are smaller. Lastly, the scaling provides better collapse for the two higher velocity cases ($U_m = 15.6$ and 21.3 m/s) while the lowest velocity ($U_m = 10.3$ m/s) shows some scatter (~ 4 dB), perhaps due to poor SNR.

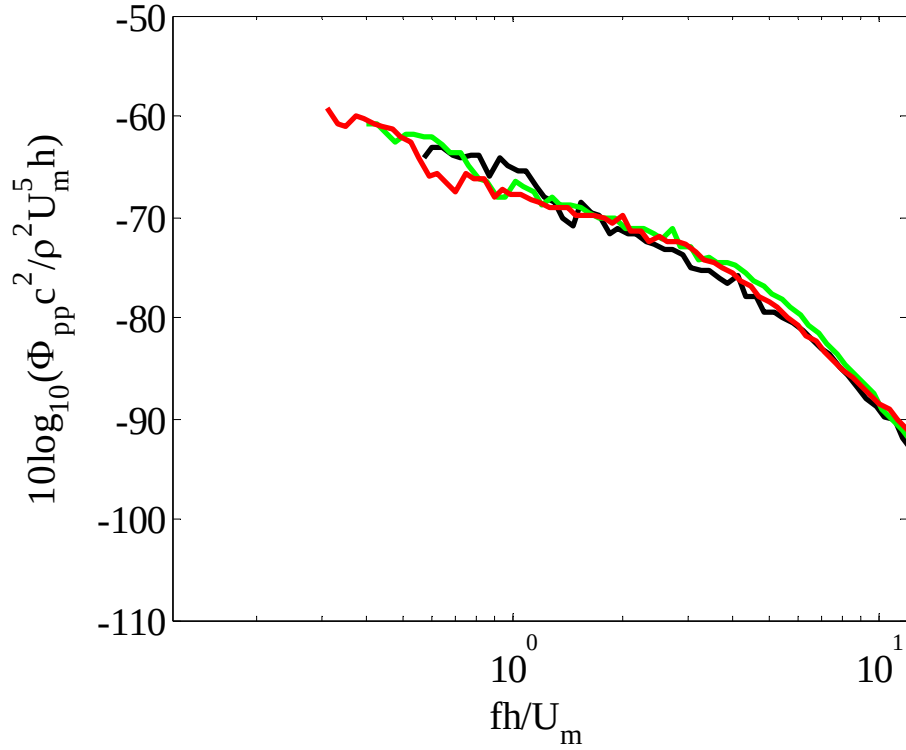


Figure 2-27 Velocity scaling of far field sound from a forward facing step using undisturbed edge velocity and step height, $r/h = 0\%$, $\theta = 123.5^\circ$. Legend is same as that for Figure 2-26.

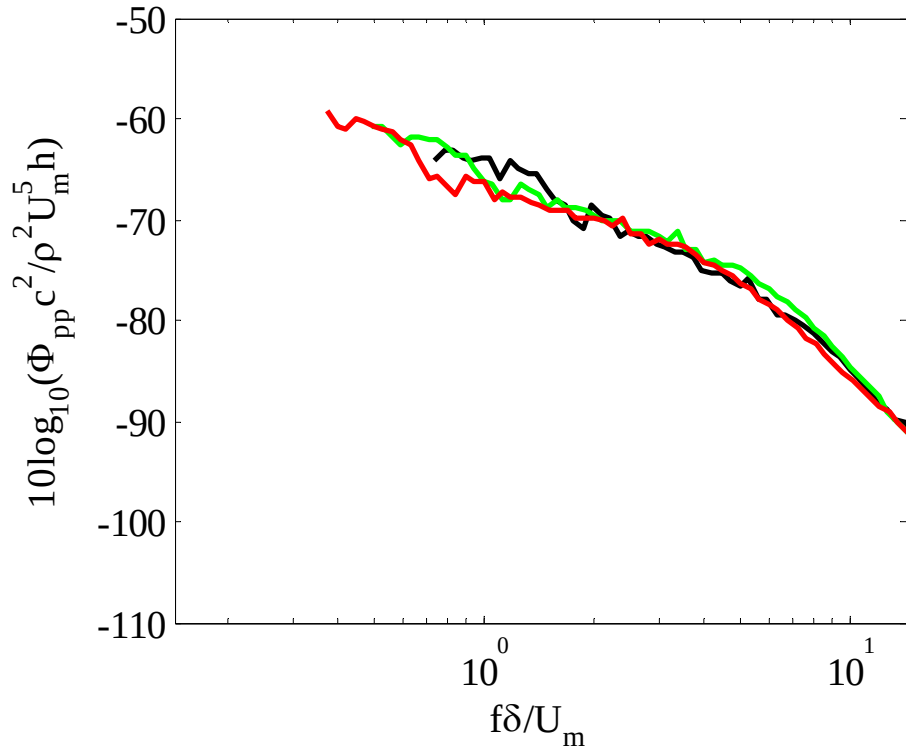


Figure 2-28 Velocity scaling of far field sound from a forward facing step using mixed scaling based on undisturbed boundary layer thickness proposed by Catlett *et al.* (2014), $r/h = 0\%$, $\theta = 123.5^\circ$. Legend is same as that for Figure 2-26.

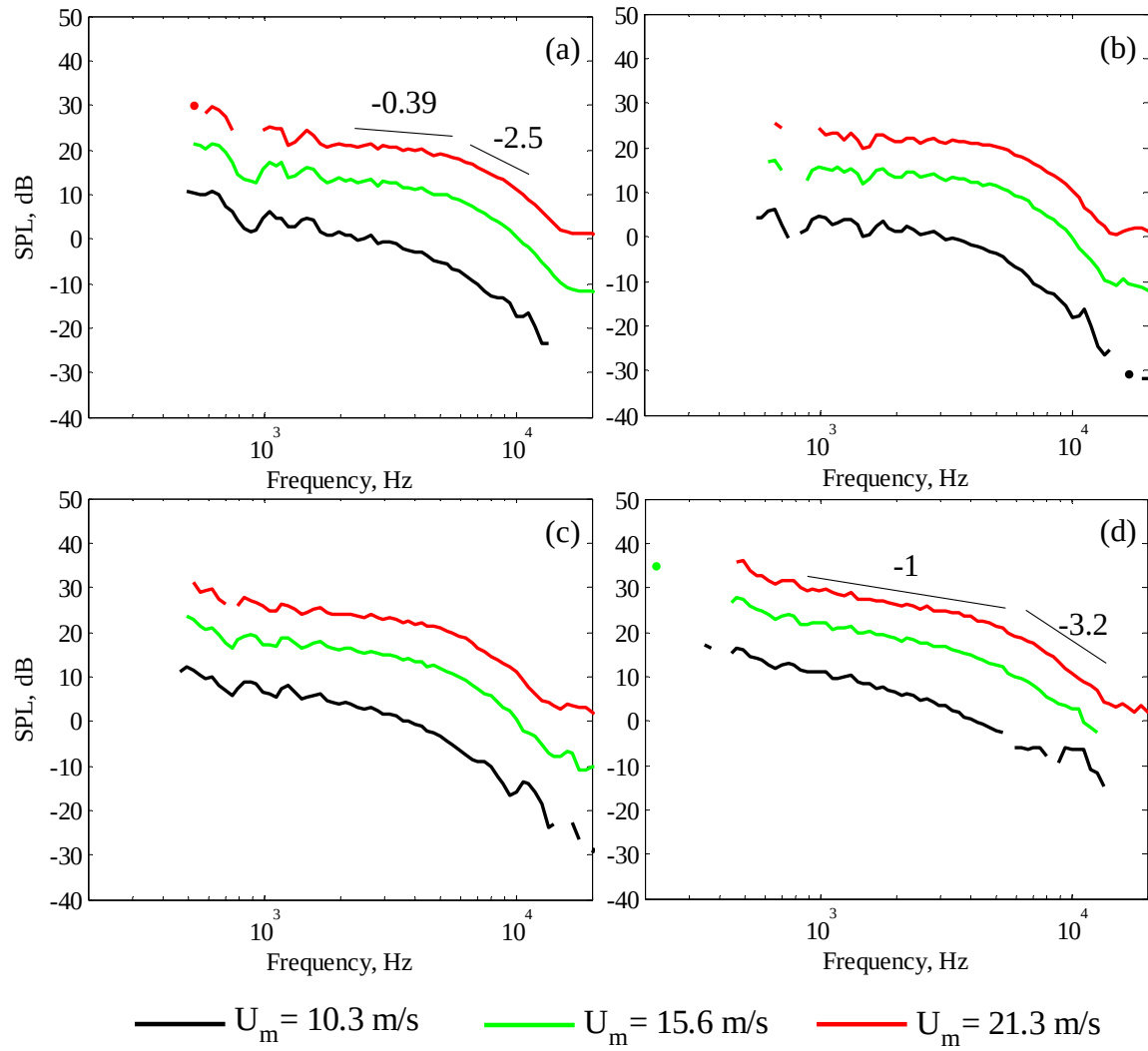


Figure 2-29 Far field sound spectrum for $r/h = 0\%$ forward step at four separate observer positions and three different velocities. (a) $\theta = 110.5^\circ$, (b) $\theta = 90^\circ$, (c) $\theta = 74^\circ$ and (d) $\theta = 51.5^\circ$.

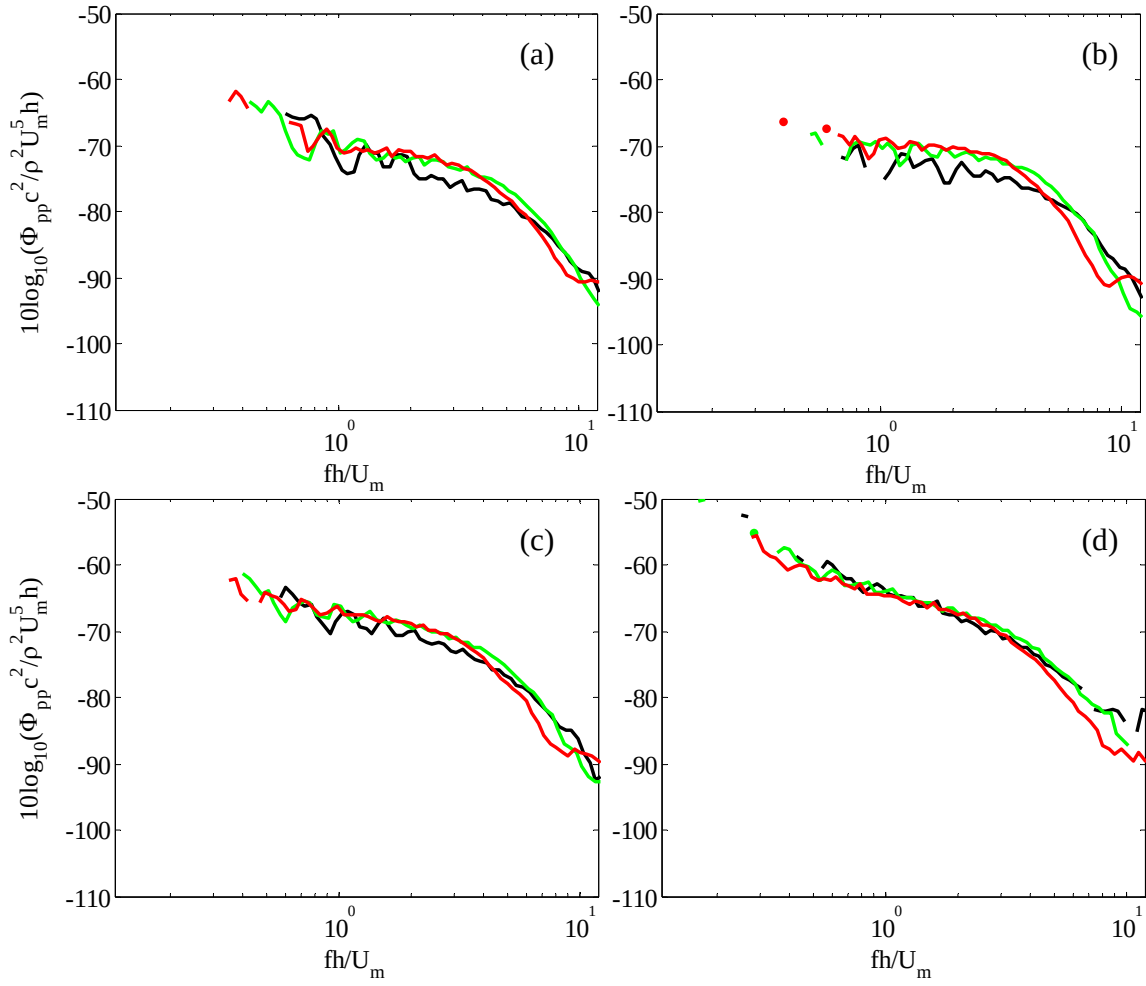


Figure 2-30 Spectra of Figure 2-29 scaled on 5th power of edge velocity and step height using equations (2-20) and (2-21). (a) $\theta = 110.5^\circ$, (b) $\theta = 90^\circ$, (c) $\theta = 74^\circ$ and (d) $\theta = 51.5^\circ$.

Figure 2-31 (a)-(d) shows the spectra of Figure 2-29 scaled on the mixed scaling (frequency scaled on undisturbed boundary layer thickness instead of step height) proposed by Catlett *et al.* (2014). As with the scaling for the most upstream observer ($\theta = 123.5^\circ$, Figure 2-27 (b)) this scaling does not offer any significant improvement over the scaling with frequency scaled on the step height due to the weak variation in boundary layer thicknesses with velocity.

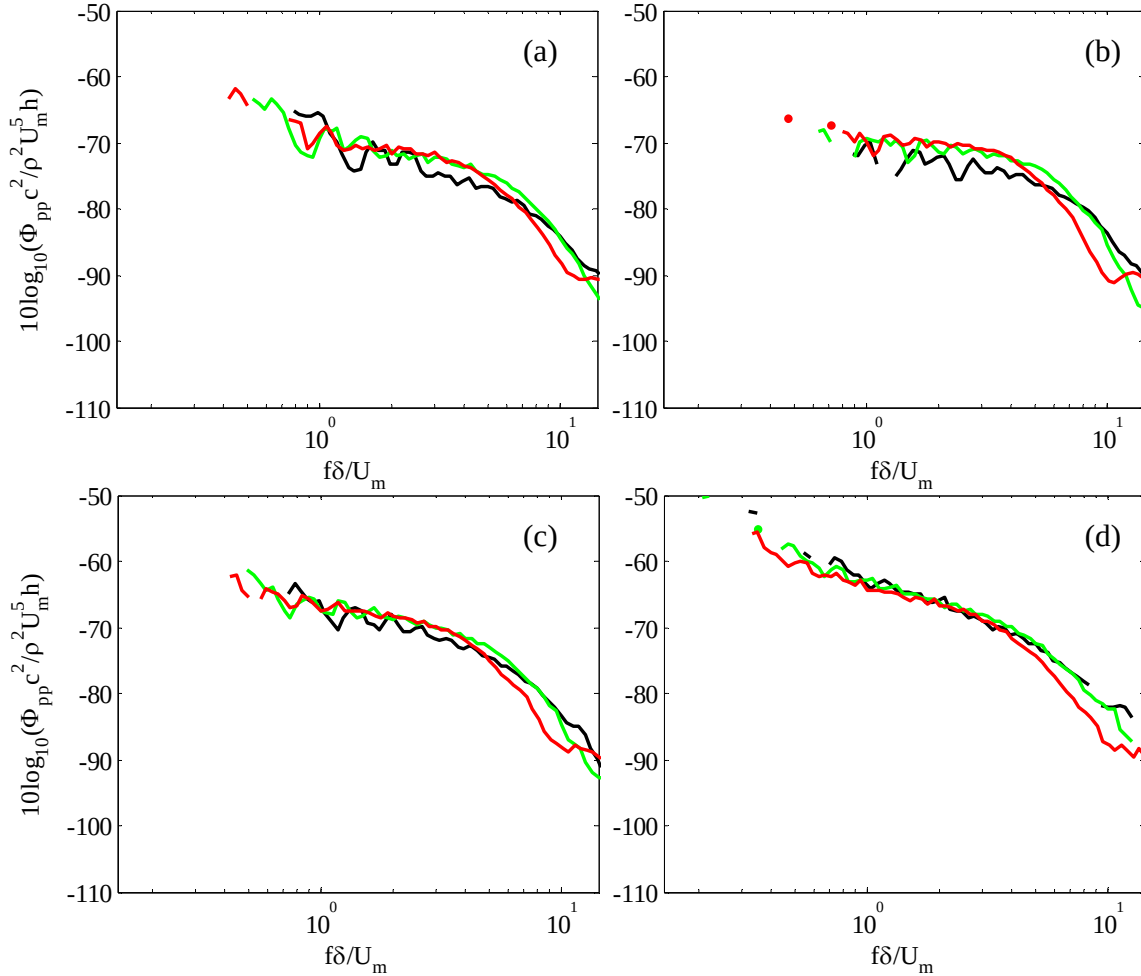


Figure 2-31 Spectra of Figure 2-29 scaled on mixed scaling given by Catlett *et al.* (2014). (a) $\theta = 110.5^\circ$, (b) $\theta = 90^\circ$, (c) $\theta = 74^\circ$ and (d) $\theta = 51.5^\circ$.

2.2.4.2 Comparison with Catlett *et al.* (2014)

Catlett *et al.* (2014) measured the far field sound from a series of forward facing steps (with no corner rounding) with different step heights in the same facility as used in the present work. The location of the leading edge of their step relative to the nozzle exit was the same as in present work (1372 mm). One of the step heights used in their work, $h = 11.7$ mm, is close to the step height $h = 12.7$ mm used in present work. They measured far field sound using four microphones whose position (both radial and angular) were the same as four of the microphones in the measurement arc used in the present work. Thus a direct comparison can be made between the present results and those of Catlett *et al.* (2014).

Figure 2-32 compares measurements at $\theta = 123.5^\circ$. The present spectra for all three velocities have the same shape as those of Catlett *et al.*, but are consistently higher by approximately 2-3 dB throughout the frequency range. The reason for this discrepancy is the changes to the wall-jet facility made after Catlett

et al.'s (2014) measurements. As mentioned in §2.1.1, a gap was created between the wall-jet nozzle exit and the horizontal acoustic baffle used to shield far field microphones increasing the width of the two-dimensional core of the flow from 600 mm to 800 mm approximately. These changes also had small effect on the undisturbed edge velocity and the undisturbed boundary layer thickness at the step location. Note also that the step height for the present work was slightly different from that of Catlett *et al.* (2014). These differences in present and past measurement conditions are summarized in Table 2-6.

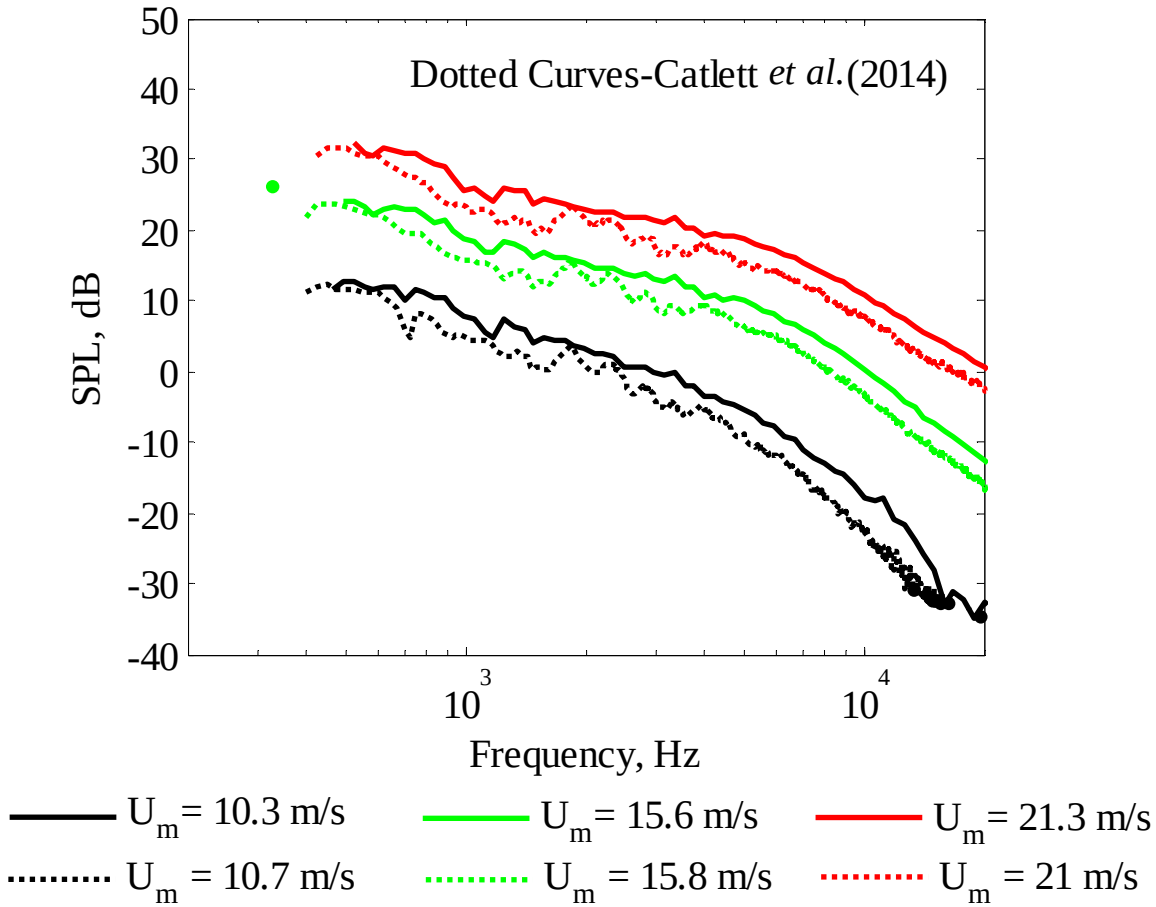


Figure 2-32 Comparison of present far field sound measurements with Catlett *et al.* (2014), $r/h = 0\%$, $\theta = 123.5^\circ$. Measurements of Catlett *et al.* (2014) shown as dotted curves.

Table 2-6 Comparison of flow conditions between present work and measurements of Catlett *et al.* (2014).

h (mm) Catlett et al. (2014)	h (mm) (Present)	U_m (m/s) Catlett et al. (2014)	U_m (m/s) (Present)	δ (mm) Catlett et al. (2014)	δ (mm) (Present)
11.7	12.7	10.7	10.3	20.6	16.3
11.7	12.7	15.8	15.6	18.8	15.7
11.7	12.7	21	21.3	17.7	15.2

The differences shown in Table 2-6 and the difference in the sound producing span between the present and past work can be quantitatively accounted for to some extent using a spanwise source distribution model. We assume a spanwise point source located at the forward step face location as shown in Figure 2-33. The distance of this point source from the spanwise center of the wall-jet plate is z and the distance from the spanwise center to the observer (microphone center) is R . Note that the distance R is same for both present and past work since the microphone in both cases were installed in the spanwise center plane (see Figure 2-16) and were at the same radial distance and angle relative to the step. The square of distance from this point source to the observer is then given by

$$r^2 = R^2 + z^2 \quad (2-24)$$

Let us now assume that this point source has a strength Q per frequency then the acoustic intensity induced by this point source per frequency at the observer location shown in Figure 2-33 would be given by

$$dI = \frac{dQ}{r^2} = \frac{dQ}{R^2 + z^2} \quad (2-25)$$

The total acoustic intensity induced by a distribution of these point sources along the step span would then be given by an integral which runs along the entire effective span i.e.

$$I = \int_{-W/2}^{W/2} \frac{dQ}{R^2 + z^2} dz \quad (2-26)$$

where W is the length of the effective span of the step i.e. the width of the two-dimensional core of the wall-jet flow at the step location. The integral in equation (2-26) can be performed if the acoustic power of the source is known. For two different flows this integral can be performed to yield the relative intensities which is what is needed to compare the present measurements to Catlett *et al.*'s (2014). Following Catlett *et al.* (2014), we assume a mixed scaling in which the power radiated by this point source per frequency scales on the 5th power of edge velocity and the step height, and that the frequency scales on the boundary layer thickness and edge velocity. So, we choose the source power dQ to be of the form

$$dQ = U_m^5 h \quad (2-27)$$

Then, the integral in equation (2-26) becomes

$$I = \int_0^W \frac{U_m^5 h}{(R^2 + z^2)} dz = \frac{U_m^5 h}{R} \tan^{-1} \left(\frac{W}{R} \right) \quad (2-28)$$

Equation (2-28) can now be used to compare the relative acoustic intensities between the present and past measurements using appropriate step height, edge velocity, width of two-dimensional core. Integrating equation (2-28) with parameters shown in Table 2-6 for the present and past work we can obtain a ratio of the acoustic power induced due to differences in the conditions between the two measurements. This ratio can then be applied to the spectrum to account for the differences in the spectral levels. If we denote the acoustic induced intensities by present and past work as I_p and I_c respectively, the spectral scaling factor is given by

$$F_{\Phi_{pp}} = \frac{I_p}{I_c} \quad (2-29)$$

Similarly, for the frequency axis using the mixed scaling on boundary layer thickness and edge velocity, we can obtain a scale factor for frequency axis as

$$F_f = \frac{\left(\frac{U_m}{\delta}\right)_P}{\left(\frac{U_m}{\delta}\right)_C} \quad (2-30)$$

where the subscript P and C refer to the present and past flow conditions respectively. Note that the way these scale factors in equations (2-29) and (2-30) are defined the present spectral levels and frequency values must be divided by these factors. Table 2-7 below shows the scale factors to be applied to present far field sound spectral levels (in dB) and frequencies for the three edge velocities.

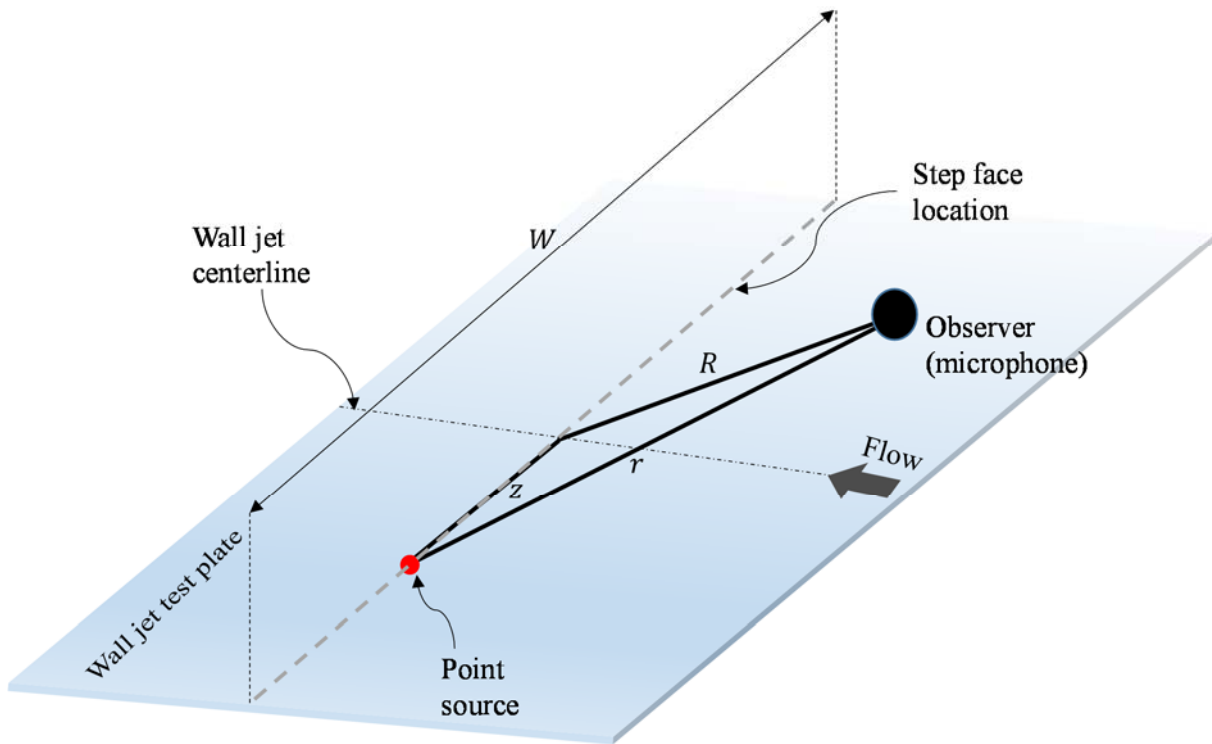


Figure 2-33 Spanwise point source distribution model to account for differences between present measurements and those by Catlett *et al.* (2014).

Table 2-7 Scaling factors to be applied to present far field sound spectra.

U_m	$F_{\Phi_{pp}}(\text{dB})$	F_f
10.3	0.26	1.21
15.6	0.81	1.18
21.3	1.39	1.18

Figure 2-34 shows the comparison between the present and past far field sound spectra measured at $\theta = 123.5^\circ$ after the application of scaling factors of Table 2-7 to the present data. Looking at the figure it can be seen that once the differences in the test conditions are approximately accounted for the past and

the present measurements show good agreement. The slight discrepancy at the lowest frequencies can be attributed to relatively higher uncertainty in both the present and past spectra.

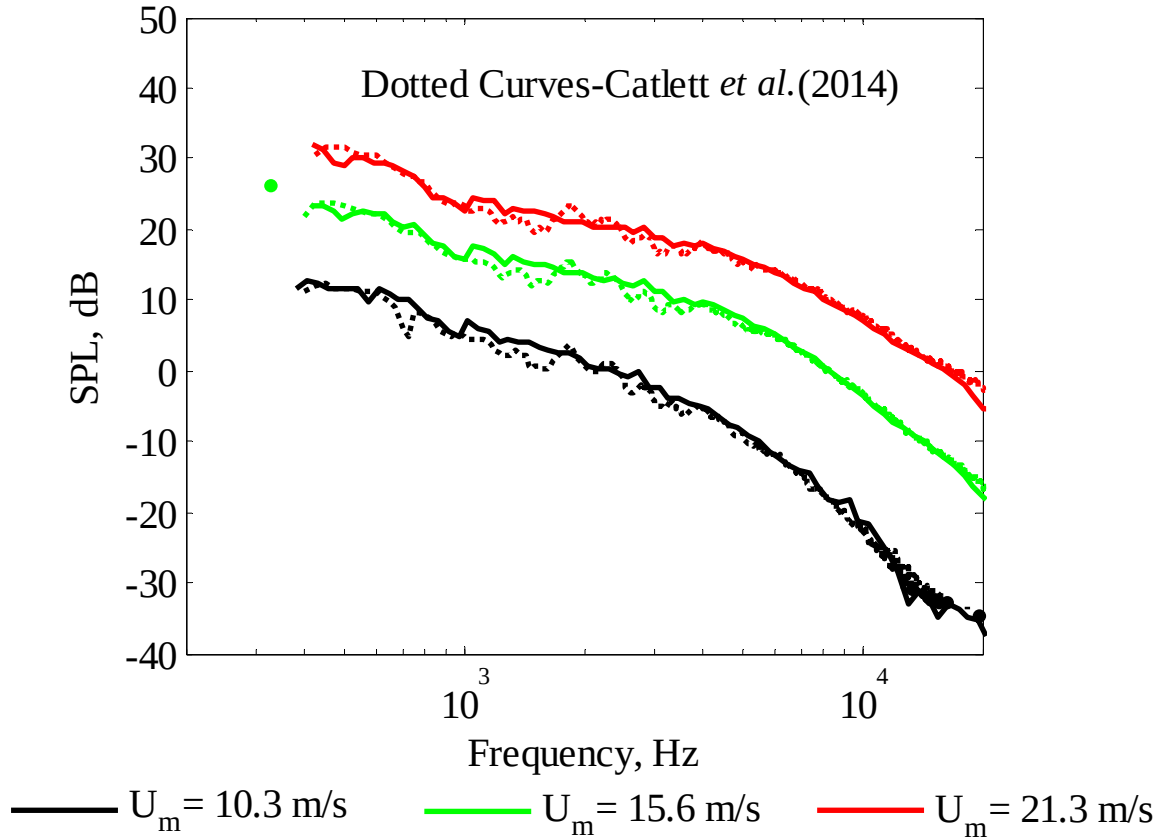
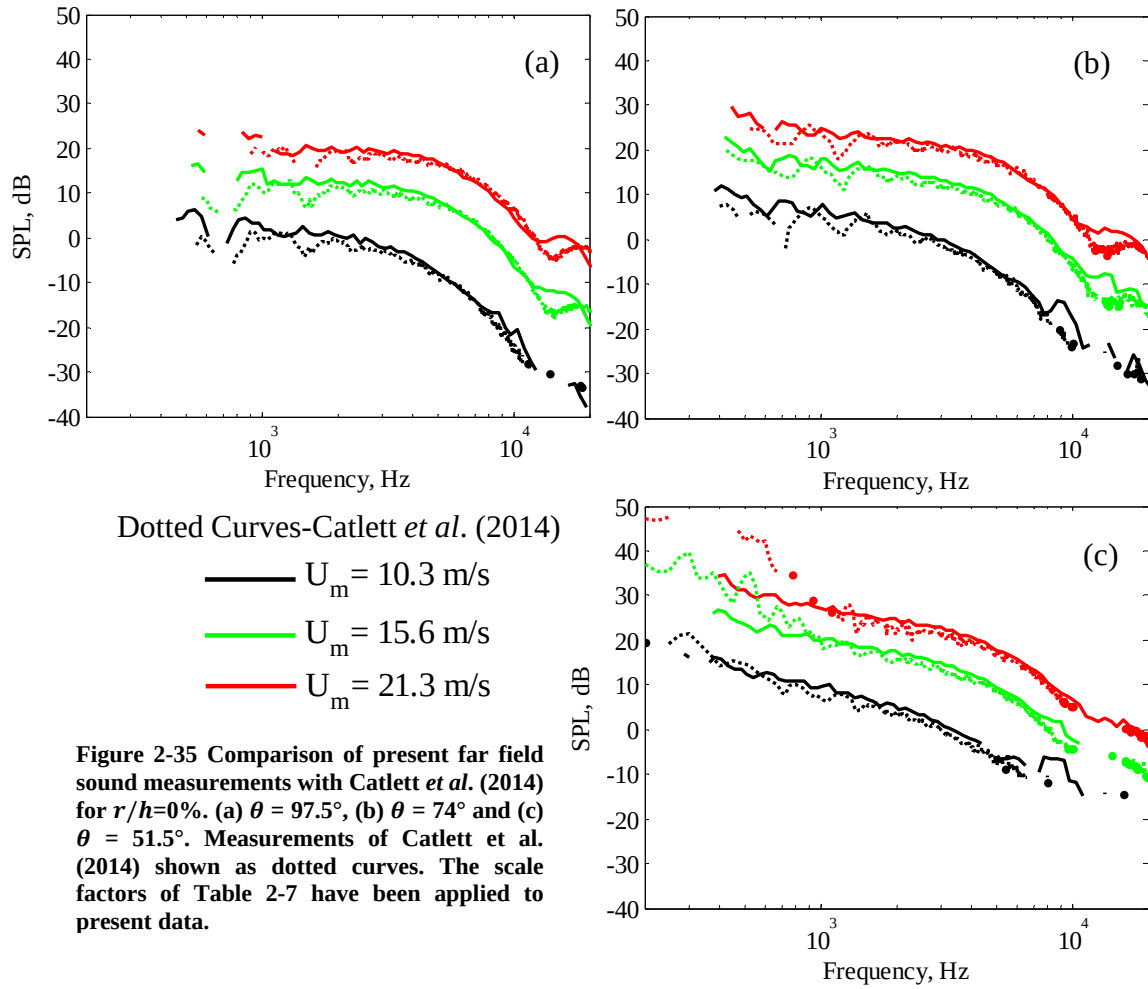


Figure 2-34 Comparison of present far field sound measurements with Catlett *et al.* (2014) after applying the scale factors of Table 2-7, $r/h = 0\%$, $\theta = 123.5^\circ$. Measurements of Catlett *et al.* (2014) shown as dotted curves. Note that the scaling factors of Table 2-7 are applied to the present data.

Figure 2-35 (a)-(c) shows the comparison between present and past measurements for three additional observer locations after application of scale factors (Table 2-7) to account for the differences in measurement conditions. Looking at Figure 2-35 (a) for $\theta = 97.5^\circ$ we see that the curves for all three velocities show good general agreement at low and mid frequencies accounting for uncertainties. However, at high frequencies the present spectral levels are higher by about 4-5 dB compared to the past measurements. Additionally the dip in the spectra which is the result of destructive interference occurs at a lower frequency for the present spectra. The reason for this is that the step height in present work is slightly larger (1 mm) which results in a lower value of frequency corresponding to destructive interference. This difference in destructive interference frequencies, assuming the same speed of sound in both cases is approximately 1 kHz which is about the same amount by which the dip in present spectra is shifted to the left on the frequency axis in Figure 2-35 (a). The comparison of the present scaled spectra with Catlett *et al.*'s (2014) measurements for the observer at $\theta = 74^\circ$ and 51.5° as shown in Figure 2-35 (b) and (c) respectively also shows good agreement through a broad frequency range. Any disagreement between the present and past measurements is limited to frequencies which are either dominated by the high uncertainty in the measurements or by the acoustic non-compactness of the step.



2.2.4.3 Directivity Pattern of Radiated Sound from the Unrounded Forward Facing Step

To consider the directivity of the sound from unrounded forward step we can plot the spectra from all eight observer location on the same plot. We will only consider the directivities at $U_m = 21.3$ m/s since those for lower velocities are qualitatively the same. Figure 2-36 shows the spectra (in SPL) for each observer location at $U_m = 21.3$ m/s. To compare these directivities to a cosine squared directivity associated with a streamwise aligned the most downstream observer location ($\theta = 51.5^\circ$) was picked as a reference and then the difference in sound levels (in dB) between this observer and the rest of the measurement arc was calculated assuming a cosine squared directivity for a source with unit strength. This difference in level designated as Δ_{dB} which is shown in the legend of the figure for each observer is given by

$$\Delta_{dB} = 20 \log_{10}[\cos(\theta)] - 20 \log_{10}[\cos(51.5^\circ)] \quad (2-31)$$

For the observer locations in the present work this value is always negative and shows the depreciation in sound levels for each observer compared to that at $\theta = 51.5^\circ$ which would be expected if the directivity pattern for step noise was a streamwise aligned dipole. For quantitative comparison between measured and cosine squared directivity two curves which represent the reference spectrum less 7 dB and 15 dB have also been shown in Figure 2-36.

Consider now the spectra in Figure 2-36. We first note that for a dipole there would be no sound directly above the step ($\theta = 90^\circ$) but the measurements show presence of sound across the frequency range. At lower frequencies each spectra except that at $\theta = 123.5^\circ$ shows reduction in sound in a manner similar to that expected for a dipole but the amount by which the sound is reduced in most cases is less than that expected from an ideal dipole. For the most upstream observer ($\theta = 123.5^\circ$) the actual sound reduction at lower frequencies compared to the most downstream observer ($\theta = 51.5^\circ$) exceeds that expected from an ideal dipole. As the frequency increases the directivity pattern shows clear divergence from a dipole type behavior in that the spectra at each observer location begin to show similar levels. For frequencies between 7 kHz – 10 kHz the spectra are within about 2 dB of each other. The acoustic wavelength to step height ratio (λ/h) at 7 kHz is approximately 3.8 and the directivity pattern is likely affected by the acoustic non-compactness of the step at these high frequencies. At frequencies beyond 10 kHz each spectra except for the most upstream observer exhibits a spectral dip associated with the destructive interference caused by the reflection from the upstream surface with the strength of this dip becoming stronger as one moves closer to the step. These results suggest that the reflections from the upstream surface of the step have a greater influence on the observer located downstream of the step. Overall, the directivity pattern shown in Figure 2-36 suggests that while at lower frequencies (where λ/h is large) the step might behave like a compact source with dipole directivity, at higher frequencies (where λ/h is small) this is not the case and the directivity pattern is influenced greatly by the acoustic non-compactness of the step geometry.

The directivity patterns shown in Figure 2-36 highlight an important issue raised earlier regarding the treatment of forward facing step as an acoustically compact geometry. In general the forward facing step is assumed to be acoustically compact in limit of zero Mach number (e.g. Howe (1989) and Ji and Wang (2010)). While this assumption may be valid for hydrodynamic flows (where the speed of sound is large), Figure 2-36 shows that caution must be exercised when applying this assumption to aeroacoustics of forward facing steps. Note that the Mach numbers for the measurements shown here is 0.06.

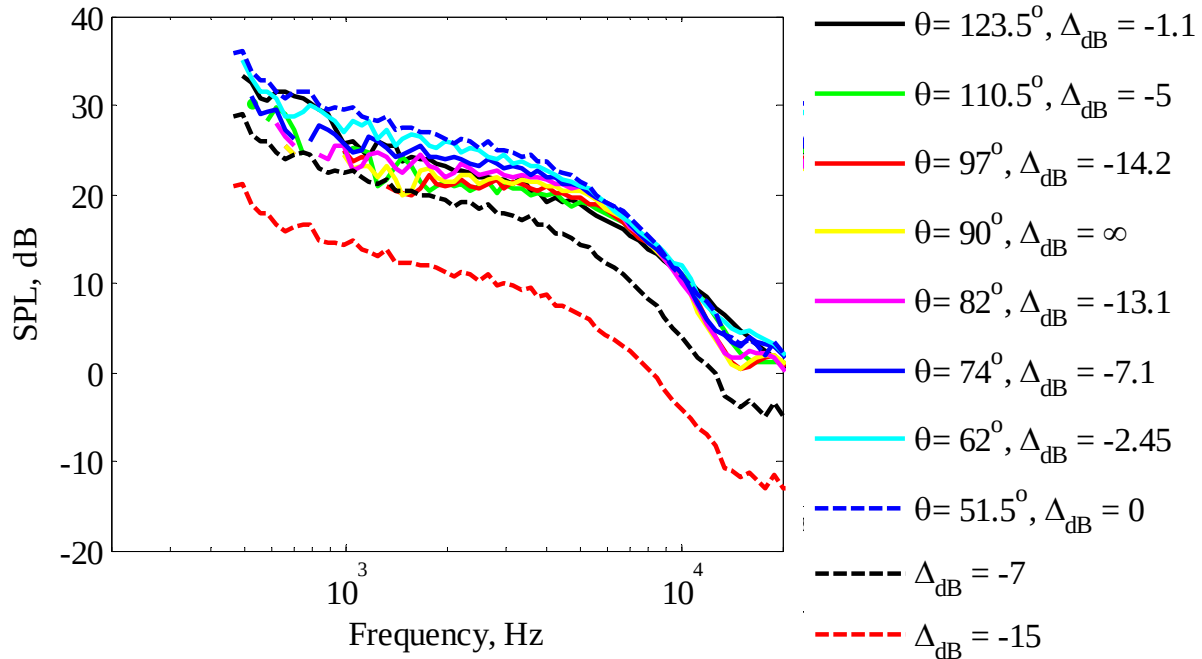


Figure 2-36 Directivity pattern for $r/h = 0\%$ forward facing step at $U_m = 21.3$ m/s. Legend shows the difference in dB at each observer location compared to the observer at $\theta = 51.5^\circ$ assuming a streamwise aligned dipole directivity of far field sound

2.2.4.4 Effects of Corner Rounding

We will now consider the effect of rounding the external corner of the forward facing step on the radiated sound. Six different rounding radii to step height ratios of $r/h = 0\%$, 6.25% , 12.5% , 25% , 50% and 100% were considered. Far field sound was measured using the same microphone setup as that for an unrounded forward step and at the same three flow velocities.

Figure 2-37 shows sound spectra measured by the most upstream microphone ($\theta = 123.5^\circ$) for all six rounded steps at $U_m = 21.3$ m/s. Rounding the step corner slightly to $r/h = 6.25\%$ leads to a sound spectrum which is essentially the same as the unrounded case up to a frequency of approximately 8 kHz. Above this frequency spectral levels for $r/h = 6.25\%$ fall to approximately 2 dB below those for $r/h = 0\%$. The peak effect of rounding occurs at 11924 Hz where a reduction of 2.1 dB is seen. It may be that small rounding of step corner does not significantly alter the sound source (i.e. the flow structure) and the decrease in high frequency sound levels might be associated with the reduction in the scattering efficiency of the step corner. In terms of Curle's (1955) theory on sound from presence of solid boundaries in turbulent flow, this means that the effect of a small corner rounding on far field sound would be a result of modification of the Green's function associated with the step geometry.

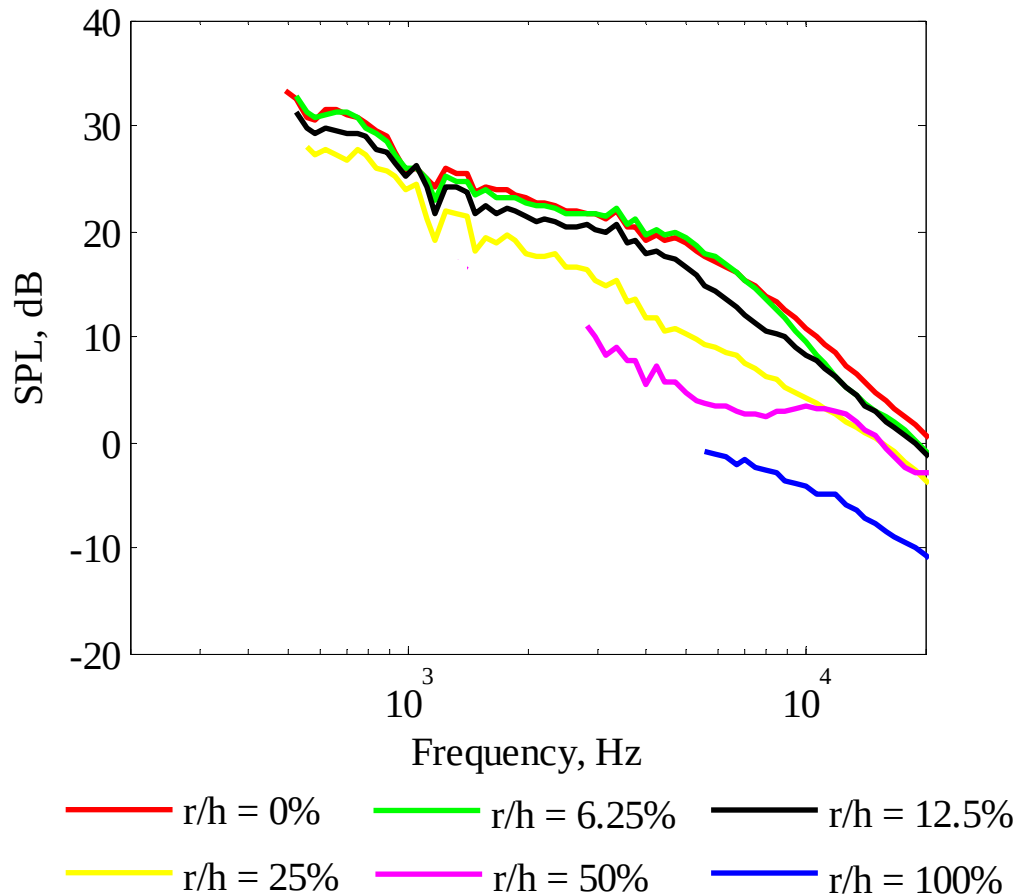


Figure 2-37 Effect of rounding the forward facing step corner on far field sound, $U_m = 21.3$ m/s, $\theta = 123.5^\circ$.

As the step corner is rounded further to 12.5% of the step height we see further reduction in the sound spectrum. This reduction in the sound is more broadband compared to that for $r/h = 6.25\%$. There is a slight reduction (~ 2 dB) in sound compared to $r/h = 0\%$ step up to a frequency of about 5 kHz. Above this frequency the sound spectrum changes its slope much in the same fashion as the unrounded step and

the levels drop further (~ 3 dB compared to the unrounded step). The peak value of reduction of sound is about 3.4 dB which occurs at 7500 Hz. The frequency based on the rounding radius of the corner and the wall-jet convection velocity is 5500 Hz which is very close to the junction where the sound spectrum changes its slope. This suggests that the rounded corner may be influencing the sound behavior at higher frequencies by changing Green's function of the step, as for $r/h = 6.25\%$. However, unlike this case, rounding the step to 12.5% of the step height also leads to reductions in the noise produced at lower frequencies. This is presumably because the step rounding is now changing the flow structure as well. Hao and Wang (2014) in their LES simulations show that for $r/h = 12.5\%$ the separation bubble size and the reattachment length on top of the step are smaller than those for an unrounded step by 43% and 45% respectively. This shortening of the bubble brings about a faster decay of the surface pressure fluctuations downstream of the step. Note that Hao and Wang (2014) did not consider the case of $r/h = 6.25\%$ in their simulations so at present it is difficult to say if the high frequency reduction in sound from that step is solely a Green's function effect.

Rounding the step corner further to $r/h = 25\%$ leads to further attenuation of sound which now affects the entire frequency range severely. The mid frequency sound attenuation is still greater than that found at lower frequencies but the two distinct slopes found in smaller rounding cases have disappeared and the spectra appears to have a constant rate of decay throughout the frequency range. The peak reduction in sound levels is approximately 8.6 dB which occurs at a frequency of 5000 Hz. The convective frequency associated with this rounding is 2750 Hz, about half of the maximum attenuation frequency. The highest two rounding radii ($r/h = 50\%$ and 100%) produced only measureable sound only at the highest frequencies. The spectra for $r/h = 50\%$ step shows a scalloping behavior at high frequencies where the spectral levels begin to rise around 9 kHz forming a peak around 10 kHz and then fall to form a plateau around frequency of 18 kHz. Sound levels measured at 100% are so low that background facility noise (i.e. the nozzle exit noise) scattered by the step might be responsible.

To examine this possibility a measurement was conducted where a white noise source (University Sound ID60C8 speaker) was placed close to the wall-jet nozzle exit with no flow, as shown in Figure 2-38 below. Then, sound from the speaker was recorded by the same far field microphones which were used to measure the flow noise both with no step present and with the $r/h = 100\%$ rounded step configuration. Figure 2-39 (a) and (b) show a comparison between the sound spectra measured with and without $r/h = 100\%$ step by the microphone at $\theta = 123.5^\circ$ for white noise source and flow at $U_m = 21.3$ m/s respectively. It can be seen that in both cases the the sound with the step in place begins to amplify around the same frequency (~ 6 kHz). Note that although not shown the SNR progressively becomes worse for these two steps as one moves downstream of the microphone at $\theta = 123.5^\circ$. This supports our argument that the noise in subtracted spectra for $r/h = 50\%$ and 100% steps is due to the background noise being scattered by the step geometry. In light of the results shown in Figure 2-39 we will exclude $r/h = 50\%$ and 100% steps from subsequent discussion.

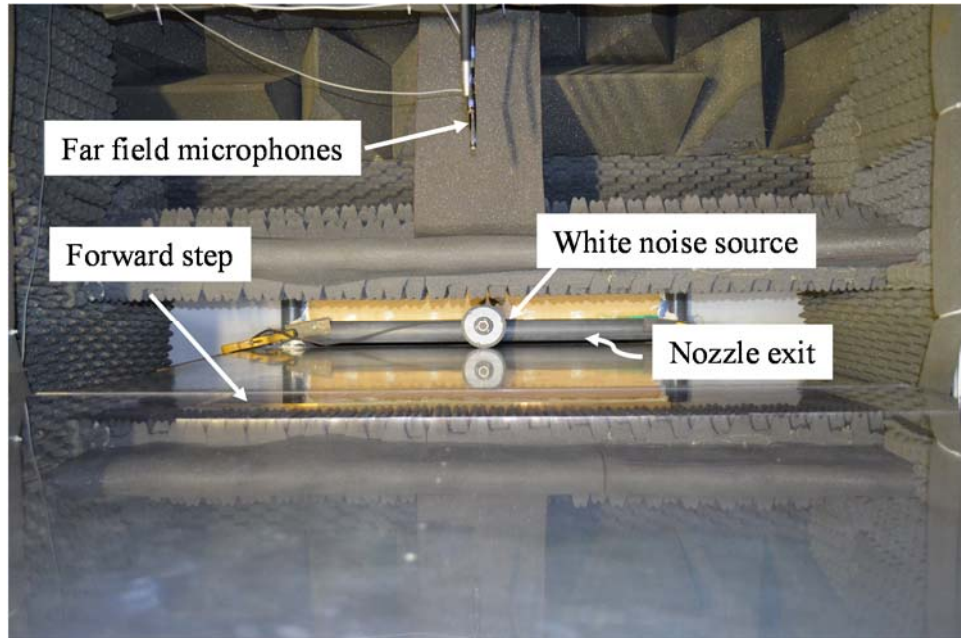


Figure 2-38 Measurement setup to obtain the response of a fully rounded forward step and no-step configuration to a white noise source placed near the nozzle exit.

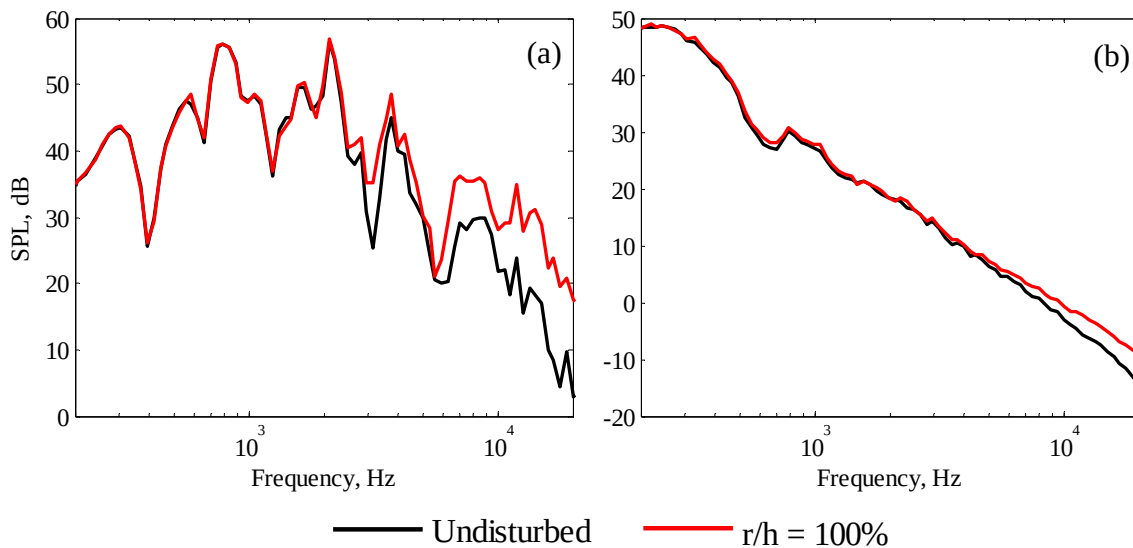


Figure 2-39 Noise amplification by $r/h = 0\%$ forward step, $\theta = 123.5^\circ$. (a) White noise source placed at the nozzle exit and (b) Measured spectra with flow at $U_m = 21.3$ m/s.

Figure 2-40 shows the effect of rounding the step corner on the far field sound spectrum at four observer angles, downstream of the $\theta = 123.5^\circ$ for which the results were shown in Figure 2-37. Note that significant sound (above that due to background scattering) was not recorded at these angles for the $r/h = 50\%$ and 100% steps. At $\theta = 110.5^\circ$ the spectra follow the same trend as at $\theta = 123.5^\circ$ in that a slight rounding of the corner ($r/h = 6.25\%$) leads to a reduction of sound only at highest frequencies and, as the corner is rounded further, the attenuation spreads to the lower frequencies as well. At this lower directivity angle, however, the effects of non-compactness become visible at high frequencies, and the spectra for r/h

= 6.25% and 12.5% appear to experience the effect of non-compactness at highest frequencies much in the same way as seen for $r/h = 0\%$ step. For $r/h = 25\%$ the effect of this non-compactness is not as intense as that for an unrounded step but a slight flattening of the spectra is seen at the highest frequencies.

As we move directly over the step ($\theta = 90^\circ$, Figure 2-40 (b)) the sound levels drop in the same fashion as those seen at upstream observer locations but the spectral shape for each step undergoes a change with the appearance of a mid-frequency hump and high frequency dip associated with non-compactness effects similar to those seen for $r/h=0\%$. The spectral dip associated with destructive interference is present for all cases at this location, however, the location of this dip shifts to lower frequencies with increasing rounding. While this behavior is not clearly visible for $r/h = 6.25\%$, for $r/h = 12.5\%$ the spectra begins to form a plateau around a frequency of 12.6 kHz for $r/h = 12.5\%$, as opposed to about 13.5 kHz for $r/h = 0\%$ and 6.25%. The half acoustic wavelength associated with this frequency is about 13.7 mm which is slightly larger than the step height. This can be explained if one considers the fact that with increasing rounding the contour length of the step face increases due to the added circular geometry, so that sound generated at the end of the step corner has further to travel before being reflected from the upstream wall. For $r/h = 12.5\%$ the total perimeter of the rounded and vertical parts of the step is 13.6 mm. For $r/h = 25\%$ the half wavelength associated with where the high frequency spectral plateau begins (around 11 kHz) is about 15.3 mm and the perimeter of the rounded and vertical part of the step face is approximately 14.5 mm.

Moving further downstream to observation angle of $\theta = 74^\circ$ (Figure 2-40 (c)) the spectra show similar behavior as seen directly over the step except the high frequency dip is now less pronounced and the mid frequency hump structure begins to straighten out for each step. At the farthest downstream location of $\theta = 51.5^\circ$ as shown in Figure 2-40 (d) the spectra begin to revert to their upstream shape with regions of two distinct slopes. The subtracted spectra for $r/h \geq 12.5\%$ have a limited frequency range for which sound is seen because the SNR for these higher rounding at this observer location is low.

We can also apply the mixed scaling of Catlett *et al.* (2014) which as shown earlier (see §2.2.4.1) reliably collapses the data for the unrounded step to rounded steps. This mixed scaling applied to the sound spectra measured at $\theta = 123.5^\circ$ for $r/h \leq 25\%$ is shown in Figure 2-41. Note that the scaling for $r/h = 0\%$ step has been repeated in Figure 2-41 (a) for the sake of comparison. Looking at Figure 2-41 (b) which shows the scaled spectra for $r/h = 6.25\%$ step we see that similar to the unrounded step this scaling provides a reasonable collapse of data for all three velocities at this observer location (except for some scatter for the lowest velocity at low frequencies which was also seen for the unrounded step). The scaling collapses the spectra for $r/h = 12.5\%$ and 25% also as shown in Figure 2-41 (c) and Figure 2-41 (d) respectively. The spectra for $r/h = 25\%$ step and $U_m = 10.3$ m/s shows some scalloping behavior possibly due to poor SNR but the collapse is still within 3dB. Though not shown here this scaling does not collapse the spectra for $r/h = 50\%$ and 100% cases because they are dominated by the scattered facility noise and there is little evidence of fluid dynamic sound source in those cases.

Figure 2-42 shows the mixed scaling applied to the far field sound measured at $\theta = 90^\circ$ for $r/h \leq 25\%$. As seen in this figure, similar to that observed for the unrounded forward step, the mixed scaling does not collapse the data for rounded forward steps at this observer location due to the effect of non-compactness dominating the measured spectra in each case.

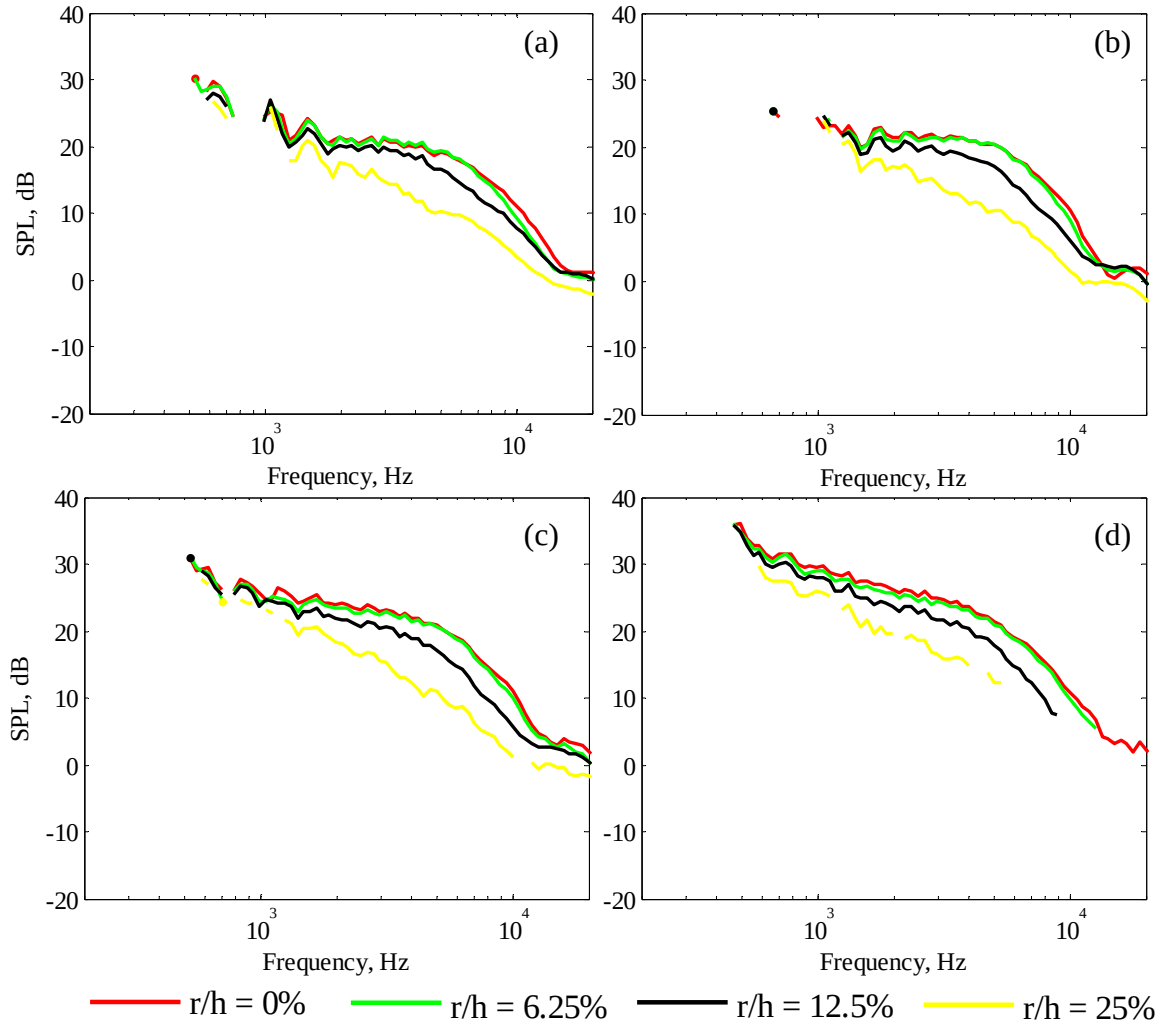


Figure 2-40 Effect of rounding the forward facing step corner on far field sound at four separate observer locations, $U_m = 21.3\text{m/s}$. (a) $\theta = 110.5^\circ$, (b) $\theta = 90^\circ$, (c) $\theta = 74^\circ$ and (d) $\theta = 51.5^\circ$.

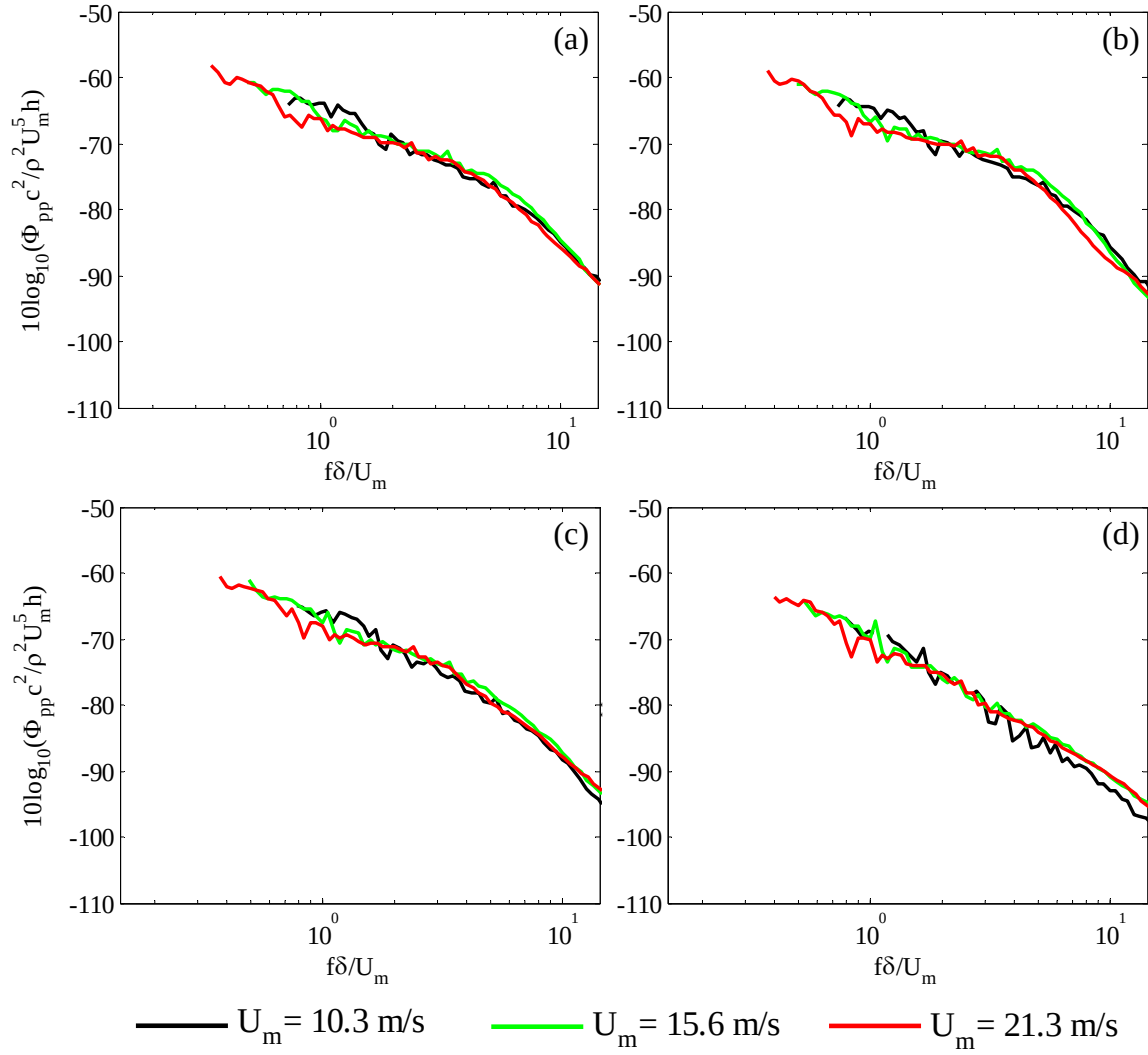


Figure 2-41 Mixed scaling of far field sound spectra for rounded forward facing steps at $\theta = 123.5^\circ$. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$ and (d) $r/h = 25\%$.

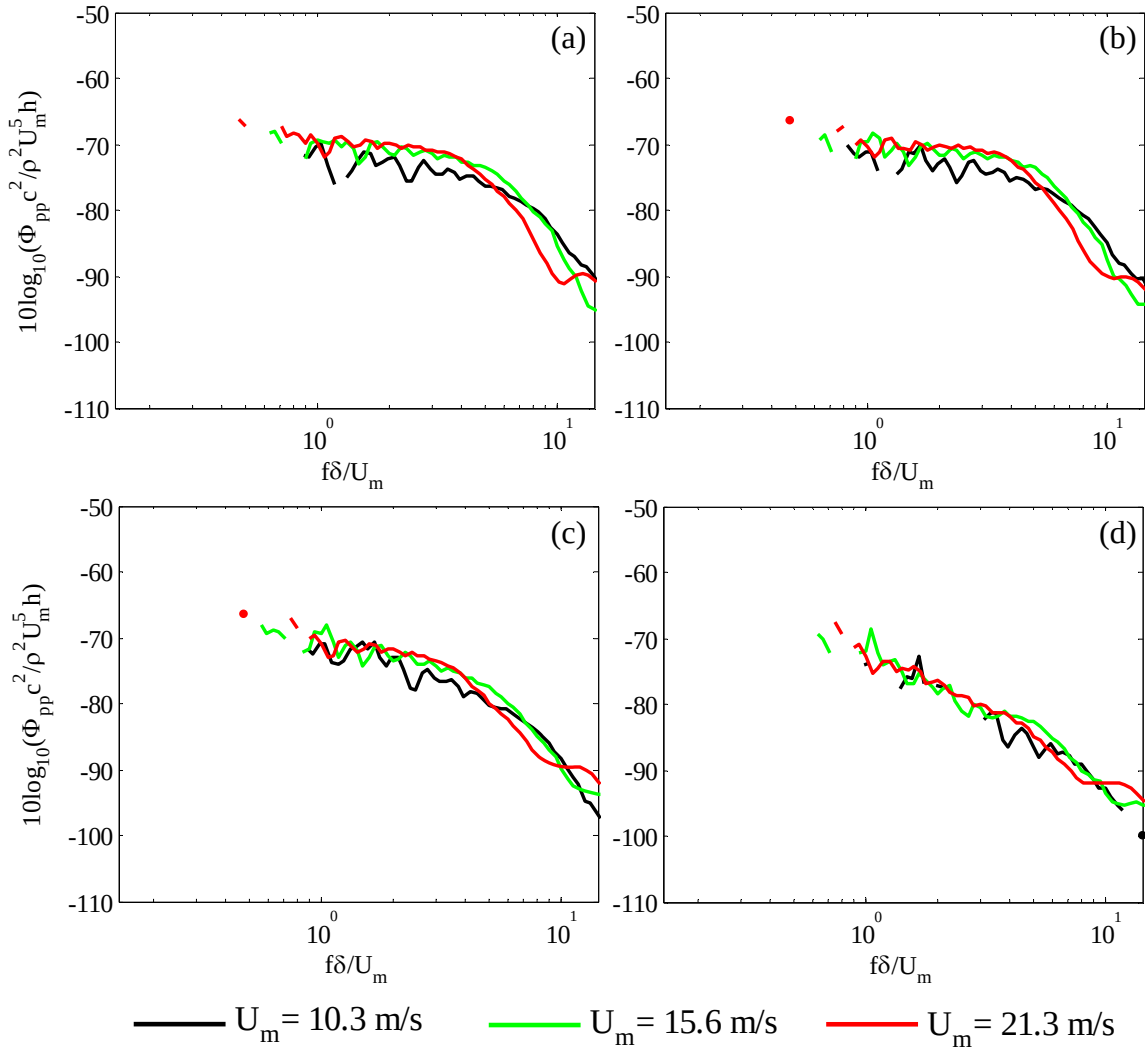


Figure 2-42 Mixed scaling of far field sound spectra for rounded forward facing steps at $\theta = 90^\circ$. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$ and (d) $r/h = 25\%$.

To consider the directivity pattern of sound radiated from rounded forward steps we can plot the spectra from these steps at each observer location on the same plot. We will only be considering the directivities at $U_m = 21.3$ m/s since those for lower velocities are qualitatively the same. Figure 2-43 (a)-(d) show the spectra for all observer locations within each plot for four rounding of step corner. The legend at the bottom of the figure shows the change in sound levels (Δ_{dB}) for each observer location compared to the observer at $\theta = 51.5^\circ$ if a streamwise aligned dipole directivity pattern is assumed (see §2.2.4.3 for details on how Δ_{dB} was calculated). Note that the spectra for $r/h = 0\%$ in Figure 2-43 (a) are the same as in §2.2.4.3 and have been repeated here for the sake of comparison. Looking at these figures it can be seen that rounding the step corner does not qualitatively change the directivity pattern for radiated sound. As for an unrounded forward step the spectra for each rounding shows variation of sound levels across the measurement arc at lower frequencies suggesting a dipole like pattern. Quantitatively, however, the variation of sound at lower frequencies becomes less pronounced as the rounding increases. At higher frequencies the spectra for rounded forward steps also show the effect of non-compactness and the spectral dip is clearly visible in each plot. One noticeable difference however is that for $r/h = 12.5\%$ and 25% (Figure 2-43 (c) and (d) respectively) the spectral dip shifts to lower frequencies and has a much smoother appearance. This as explained earlier is likely because with increasing rounding the contour length of the

step face increases due to the added circular geometry, so that sound generated at the end of the step corner has further to travel before being reflected from the upstream wall.

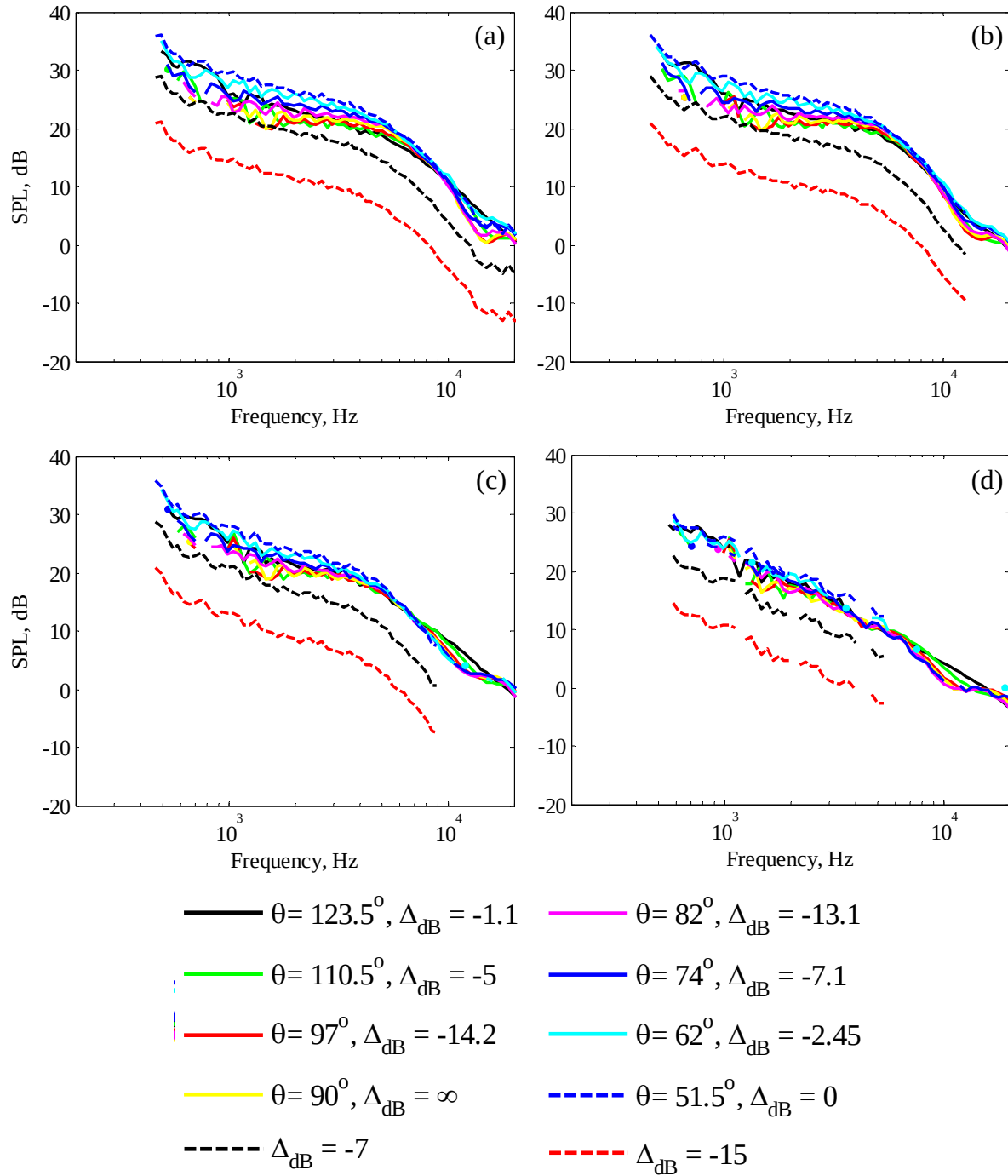


Figure 2-43 Directivity patterns for rounded forward facing step at $U_m = 21.3 \text{ m/s}$. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$ and (d) $r/h = 25\%$

2.2.5 Far Field Sound from Gaps

The far field sound from gaps was measured using the same 8 microphone arc used for the rounded forward facing steps (see §2.1.6.1 for description of the microphone setup). As described in §2.1.4 two

different types of gap configurations referred to as type I and type II gaps were considered. Type I gaps consist of an unrounded forward facing step ($r_2/h = 0\%$) and three different rounding of the backward step corner of $r_1/h = 0\%$, 12.5% and 50%. For these gaps four gap widths of $w = 1.13h$, $2h$, $4h$ and $8h$ were also considered. The type II gap configuration is where the backward step was kept unrounded ($r_1/h = 0\%$) and six different rounding radii for the forward step of $r_2/h = 0\%$, 6.25%, 12.5%, 25%, 50% and 100% were considered. For these gaps measurements were made for a single gap width of $w = 4h$. All gap measurements were made at three different wall-jet nozzle exit velocities of $U_j = 30$ m/s, 45 m/s and 60 m/s which correspond to undisturbed edge velocity values of $U_m = 10.3$ m/s, 15.6 m/s and 21.3 m/s respectively at the forward facing step location.

We will first discuss the impact of the transition ramp used to create the backward step portion of the gap on the far field sound (see §2.1.4 for the description of this transition ramp). This will be followed by a discussion on the far field sound behavior from flow over unrounded gaps. Next, the effect of rounding the backward step portion of the gap (type I gap) on far field sound will be considered. Finally, the impact of rounding the forward step portion of the gap (type II gap) on the far field sound will be presented. The far field sound results presentation will follow the convention described in §2.2.2.

2.2.5.1 Noise from Transition Ramp

As discussed in §2.1.4 a 305 mm long transition ramp was used to raise the flow from the wall-jet surface to the gap height of 12.7 mm. This transition and the attached backstep were also moved in the streamwise direction to vary the gap width. To examine whether the transition ramp itself was producing any sound, measurements of far field sound were made for a zero gap width configuration as shown in Figure 2-44 below. These measurements were then compared to those made for undisturbed boundary layer to determine the effect of transition ramp on far field sound. Since the gap width was changed by moving the backward step assembly upstream, the zero gap width measurements were made for each streamwise location of the transition ramp.

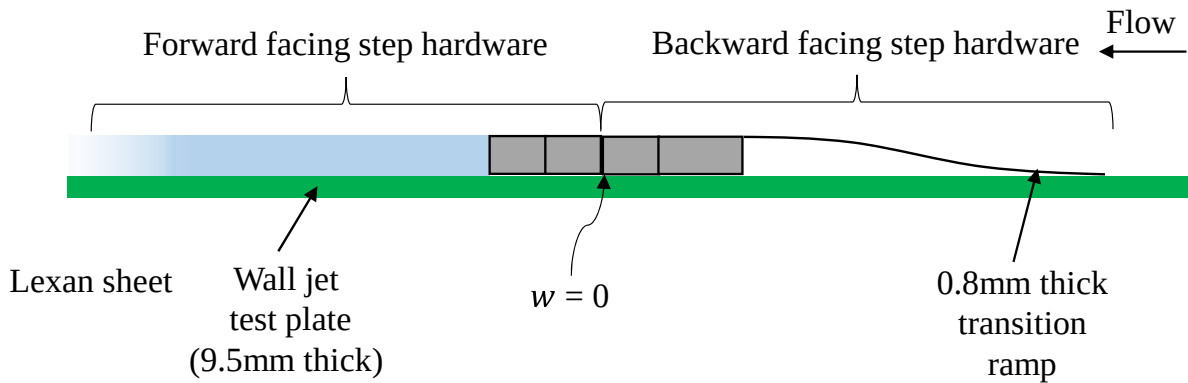


Figure 2-44 Setup to quantify the effect of transition ramp on the far field sound.

Figure 2-45 (a) and (b) show the far field sound at $\theta = 123.5^\circ$ and $\theta = 90^\circ$ respectively measured at three different velocities by installing the transition ramp with a gap width of zero at locations where it was used to create gap width of $8h$, $4h$ and $1.13h$. Also shown in the figure is the far field spectra measured at the three velocities for the undisturbed flow. It can be seen that the use of transition piece makes virtually no difference to the background noise at either observer location shown here. The maximum deviation from the smooth wall conditions for any case is ± 1 dB which is within the measurement uncertainty. In light of these results the sound spectra from the gap configurations were obtained by subtracting the undisturbed flow spectra (see § 2.2.2 for details on background subtraction) as done for rounded forward steps.

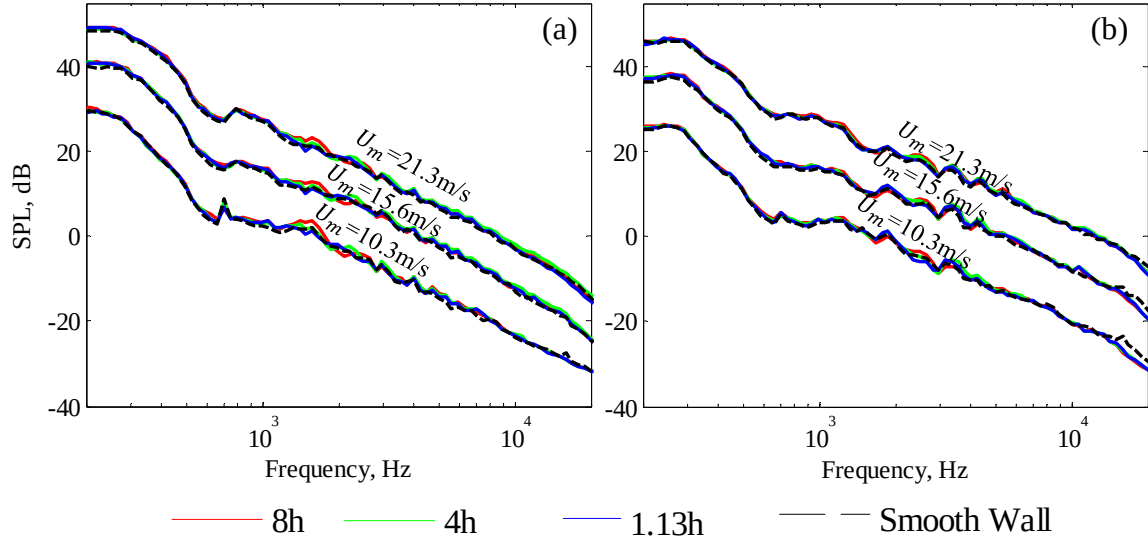


Figure 2-45 Effect of transition ramp installation at three different gap width locations on the background far field sound. (a) $\theta = 123.5^\circ$ and (b) $\theta = 90^\circ$.

2.2.5.2 Far Field Sound from Unrounded Gaps

Figure 2-46 (a)-(d) show the background subtracted far field sound spectra measured at an observer location of $\theta = 123.5^\circ$ for unrounded gaps ($r_1/h = r_2/h = 0\%$) as a function of gap width. Each plot shows spectra for three different velocities as solid curves. The dashed curves of the same color scheme as the solid curves represent the sound measured at $\theta = 123.5^\circ$ from the unrounded forward facing step. Consider now the sound from unrounded gap with $w = 1.13h$ as shown in Figure 2-46 (a). The spectra show a uniform increase in sound levels throughout the frequency range with increasing velocity. At low frequencies, though the gap sound is comparable to that from an unrounded forward facing step of same height, the gap spectra shows a hump like structure with a different slope than that for a forward step alone configuration. At higher frequencies the gap spectra are 4-5 dB lower than that for a forward step alone configuration. Additionally, the gap spectra develop some scalloping at higher frequencies. This scalloping is possibly due to acoustic reflections from the gap surfaces. The spectra for the two higher velocities where SNR is highest show a dip at a frequency of about 12 kHz. The half acoustic wavelength corresponding to this frequency is about 14.5 mm which is about the same as this gap width ($w = 14.4$ mm). This suggests that this spectral dip is being caused by destructive interference due to the reflection of sound from within the gap.

As the gap is widened to $w = 2h$ (Figure 2-46 (b)) the spectra at lower frequencies are now not only quantitatively comparable to the forward step spectra but they also develop a similar shape. The higher frequencies again show attenuated sound levels with scalloping. However, there are now two frequencies (approximately 7 kHz and 14 kHz) at which the spectra shows dips. The half acoustic wavelengths associated with these two frequencies are about 24.6 mm and 12.3 mm which are close to the gap width and the step height respectively for this gap. This suggests that as the gap opens up both the step height and the gap width begin to play a role in determining the high frequency behavior. There is no qualitative difference in the behavior of sound with changing velocity for this gap width either.

For an unrounded gap width of 4 step heights (Figure 2-46 (c)) the low frequency behavior remains essentially the same as for the $w = 2h$ gap but now the spectra at higher frequencies start to become smoother for each velocity and approach the level of forward step alone configuration. The scalloping behavior and the spectral dips are still present at high frequencies, however, these features are less pronounced than those for the narrower gaps. For the widest gap width of $w = 8h$ (Figure 2-46(d)) the shape

of the sound spectra resembles that for a forward facing step throughout the frequency range. However, unlike narrower gaps the lower frequency spectral levels are now 4-5 dB higher than those for forward facing step.

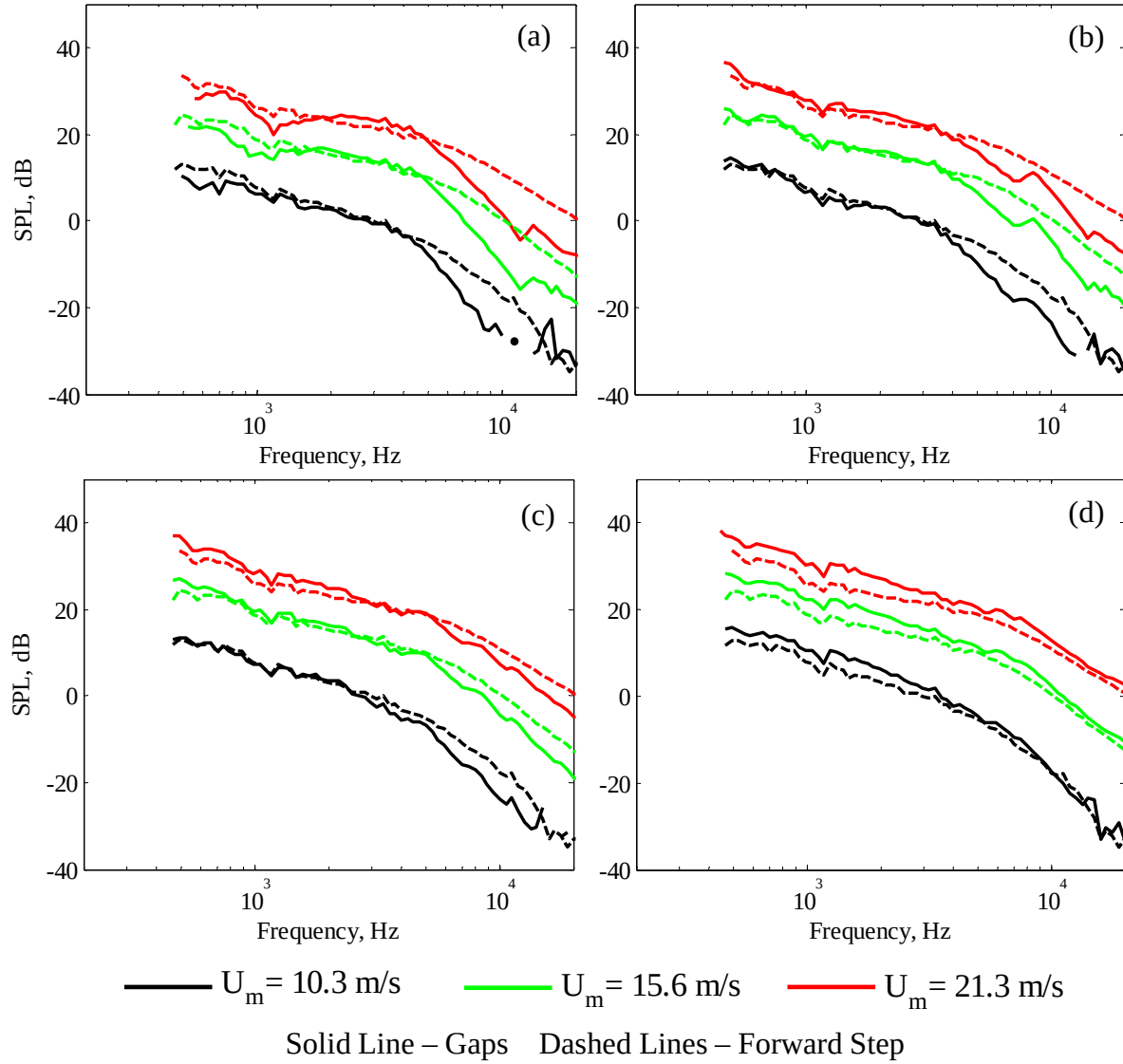


Figure 2-46 Far field sound spectra for unrounded gap configurations with different gap width at three different velocities, $\theta = 123.5^\circ$. (a) $w = 1.13h$, (b) $w = 2h$, (c) $w = 4h$ and (d) $w = 8h$.

The results of Figure 2-46 show that for wide gaps ($w \geq 4h$) the sound from gaps closely resembles a forward facing step. It has been shown by some previous studies (e.g. Driver *et al.* (1987), Eaton and Johnston (1981), Ji and Wang (2010), Farabee and Casarella (1986)) that the flow reattachment behind the backward step immersed in a conventional boundary layer with free stream occurs some 5-7 step heights downstream of the step. However, Jacob *et al.* (2011) who measured reattachment length behind a backward facing step in wall-jet flow using Laser Doppler Anemometry (LDA) found the reattachment length to be significantly shorter. They show that the flow reattaches between 2.75-3 step heights downstream of the step. Taking this to be the case in present flow we note that for $w = 1.13h$ and $2h$ the separated shear layer presumably flows straight over forward facing step portion of the gap. This suggests that the reason we see lower sound levels at higher frequencies for narrow gaps compared to forward step is because the flow

received by the forward step portion of the gap is not as well developed as a standalone forward step. On the other hand, the qualitative and quantitative agreement between the gap noise with $w = 2h$ and forward step noise at lower frequencies suggests that (at least for this observer location) perhaps the fluid dynamics of the vertical face region of the forward step may not be an important sound source at lower frequencies. For the gap width of 4 step heights the flow presumably reattaches within the gap which is the likely reason why the spectra for this gap begin to resemble those from a forward facing step for a broader frequency range. For the widest gap ($w = 8h$) considered here the separated flow behind the backward step reattaches within the gap and has some room to develop into an attached but more energized boundary layer before it reaches the forward step portion of the gap. Since forward steps are known to be louder than backward steps (see for e.g. studies by Farabee and Zocolla (1998), Catlett *et al.* (2014), Ji and Wang (2010)) the sound for $w = 8h$ configuration is completely dominated by the forward step portion and the spectra look identical to that from an isolated forward step.

Now consider the sound from the four gap configurations for observer located directly above the forward step portion of the gap ($\theta = 90^\circ$) as shown in Figure 2-47. The layout of this figure is the same as Figure 2-46. Here we see that the narrowest gap configuration ($w = 1.13h$, Figure 2-47 (a)) shows the same behavior as seen for observer located upstream (Figure 2-46 (a)) in that the sound levels at low to mid frequencies are similar to a forward step alone configuration but the spectra has a slightly different shape. At higher frequencies the gap spectra shows similar attenuated levels with a jagged appearance as seen further upstream. As the gap is widened to $w = 2h$ (Figure 2-47 (b)) the quantitative and qualitative character of the spectra at lower frequencies is now almost the same as that for a forward step but the higher frequency behavior continues to be different. The spectral dip associated with destructive interference in forward step is also observed in gap sound for the two narrow gaps but its magnitude is much lower (evident by missing spectral data points). As the gap opens up (Figure 2-47 (c) and (d)) the spectra begin to resemble forward step alone configuration much in the same way as for the observer located upstream.

Figure 2-48 shows the far field sound spectra as a function of gap width for four different observer locations at $U_m = 21.3$ m/s. Each plot in this figure shows the effect of gap width on sound spectrum for a particular observer. The sound spectra for the forward step at the same observer location has also been shown on each plot. Looking at the figure it can be seen that changing the observer location does not have any influence on the relative behavior of sound between a forward step and gaps. At each location the sound from the narrowest gap shows lower levels compared to a forward step and the spectral shape is also different. The low frequency content for $w = 2h$ and $4h$ is quantitatively and qualitatively similar to the forward step while for $w = 8h$ the spectra shows higher levels compared to a forward step. The higher frequency content at each observer location begins to appear smoother as the gap is widened likely due to the dominance of fluid dynamic sound source compared to the acoustic reflections for wider gap configurations.

The present measurements of radiated sound from flow over unrounded gaps is in close quantitative agreement with the results of Catlett *et al.* (2014) who also measured sound from similar gap configuration in the wall-jet facility. Figure 2-49 shows the comparison of present measurements at $U_m = 21.3$ m/s and measurements of Catlett *et al.* (2014) for observer located at $\theta = 123.5^\circ$. The comparison is shown for two gap width of $2h$ and $8h$ which were common between the two experiments. The correction factors which were introduced in §2.2.4.2 to account for the differences in measurement conditions have been applied to the present data. It can be seen that the past and the present data show good agreement for the gap with $w = 8h$. For the gap width of 2 step heights there is also a good agreement. As seen in comparison of present forward step noise measurements with Catlett *et al.* (2014) (§2.2.4.2) any disagreement between the measurements is limited to frequencies where the spectra are dominated by the non-compactness of the gap configuration due to the difference in gap height between the two measurements.

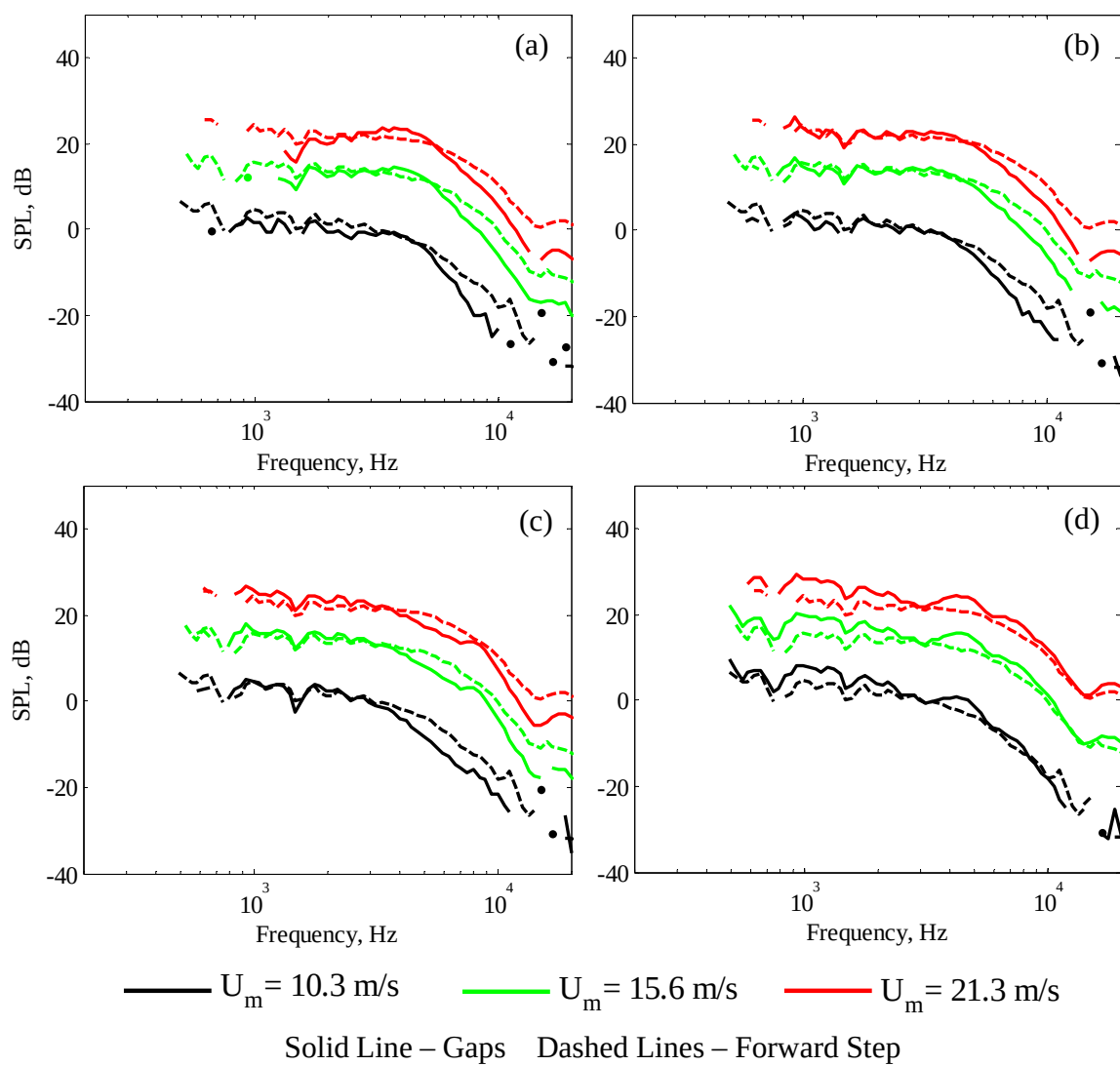


Figure 2-47 Far field sound spectra for unrounded gap configurations with different gap width at three different velocities, $\theta = 90^\circ$. (a) $w = 1.13h$, (b) $w = 2h$, (c) $w = 4h$ and (d) $w = 8h$.

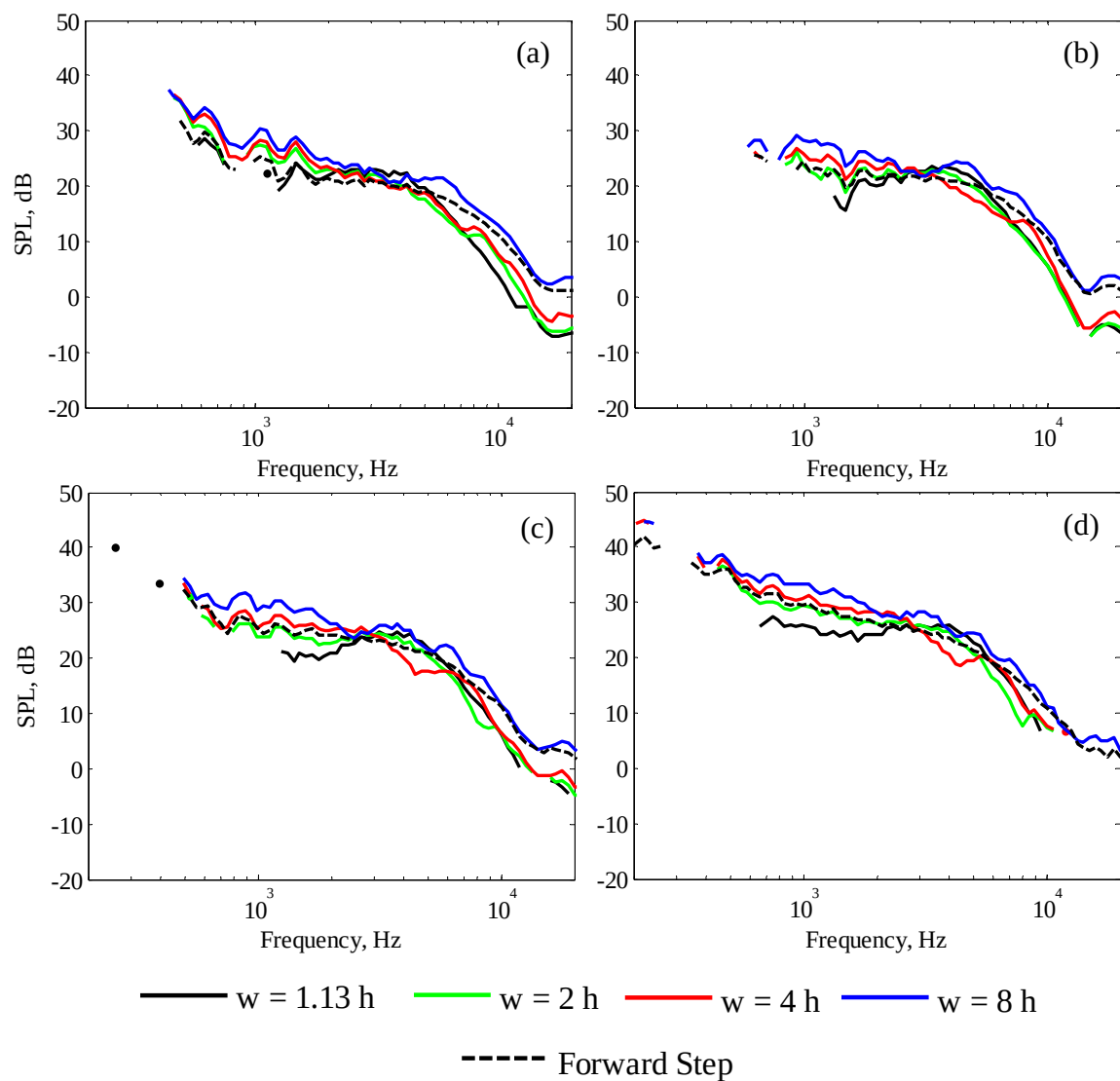


Figure 2-48 Effect of gap width on far field sound at four separate observer locations, $U_m=21.3\text{m/s}$. (a) $\theta=110.5^\circ$, (b) $\theta=90^\circ$, (c) $\theta=74^\circ$ and (d) $\theta=51.5^\circ$.

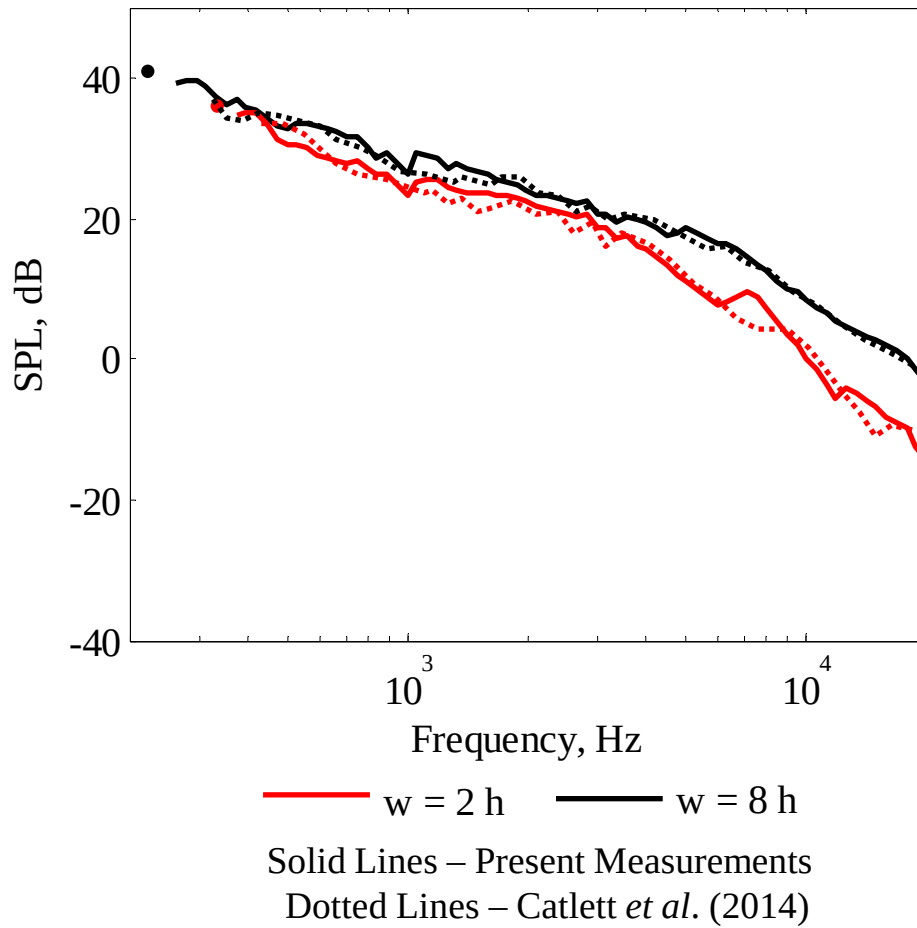


Figure 2-49 Comparison of present measurements of sound from unrounded gaps with Catlett *et al.* (2014), $U_m = 21.3$ m/s, $\theta = 123.5^\circ$. Correction factors of Table 2-7 to account for difference in measurement conditions have been applied to present data.

The sound spectra at three different velocities for unrounded gaps at $\theta = 123.5^\circ$ scaled on the mixed scaling proposed by Catlett *et al.* (2014) are shown in Figure 2-50 (a)-(d). It can be seen that for the two narrow gaps with $w = 1.13h$ and $2h$ the data does not collapse at most frequencies because the sound from these gaps, especially at mid to high frequencies, is dominated by acoustic reflections from within the gap as evident by their scalloping behavior. However, as the gap gets wider and the shear layer starts to interact with the forward facing step the mixed scaling which is based on the flow parameters and step height provides good collapse of data throughout the frequency range (Figure 2-50 (c) and (d)).

Figure 2-51 shows the far field sound spectra measured at $\theta = 90^\circ$ scaled using the mixed scaling for each gap configuration. In this case we see that the scaling does not collapse the data irrespective of the gap width. As we had seen in the far field sound from forward steps that the region above the step was dominated by non-compactness effects associated with acoustic reflection from the upstream surface. In the case of gaps the presence of the vertical face of backward step provides another surface for reflections leading to the spectra being further dominated by the acoustic processes.

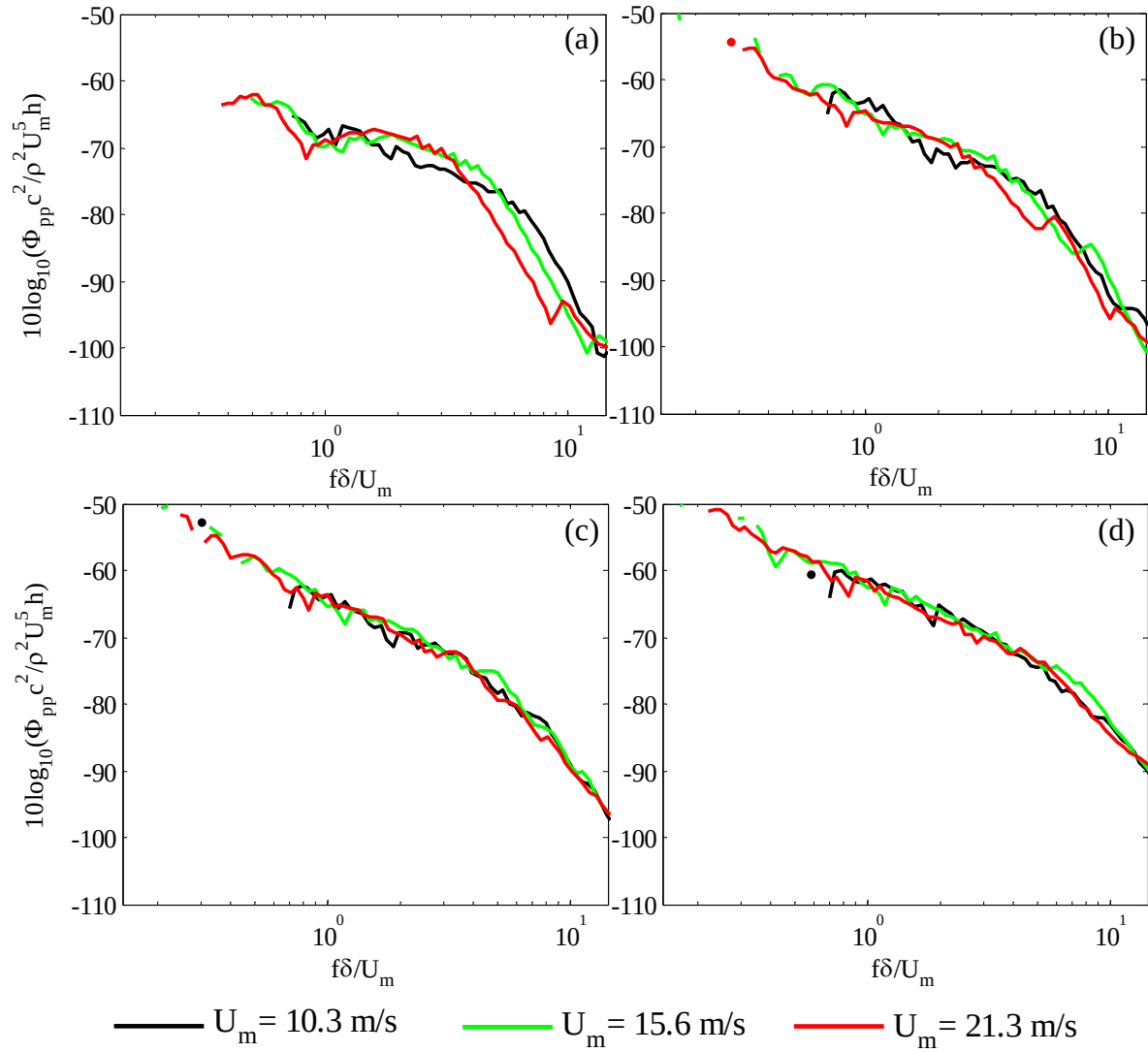


Figure 2-50 Mixed scaling for far field sound from unrounded gaps, $\theta = 123.5^\circ$. (a) $w = 1.13h$, (b) $w = 2h$, (c) $w = 4h$ and (d) $w = 8h$.

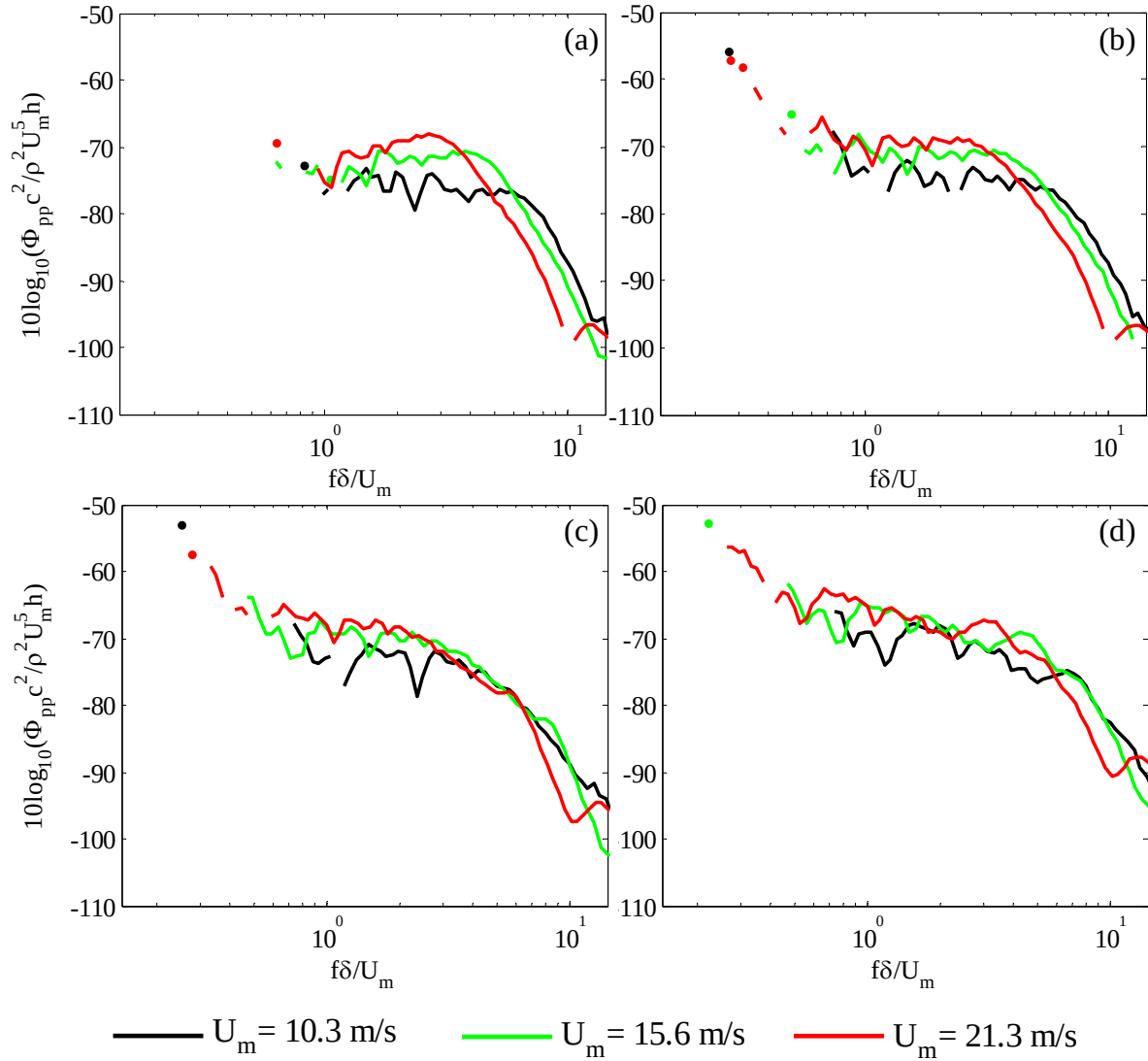


Figure 2-51 Mixed scaling for far field sound from unrounded gaps, $\theta=90^\circ$. (a) $w=1.13h$, (b) $w=2h$, (c) $w=4h$ and (d) $w=8h$.

2.2.5.3 Far Field Sound from Type I Gaps

We will now consider the effects of rounding the corner of the backward step portion of the gap while keeping the forward step portion unrounded. Figure 2-52 shows background subtracted sound spectra at $\theta = 123.5^\circ$ for four different gap widths, all three backward step rounding radii and all three velocities. The velocity corresponding to each curve is shown just above it. The most striking and perhaps surprising aspect of the sound spectra shown here is that regardless of the gap width and flow speed the rounding of the backward step corner of the gap has negligible impact on the far field sound. For the narrowest gap of $w = 1.13h$ and higher velocities the rounding of the backward step corner leads to a slight elevation (about 1.5 dB and 4 dB at $U_m = 21.3$ m/s and 15.6 m/s respectively) of sound at low frequencies (below 2 kHz). However, as the gap width increases this discrepancy goes away and for $w > 1.13h$ the sound from each of the three rounded gaps is nearly identical throughout the frequency range. This result is surprising because changing the rounding of the backward step can be expected to change the behavior of the shear layer being received by the forward step portion of the gap. For example Bravo and Zheng (2000) measured the reattachment length from a rounded backward step with $r/h = 90\%$ and found it to be approximately 27%

shorter than a similar backward facing step. Bentalab *et al.* (2012) who performed highly resolved LES simulations of flow over a backward step with a gentle gradient along its trailing edge have found that both the separation and reattachment are unsteady and that the instantaneous reattachment length can extend over most of the separated zone. Furthermore, they also show that the dividing streamline between the shear layer and recirculating flow has quite a shallow angle. These results suggest that at least for the highest rounding of $r_1/h = 50\%$ one should expect the interaction of the shallow separated shear layer and the forward step geometry to influence the radiated sound. However, it seems that this interaction has little to no bearing on the sound being produced by the gap and the spectra remain similar to those for an unrounded gap configuration. These results suggest that the self-noise from the forward step is quite strong and completely dominates the behavior of the overall sound being produced by the gap configurations.

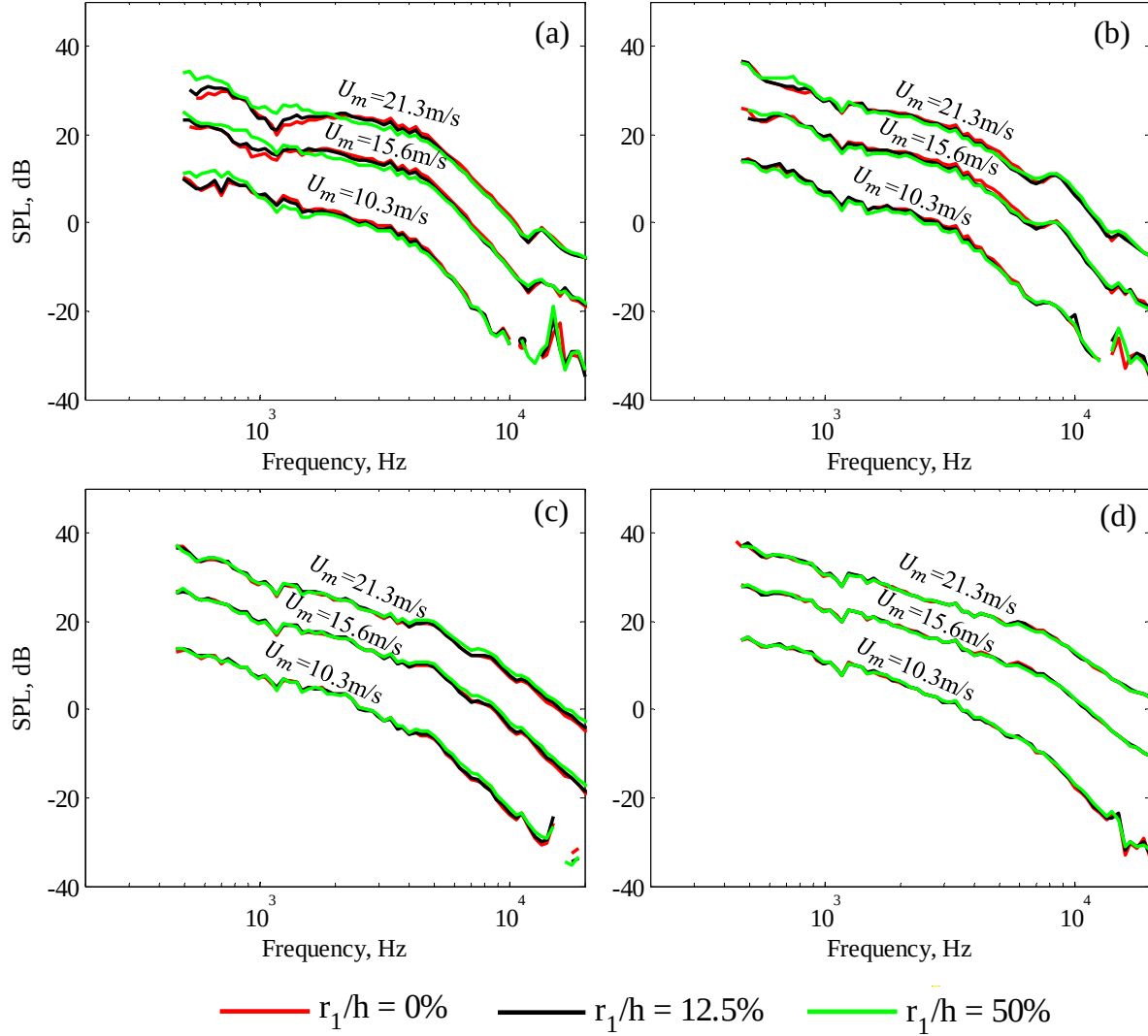


Figure 2-52 Far field sound from type I gaps at $\theta = 123.5^\circ$. (a) $w = 1.13h$, (b) $w = 2h$, (c) $w = 4h$ and (d) $w = 8h$. The undisturbed edge velocity U_m for each curve is shown above it.

Let us now look at the effect of backward step rounding on the radiated sound at four additional observer locations. The sound spectra for all three rounding of the backward step at four observer locations have been shown in Figure 2-53 (a)-(d) for gap width of $w = 1.13h$, $2h$, $4h$ and $8h$ respectively. The spectra shown are all for the undisturbed edge velocity of $U_m = 21.3$ m/s. Note that the spectra for all three rounding at a particular observer location have been scaled by the same constant factor r so that all four observer

locations for a particular gap width could be shown on the same plot. So, the absolute magnitude of the spectra is arbitrary as noted along the abscissa of the figure. Again we see that, at these other observer angles, the sound field is nearly independent of the backward step rounding for all gap widths considered. The only effect of backward step rounding is seen for the narrowest gap (Figure 2-53 (a)) where the sound due to the largest rounding is slightly higher at frequencies below about 2 kHz. This increase is possibly because the interaction between the shallow separated layer from the rounded backward step and the forward step is significant for this gap due to the proximity of the two steps.

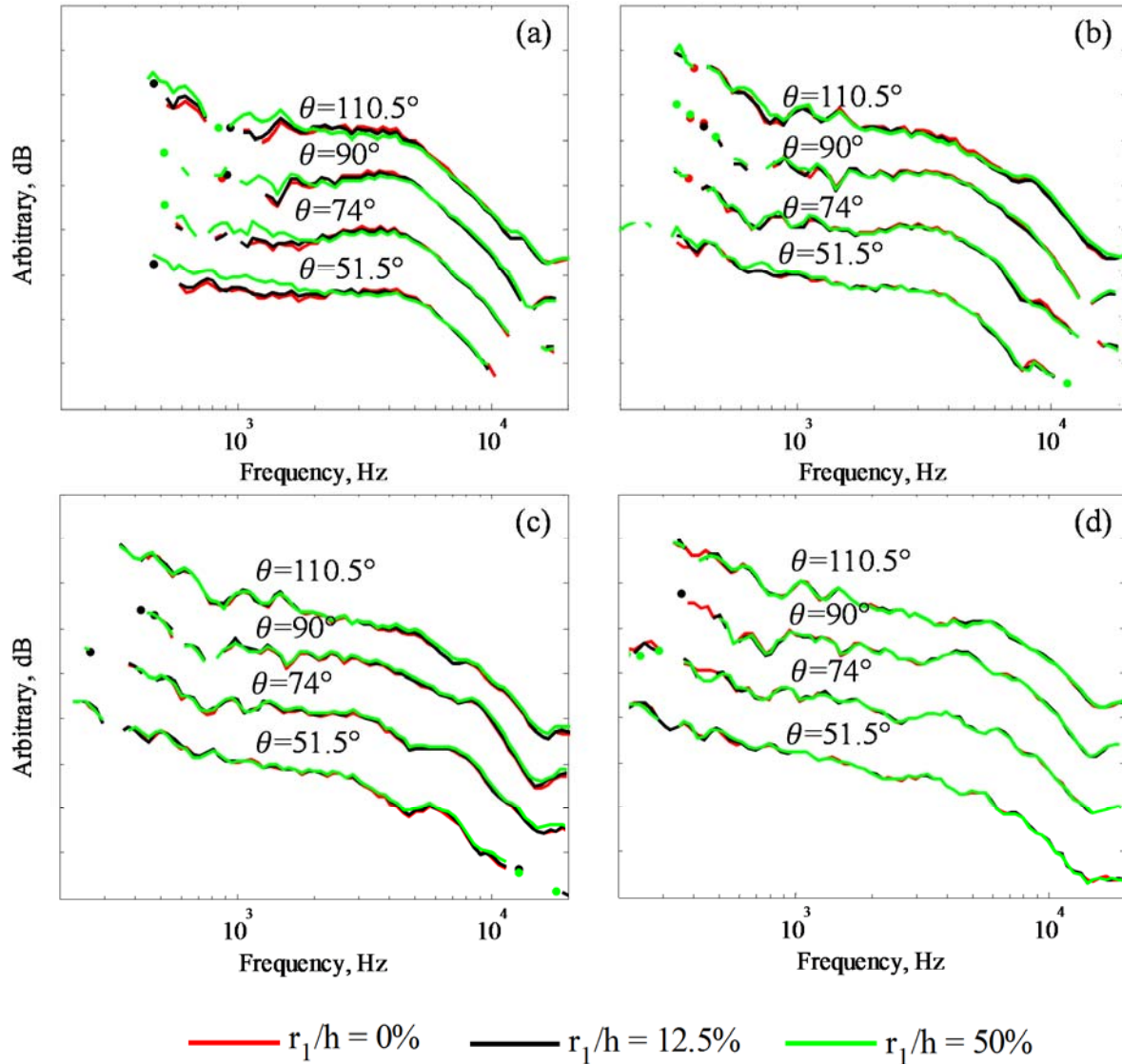
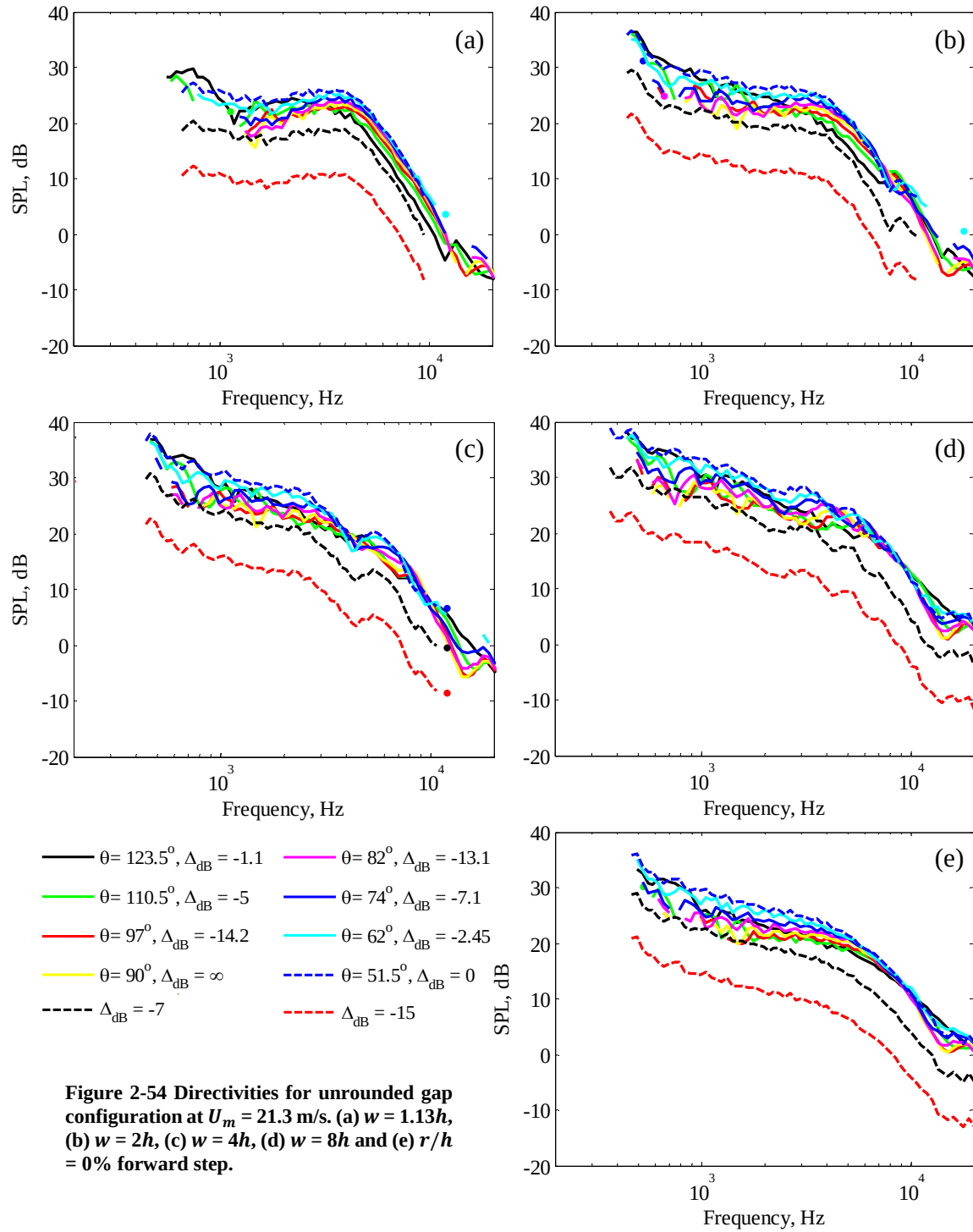


Figure 2-53 Far field sound from type I gaps at four separate observer locations, $U_m = 21.3\text{m/s}$. (a) $w = 1.13h$, (b) $w = 2h$, (c) $w = 4h$ and (d) $w = 8h$. Note that the magnitude of each curve has been scaled by an arbitrary value to show sound at different observer locations on the same plot. The observer location for a particular curve has been shown just above that curve. The legend is the same as that in Figure 2-52.

We will now look at the directivity pattern of sound from two-dimensional gaps. Since we have seen that the sound spectra are essentially independent of the backward step rounding we will only look at the directivities from unrounded gap configuration. We will only consider the directivities at $U_m = 21.3\text{ m/s}$ since those for lower velocities are qualitatively the same. The directivities are plotted as spectra for



each observer location as done for forward steps directivities (see §2.2.4.3). Figure 2-54 (a)-(d) show the directivities from unrounded gaps for all four gap width at $U_m = 21.3$ m/s. The spectra for the unrounded forward step at the same velocity have been shown in Figure 2-54 (e). The legend shows the reduction in sound level at each observer compared to the observer at $\theta = 51.5^\circ$ assuming a streamwise aligned dipole directivity pattern. Also shown in each plot are the two curves which show the reference spectrum less 7 dB and 15 dB for quantitative comparison. If we now compare the spectra for gaps at lower frequencies to

those for a forward step we see that except for the narrowest gap ($w = 1.13h$, Figure 2-54 (a)) each gap spectra shows similar variation of sound with observer location as the forward step configuration. For the narrowest gap configuration the lower frequencies are suppressed at each observer location and is presumably due to the forward step portion being embedded within the wake of the backward step. The higher frequencies for $w = 4h$ and $8h$ show similar slope as forward step spectra but have a jagged appearance especially for $w = 4h$ which is likely due to the acoustic reflections within the gap. For the two narrow gaps ($w = 2h$ and $1.13h$) the higher frequencies show a much steeper slope and more variation in sound levels across the measurement arc presumably due to the shear layer flowing straight over the gap.

2.2.5.4 Far Field Sound from Type II Gaps

We will now consider the effects of rounding the forward step portion of the gap while keeping the backward step portion unrounded. Note that unlike type I gaps the sound from type II gaps was measured only for one gap width of $w = 4h$. The rounding radii applied to the forward step portion of the gap are exactly the same for which sound was measured from forward steps and so this provides a good basis for comparison. Figure 2-55 (a) shows the background subtracted sound spectra measured by the most upstream microphone ($\theta = 123.5^\circ$) at $U_m = 21.3$ m/s for all six rounding of the aft portion of the gap. For sake of comparison the sound spectra from rounded forward steps measured under the same conditions are repeated in Figure 2-55 (b).

Looking at Figure 2-55 (a) we see that unlike type I gaps the rounding of the forward step portion of the gap makes considerable difference to the sound field. This is not a surprising result given that we have already seen in §2.2.5.2 that the sound from the gap of the same width is dominated by the forward step component. The qualitative effect of rounding the aft corner of the gap on sound spectra is essentially the same as that for the forward step configuration. However, there are some quantitative differences. For the two lowest rounding radii ($r_2/h = 6.25\%$ and 12.5%) the attenuation of sound is more than what was seen in forward step alone configuration. Rounding the gap corner to $r_2/h = 25\%$ produces less attenuation of sound compared to that seen for forward step with the same rounding. For the forward steps the sound from $r/h = 50\%$ and 100% was limited to highest frequencies and was shown to be due to scattering of the background noise by the step. However, for gap configuration with the same rounding of the forward step corner sound is seen at lower frequencies as well. This difference could most likely be due to the presence of backward step which in addition to producing some sound also modifies the flow received by the forward step. To further explore this we can compare the sound being produced by $r_2/h = 50\%$ and 100% gaps to that produced by a backward step alone configuration. Although, no measurements of sound from backward step alone configuration were made in the present work, Catlett *et al.* (2014) measured sound from backward steps in the same facility at approximately the same observer location for which the gap spectra are shown in Figure 2-55. We have already seen in §2.2.4.2 that their results for forward step sound are in good agreement with the present results once the differences in the flow conditions and step geometry are accounted for via a simple source distribution model. Thus, we expect that their backward step results will be applicable to present work. Figure 2-56 (a) and (b) compare the sound from the type II gaps with $r_2/h = 50\%$ and 100% to the sound produced by a backward step alone configuration which was measured by Catlett *et al.* (2014). As seen in the Figure 2-56 (a) the sound due to a $r_2/h = 50\%$ gap closely resembles the sound from a backward step alone configuration above about 2 kHz. This gap does produce some additional low frequency content which could be due to the forward step-separated shear layer interaction. As the aft corner of the gap is rounded with radius equal to step height (Figure 2-56 (b)) the sound spectra starts to look nearly the same as that for a backward step. These comparison suggest that at least for higher rounding ($r_2/h \geq 50\%$) the spectra are contaminated with sound from the backward step resulting in their noisier appearance compared to forward step alone configuration.

Figure 2-57 (a)-(d) shows the sound spectra as a function of rounding of the gap corner for four observer locations of $\theta = 110.5^\circ$, 90° , 74° and 51.5° respectively. This figure is comparable to Figure 2-40 which showed the sound spectra for forward steps with rounded corners measured at the same observer locations and the same velocity. Looking at Figure 2-57 (a) we see that the rounding of the gap corner has

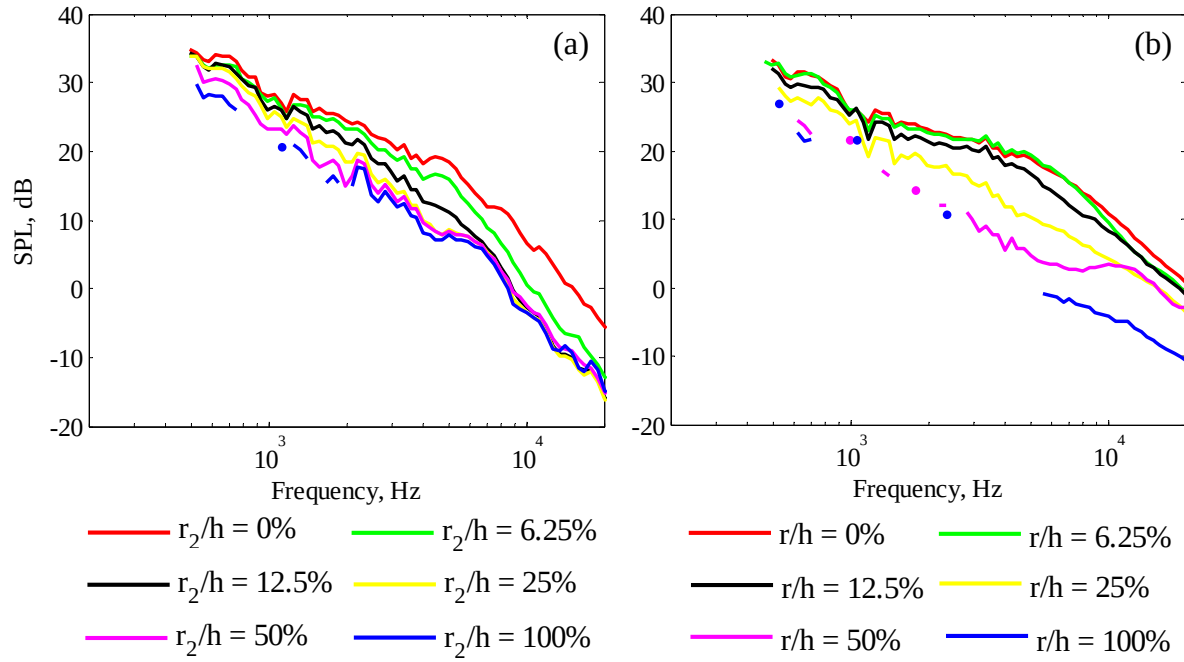


Figure 2-55 Far field sound from type II gaps, $\theta = 123.5^\circ$, $U_m = 21.3$ m/s. (a) Sound spectra from type II gaps and (b) Sound spectra from forward steps with same rounding radii as that of type II gaps.

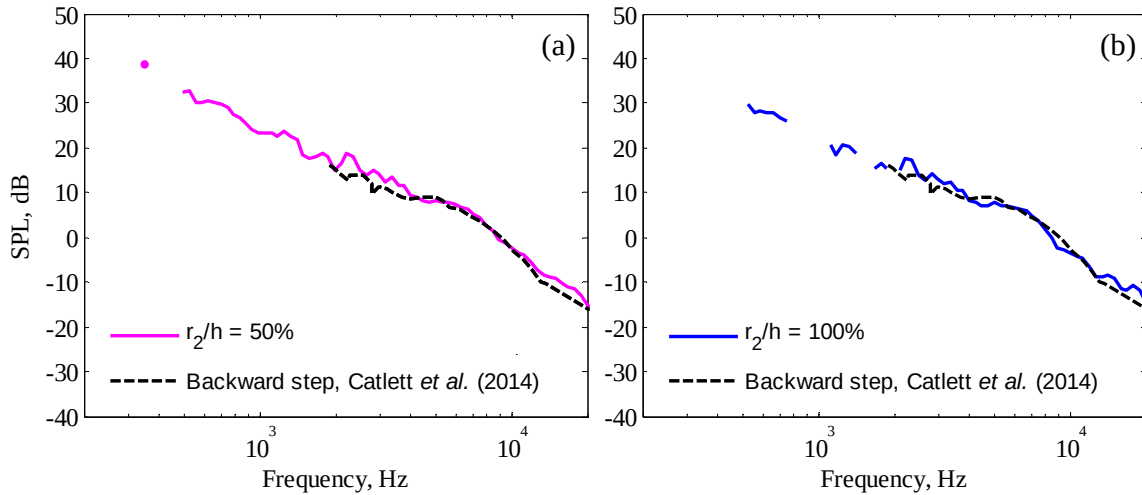


Figure 2-56 Comparison of far field sound from type II gaps with corner rounding of $r_2/h = 50\%$ and 100% with far field sound from a backward step configuration.

the same qualitative effect on sound as rounding of forward step alone configuration. However, similar to the spectra at $\theta = 123.5^\circ$ (Figure 2-55) there are some quantitative differences with lower rounding ($r_2/h = 6.25\%$ and 12.5%) showing more sound attenuation and higher rounding ($r_2/h \geq 25\%$) showing less sound attenuation when compared to a forward step configuration. Similar to forward steps the rounded gap spectra also begin to show the spectral trough associated with the destructive interference but this effect is not as pronounced as seen for forward steps or unrounded gap configuration. The mid to high frequency content of the spectra at this location also appears to smooth out as the rounding increases. Moving further downstream to the observer located directly above the forward step component of the gap ($\theta = 90^\circ$, Figure

2-57 (b)) we see that the spectra for lower rounding ($r_2/h < 25\%$) show a similar shape as that for a forward step. However, compared to a forward step the SNR at this location for gaps is low enough so that no sound is seen above 10 kHz for any rounded gap configuration. Further downstream at $\theta = 74^\circ$ (Figure 2-57 (c)) the sound continues to decrease with the rounding and for $r/h = 50\%$ and 100% there is barely any sound being produced. At the last downstream location ($\theta = 51.5^\circ$, Figure 2-57 (d)) this trend continues, the sound drops with increasing rounding but compared to a forward step configuration the flow over gap produces low SNR and the frequencies above 10 kHz do not show any sound for any gap configuration.

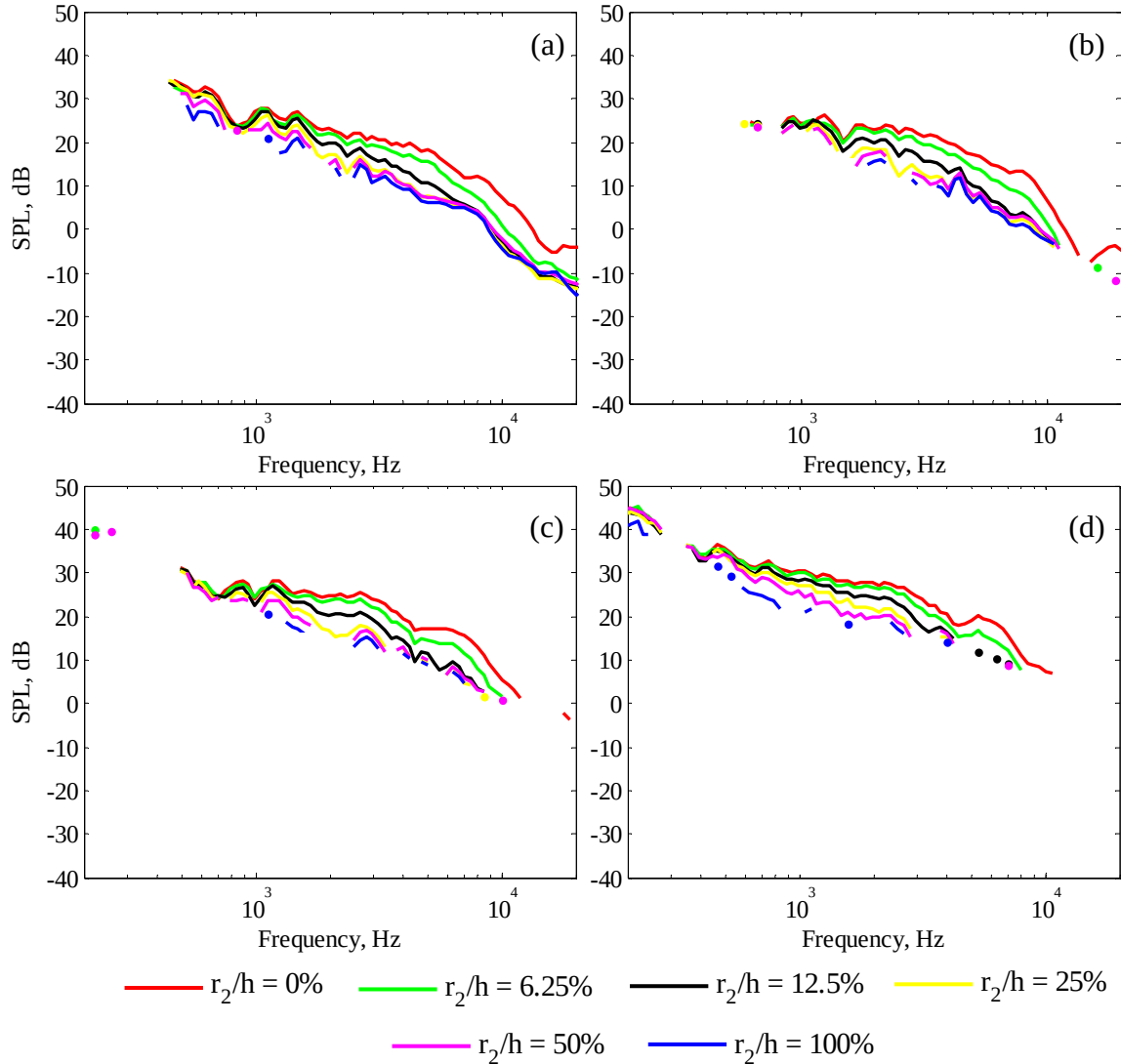


Figure 2-57 Far field sound from type II gaps at four separate observer locations, $U_m = 21.3$ m/s. (a) $\theta = 110.5^\circ$, (b) $\theta = 90^\circ$, (c) $\theta = 74^\circ$ and (d) $\theta = 51.5^\circ$.

Figure 2-58 (a), (c) and (e) show the directivities as spectra for each observer location for type II gaps at $U_m = 21.3$ m/s for $r_2/h = 6.25\%$, 12.5% and 25% respectively. For comparison the spectra at the same velocity and observer locations for forward step alone configurations with $r/h = 6.25\%$, 12.5% and 25% are shown in Figure 2-58 (b), (d) and (f) respectively. The legend of the figure shows the sound reduction (Δ_{dB}) at each observer location compared to observer at $\theta = 51.5^\circ$ assuming a streamwise aligned dipole directivity (see §2.2.4.3 for details on calculation of Δ_{dB}). The curves representing the reference spectrum (at $\theta = 51.5^\circ$) less 7 dB and 15 dB have also been shown in each plot to enable quantitative

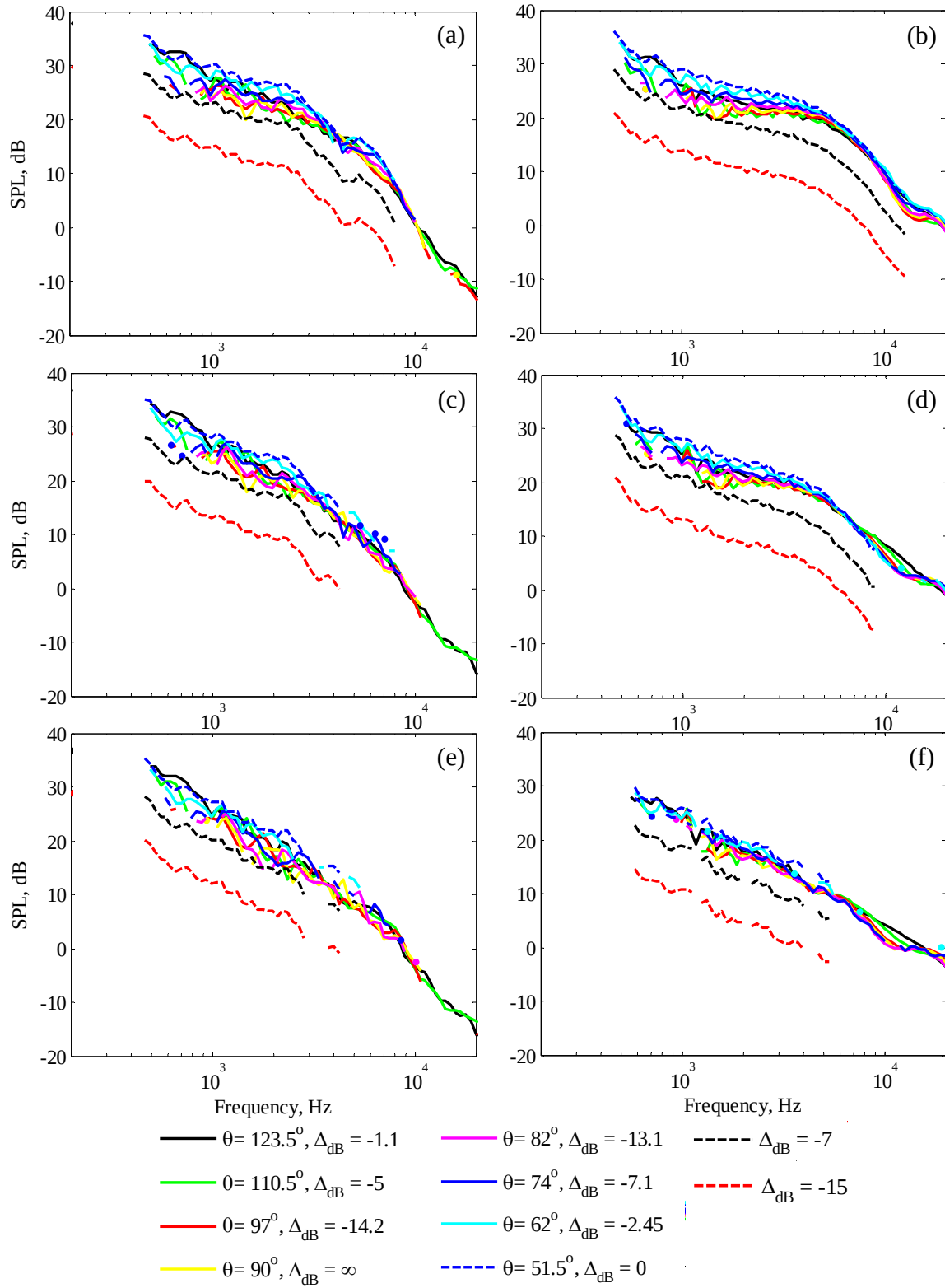


Figure 2-58 Comparison of directivities between type II gaps and forward step configuration at $U_m = 21.3$ m/s. Rounded gaps: (a) $r_2/h = 6.25\%$, (c) $r_2/h = 12.5\%$ and (e) $r_2/h = 25\%$. Forward steps : (b) $r/h = 6.25\%$, (d) $r/h = 12.5\%$ and (f) $r/h = 25\%$.

comparison. As seen in the figure the sound variation at lower frequencies for the gap configuration follows the same trend as that for a forward step alone configuration with same rounding except the gap spectra have more noise for higher rounding ($r_2/h = 12.5\%$ and 25%) than the forward step of same rounding. At higher frequencies the gap spectra display a steeper slope and lower SNR (evident by absence of spectral data points) compared to the forward step with same rounding. Furthermore, the spectral dip associated with destructive interference in forward step noise is absent or is less pronounced in gap noise. In §2.2.5.2 we had seen that for an unrounded gap with $w = 4h$ the sound at higher frequencies was suppressed and the spectra had a jagged appearance compared to an unrounded forward step. This difference in high frequency behavior was attributed to the forward step portion of the gap not receiving a fully developed attached flow. So, presumably, as the forward step portion of the gap is rounded the overall sound being produced is reduced similar to what was seen for a forward step alone configuration. This further sound reduction with increasing rounding is then the likely cause of low SNR and absence of the spectral dip at higher frequencies.

2.2.6 Far Field Sound from Rounded Swept Forward Facing Steps

We now turn our attention to the far field sound from discontinuities which are no longer aligned perpendicular to flow i.e. when the flow is no longer two-dimensional. To this end we consider the problem of radiated sound due to turbulent boundary layer flow over swept forward facing steps. The sweep angles with respect to the perpendicular to the incoming velocity vector that were studied were $\alpha = 0^\circ, 5^\circ, 15^\circ$ and 30° . For each sweep angle four different roundings of the step corner with rounding radius to step height ratios of $r/h = 0\%, 12.5\%, 25\%$ and 50% were also considered. The measurements of far field sound were made for undisturbed edge velocities of $U_m = 21.3$ m/s, 15.6 m/s and 10.3 m/s which are the same three velocities at which measurements were made for two-dimensional discontinuities. The adjustable sweep step apparatus was positioned so that when unswept it was located at the same location on the wall-jet test surface (1372 mm downstream of the nozzle exit) as the fixed forward step configuration. Thus the undisturbed boundary layer characteristics described in §2.1.1.1 are also applicable here. Recall from §2.1.6.2 that the sound from swept steps was measured using two sets of microphones, the first which will be referred to as fixed microphones are the same 8 microphones as used for two-dimensional steps and they are fixed with respect to the incoming velocity vector. The second set of microphone consists of two microphones which will be referred to as moving microphones move with each sweep of the step and thus are local to the swept step coordinate system. All results presented in this section will follow the convention highlighted in §2.2.2.

2.2.6.1 Sound from Unswept forward facing step

As highlighted in §2.1.5 a foreshortened streamwise geometry was used with the adjustable sweep setup. This geometry consisted of a forward step whose upper surface was about 12 step height long in the streamwise direction followed by a curved transition ramp at the trailing edge. The impact of using such a finite streamwise geometry on the far field sound was quantified by varying the length of the upper surface and comparing the radiated sound from the fixed two-dimensional step geometry which had an upper surface length of approximately 100 step heights. Two different lengths of the flat upper surface equal to 8 and 12 step heights were considered for this study as shown in Figure 2-59 below and the sound was measured by the same microphones used for two-dimensional forward steps.

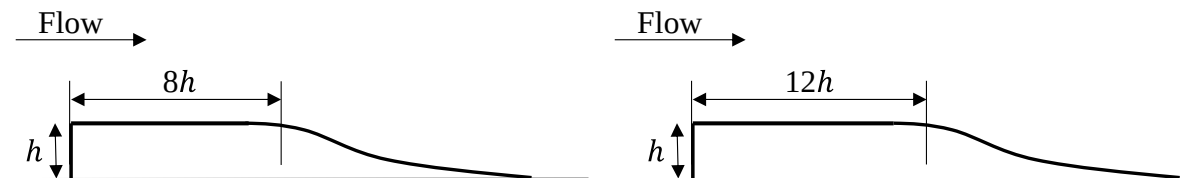


Figure 2-59 Configurations used to determine the effect of finite streamwise extent of swept step assembly on far field sound.

Figure 2-60 shows the background subtracted far field sound measured at four observer locations for a $r/h = 0\%$ unswept step with three different lengths of the flat upper surface. The spectra shown are all for $U_m = 21.3$ m/s. It can be seen from the figure that for each observer location there is a slight discrepancy between the spectra for 8 step height test case and the two-dimensional forward step at frequencies below about 2 kHz. However, increasing the length of the upper surface to 12 step heights before the flow is ramped down removes this discrepancy and the sound at each observer location is essentially the same as that for the step whose upper surface is 100 step heights long.

The test for the finite streamwise geometry was also done for two rounded steps with corner rounding of $r/h = 12.5\%$ and 25% . The sound spectra at two observer locations for each of these steps with the three different lengths of upper surface have been shown in Figure 2-61. Looking at Figure 2-61 (a) and (b) which shows the spectra for $r/h = 12.5\%$ step at observer locations of $\theta = 110.5^\circ$ and $\theta = 90^\circ$ respectively it can be seen that the same low frequency discrepancy as seen for an unrounded step in Figure 2-60 exists here for the 8 step height case. However, the magnitude by which the 8 step height exceeds the 100 step height case is slightly lower than what was seen $r/h = 0\%$ step in Figure 2-60. This is likely due to the shrinking size of the downstream separation bubble with increasing rounding. As seen for $r/h = 0\%$ increasing the length of the flat upper surface to 12 step heights removes this discrepancy. For $r/h = 25\%$ (Figure 2-61 (c) and (d)) the SNR becomes poor as less sound is being produced by this rounded step and the sound from the three test cases appears to be nearly identical. The results of Figure 2-60 and Figure 2-61 show that the swept step assembly with 12 step height long flat surface before the flow is smoothly ramped down to the wall-jet test plate is appropriate to simulate sound from a forward step.

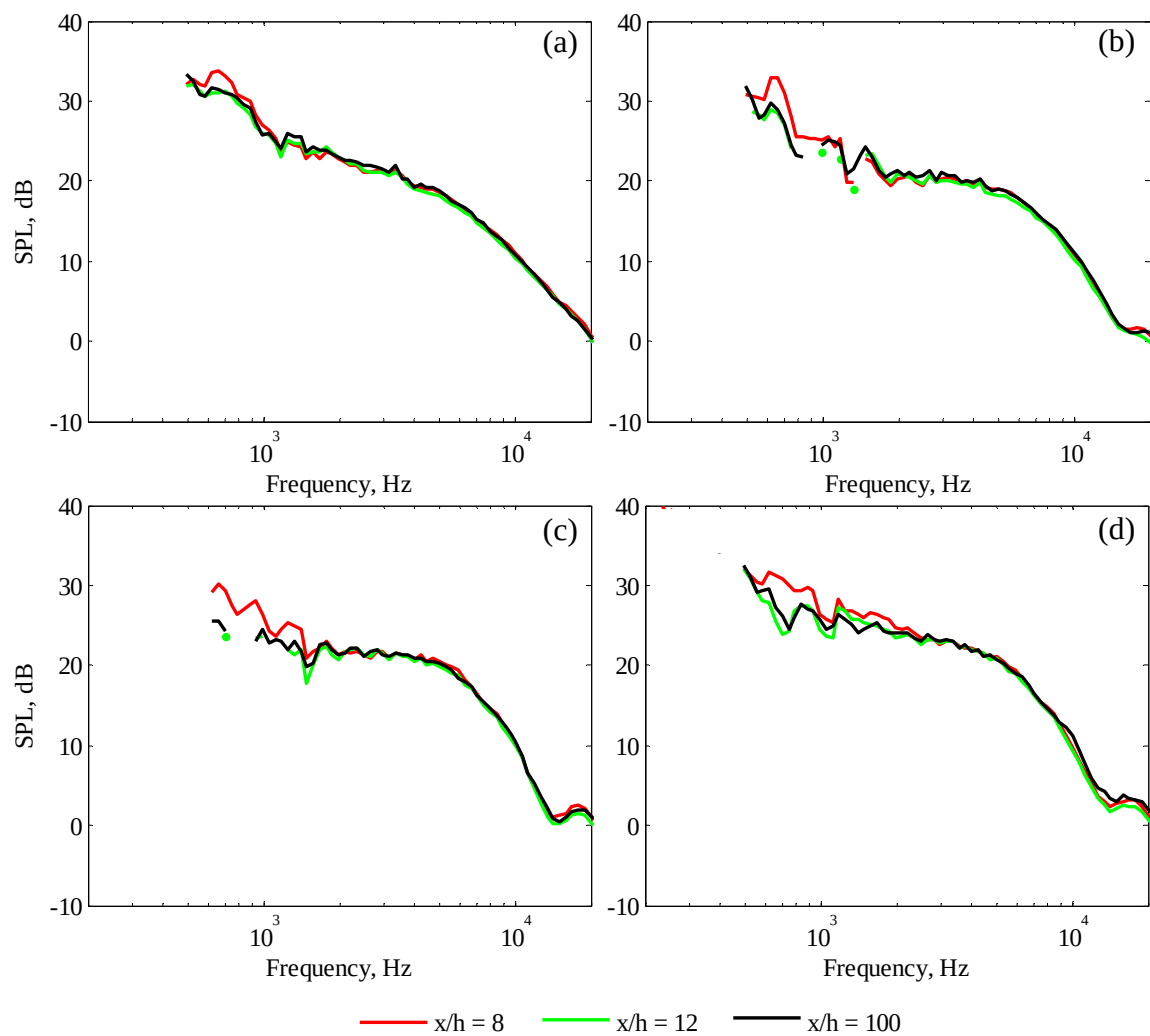


Figure 2-60 Effect of finite forward step geometry on far field sound, $U_m = 21.3$ m/s, $r/h = 0\%$. (a) $\theta = 123.5^\circ$, (b) $\theta = 110.5^\circ$, (c) $\theta = 90^\circ$ and (d) $\theta = 74^\circ$.

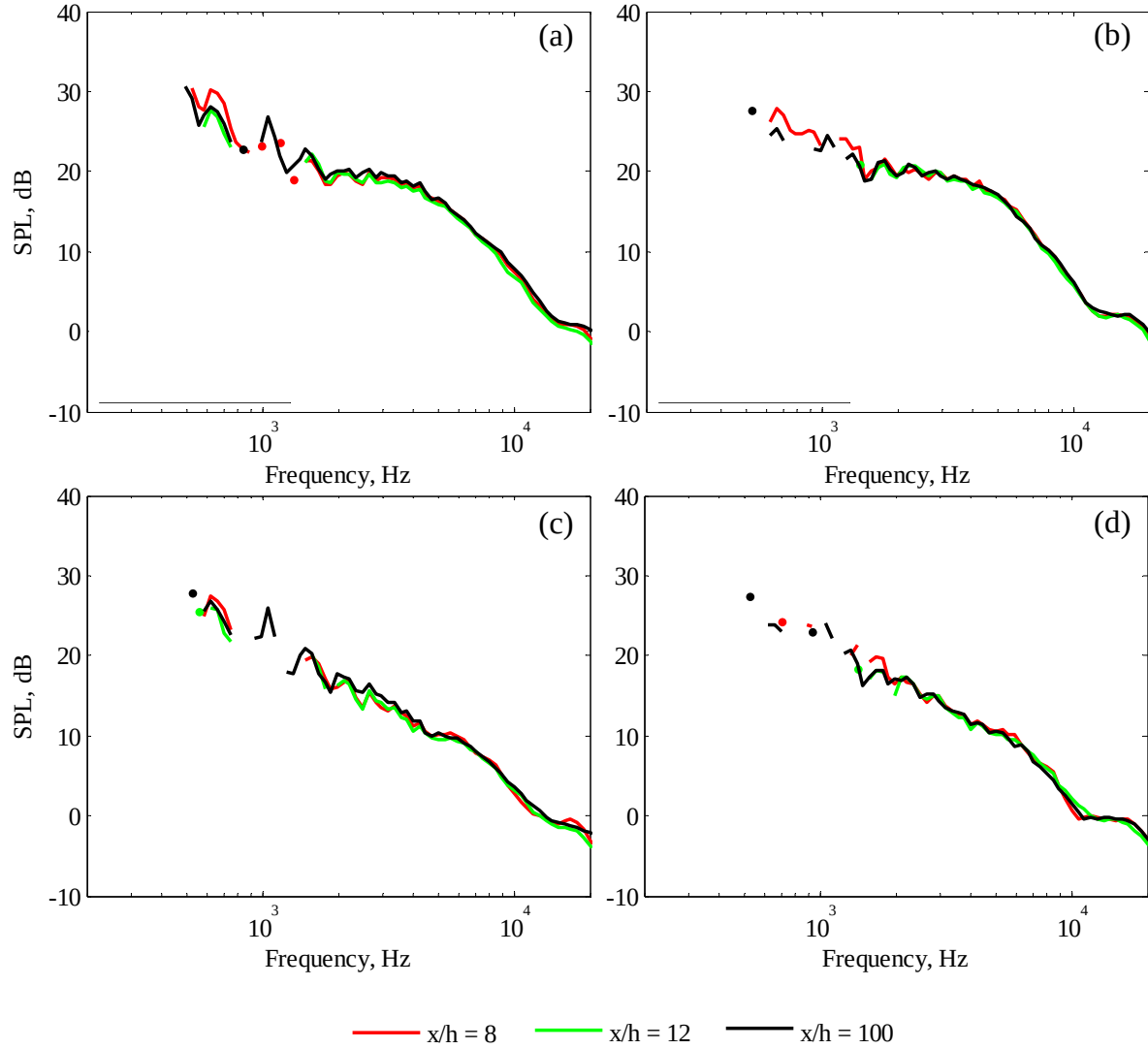


Figure 2-61 Effect of finite forward step geometry on far field sound, $U_m = 21.3$ m/s, (a) $r/h = 12.5\%$, $\theta = 110.5^\circ$, (b) $r/h = 12.5\%$, $\theta = 90^\circ$, (c) $r/h = 25\%$, $\theta = 110.5^\circ$ and (d) $r/h = 25\%$, $\theta = 90^\circ$.

2.2.6.2 Sound from Flow Outside the Two-Dimensional Core of Wall-Jet Flow

We have already established earlier in §2.2.3 that the three-dimensional shear layer outside the two-dimensional core of the wall-jet facility had little influence on the far field sound in case of a two-dimensional forward step geometry. However, for the swept step it was necessary to perform this test again since in §2.1.5 it was shown that as the sweep angle of the forward step increases the outer edges of the step assembly (in the spanwise direction) move inwards and lie on the wall-jet test surface. Figure 2-62 shows this situation. It can be seen that sweeping the step assembly results in its outer edges getting exposed to the oncoming flow. To assess the effects of these exposed edges the highest sweep angle of $\alpha = 30^\circ$ was chosen. Far field sound measurements were made by first leaving the spanwise edges of the geometry exposed and then the measurements were repeated after smoothly fairing the edges to the wall-jet test plate using Mylar. The measurements were made for three flow velocities of $U_m = 21.3$ m/s, 15.6 m/s and 10.3 m/s. Figure 2-63 shows the background subtracted far field sound spectra for the two test cases at four separate observer locations for all three velocities. Looking at the figure it can be seen that leaving the edges exposed or covering them with Mylar makes no difference to the radiated sound at any observer location or for any flow velocity. This shows that the exposed edges had no effect on the radiated sound for swept

step with $\alpha = 30^\circ$. Since for lower sweep angles the exposed edges would move outward i.e. away from the flow region this would also be true for sweep angles of $\alpha = 5^\circ$ and 15° . Despite of the exposed edges making no difference to the radiated sound all measurements from swept forward steps were made with the spanwise edges of the step geometry covered with Mylar.

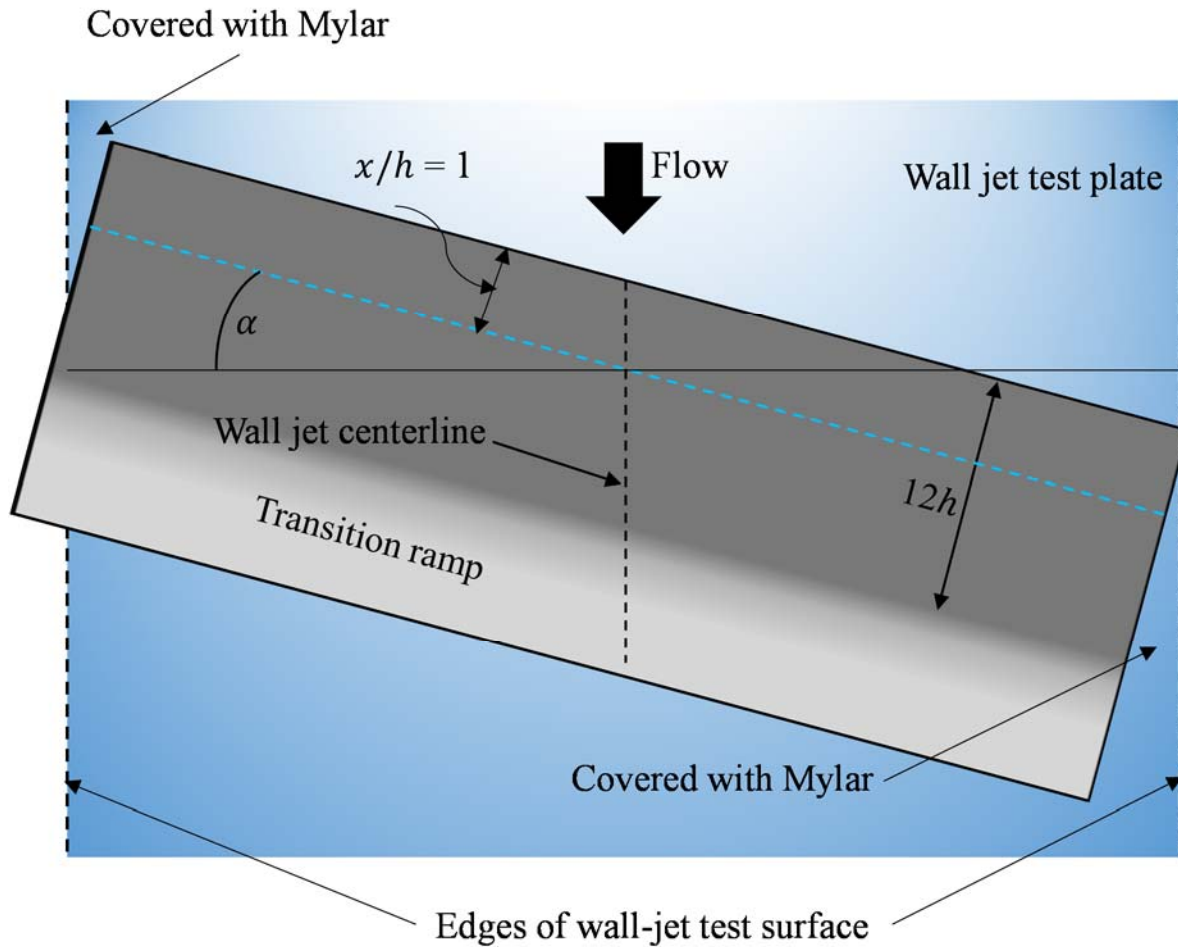


Figure 2-62 Exposed spanwise edges of swept step assembly.

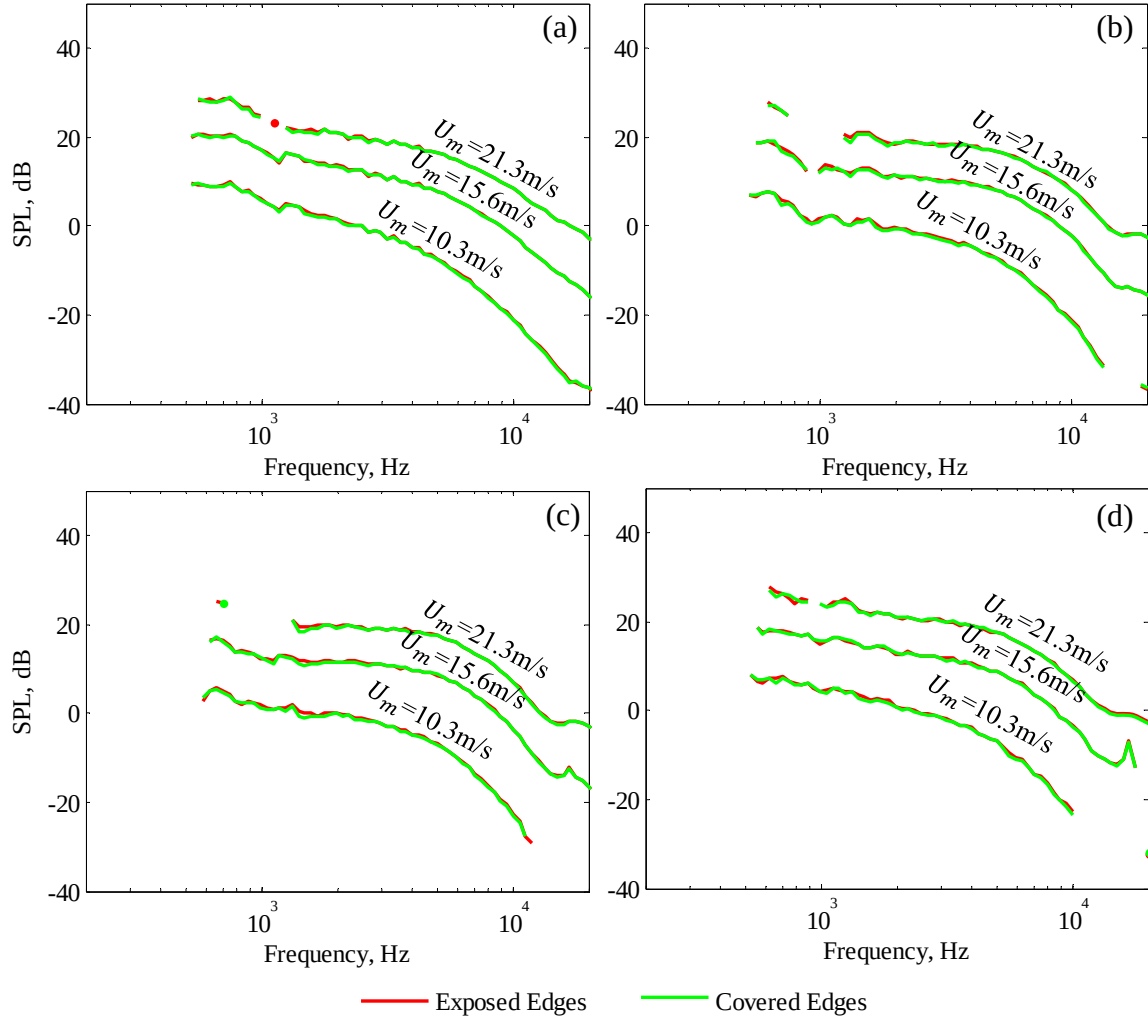


Figure 2-63 Effect of the flow outside the two-dimensional core of the wall-jet on sound from swept forward step geometry with $\alpha = 30^\circ$, $r/h = 0\%$. (a) $\theta = 123.5^\circ$, (b) $\theta = 110.5^\circ$, (c) $\theta = 90^\circ$ and (d) $\theta = 74^\circ$.

2.2.6.1 Equivalency of Sound Measurements made with the Moving and Fixed Microphones

The far field sound from the swept steps was measured using a fixed microphone arc and two moving microphones (Figure 2-18). At zero sweep two of the microphones in the fixed arc ($\theta = 123.5^\circ$ and 90°) are at positions equivalent to those of the two moving microphones. For the fixed and moving microphones whose line of sight is perpendicular to the plane of the vertical face of the step ($\theta = 123.5^\circ$ and $\theta' = 123.5^\circ$) the only difference is that they are located at a different spanwise station, about 11 step heights apart from each other. Let us call these microphones *perpendicular microphones*. For the second set of equivalent microphones ($\theta = 90^\circ$ and $\theta' = 90^\circ$) both the microphones are in the plane of the step face. However, their orientation within that plane is slightly different. Let us call these microphones *parallel microphones*. The line of sight of the microphone in the fixed arc ($\theta = 90^\circ$) is normal to the wall-jet test plate. On the other hand the line of sight of the microphone in the moving arc ($\theta' = 90^\circ$) is at an angle ($\psi = 56.5^\circ$, see Figure 2-18) to the wall-jet test plate. With these differences in mind we first compare the sound recorded by these equivalent microphones.

Figure 2-64 (a) and (b) show comparison of sound measured in the unrounded unswept step case by the perpendicular and parallel microphone pairs respectively. We see that for most frequencies the response of the two microphones in each pair is the same, as expected. The slight discrepancy (about 2dB)

at the lowest frequencies in Figure 2-64 (a) for the perpendicular microphones is likely due to high uncertainty associated with poor SNR here. The SNR of the moving microphone ($\theta' = 123.5^\circ$) is slightly lower than the fixed microphone (evident by the missing spectral values at low frequencies) at these low frequencies. The likely source of this is better acoustic shielding of the fixed microphone from the facility noise by the vertical acoustic baffle (see Figure 2-1 for the location of vertical baffle). In case of the parallel microphone pair (Figure 2-64 (b)) the largest discrepancy occurs at the highest frequencies and keeping in mind the difference in line of sight of these microphones is believed to be due to difference in directional response of the microphones. For $r/h = 12.5\%$ (Figure 2-64 (c) and (d)) both the moving and fixed arc microphones show good agreement with each other.

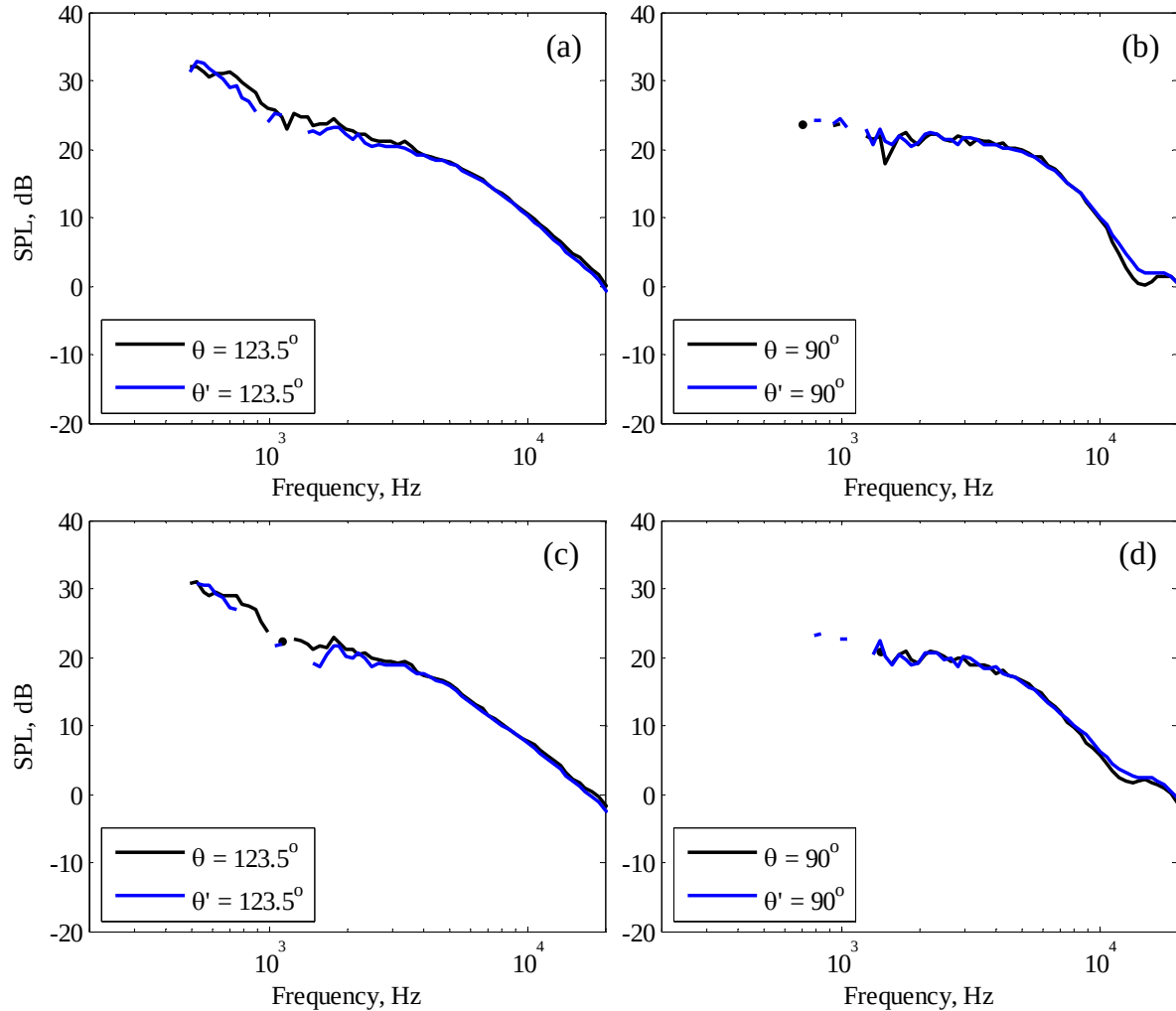


Figure 2-64 Comparison between measured sound from moving and fixed microphones for an unswept forward step with and without corner rounding at $U_m = 21.3$ m/s. (a) and (b) show comparison for $r/h = 0\%$ step and (c) and (d) show comparison for $r/h = 12.5\%$ step.

2.2.6.2 Effect of Sweeping an Unrounded Forward Facing Step on Far Field Sound

Let us now look at the effect of sweeping an unrounded forward step on far field sound. Figure 2-65 (a) and (b) show the background subtracted sound spectra as a function of sweep angle measured by microphones at $\theta = 123.5^\circ$ and $\theta' = 123.5^\circ$ respectively. The spectra shown are all measured at flow velocity of $U_m = 21.3$ m/s. It should be noted that since the location of the moving microphones with respect to flow

fixed coordinates changed with every sweep angle, the background sound measurements for these microphones were made after each swept step measurements by removing the assembly from the wall-jet test surface. Looking at the figure now the first observation that can be made is that sweeping the step does not affect the character of the sound spectrum. For sweep angle of $\alpha = 5^\circ$ the spectrum is indistinguishable from an unswept step. As the step is swept to $\alpha = 15^\circ$ the entire spectrum shifts down by about 1 dB. Doubling the sweep angle to $\alpha = 30^\circ$ the sound attenuation further increases to about 2-3 dB without affecting the shape of the spectrum. If we now look at the sound measured by the moving microphone at $\theta' = 123.5^\circ$ in Figure 2-65 (b) we see the same qualitative effects of sweeping the step. Quantitatively the sound measured by this microphone shows slightly larger attenuation for the highest sweep angle. This can be understood by remembering that the wall-jet flow has a strong inhomogeneity along the streamwise direction because the flow properties are a strong function of distance from the nozzle exit. The way the sweeping of forward step is performed in the present work leads to one half of the step span moving closer to the nozzle while the other half moves away from it. This causes a significant spanwise inhomogeneity especially for higher sweep angles. As will be seen shortly this spanwise inhomogeneity combined with the changing source to observer distance is responsible for the quantitative differences seen in Figure 2-65.

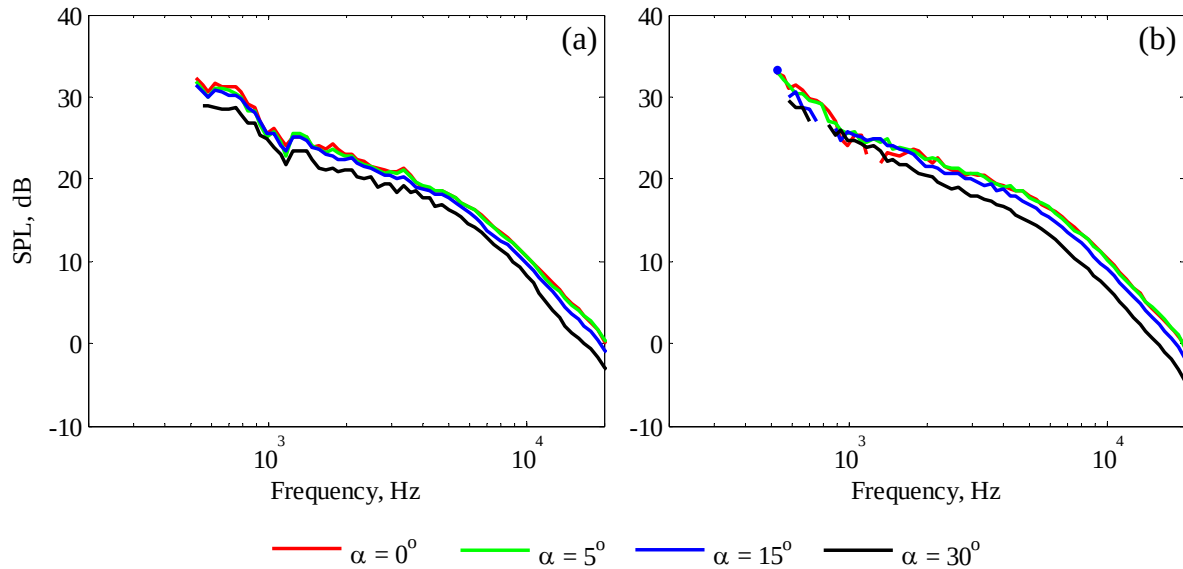


Figure 2-65 Effect of sweeping an unrounded ($r/h = 0\%$) step on far field measured by fixed and moving microphones, $U_m = 21.3\text{m/s}$. (a) $\theta = 123.5^\circ$ and (b) $\theta' = 123.5^\circ$.

If we now look at the sound measured by the fixed microphone directly above an unswept step ($\theta = 90^\circ$, Figure 2-66 (a)) we see again that the sweeping of the step only causes quantitative changes. The spectra for each sweep angle exhibits the same hump like structure at mid-frequencies and spectral trough at high frequencies due to destructive interference, as seen in an unswept step. The quantitative changes due to sweep here only become significant for the highest sweep angle of $\alpha = 30^\circ$. The story on the sound measured from the moving microphone at $\theta' = 90^\circ$ (Figure 2-66 (b)) is slightly different. The qualitative effect of the sweep is the same but we see more drastic variation in sound levels with sweep. The sound at a sweep angle of 5° is indistinguishable from an unswept step but the sound for $\alpha = 15^\circ$ shows more attenuation compared to an unswept step than seen in Figure 2-66 (a). Sweeping the step further to $\alpha = 30^\circ$ leads to further reduction of sound but the SNR at higher frequencies is much lower compared to fixed arc microphone and as a result no sound is seen at frequencies above 10 kHz. As will be demonstrated, the difference in the quantitative effect of sweeping a forward step seen in Figure 2-66 (a) and (b) is again due to the spanwise inhomogeneity and the changing source to observer distance with each sweep angle.

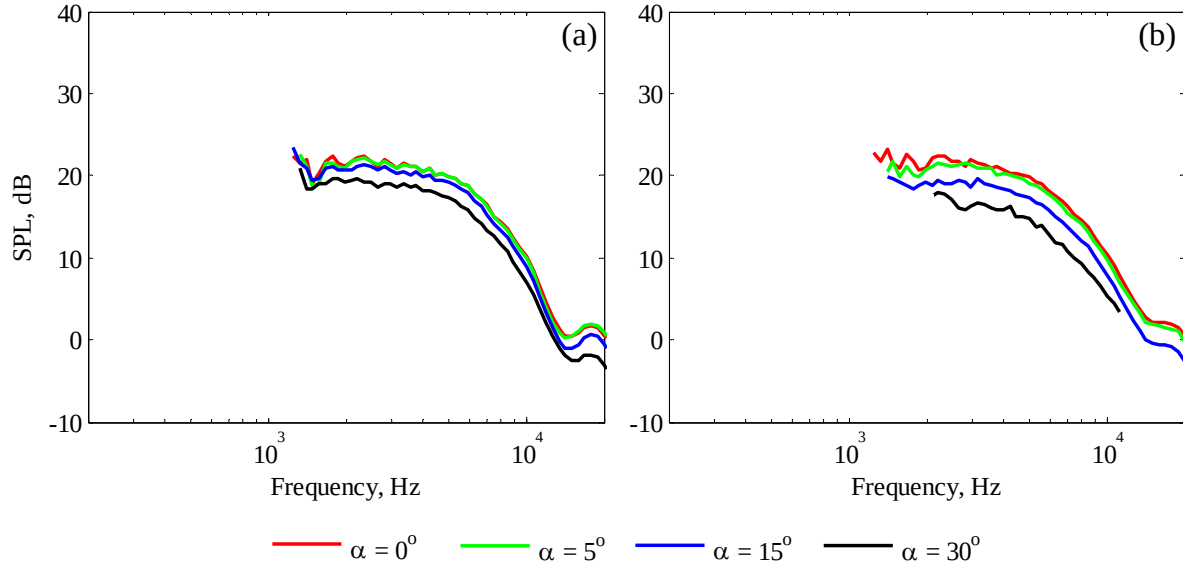


Figure 2-66 Effect of sweeping an unrounded ($r/h = 0\%$) step on far field measured by fixed and moving microphones, $U_m = 21.3$ m/s. (a) $\theta = 90^\circ$ and (b) $\theta' = 90^\circ$.

Figure 2-67 (a)-(d) shows the effect of sweeping an unrounded forward step on the far field sound at four different observer locations within the fixed microphone arc for a flow velocity of $U_m = 21.3$ m/s. The effect of sweep is nearly the same at each of these observer locations in that the shape of the spectra remains the same with levels dropping with increasing sweep angle. For the most downstream observer location (Figure 2-67 (d)) there is no sound at higher frequencies and higher sweep angles of $\alpha = 15^\circ$ and 30° due to poor SNR.

Figure 2-68 (a)-(d) shows the effect of sweeping the forward step on far field sound at four different observer locations for two additional velocities at which measurements were made. The figure shows spectra for the moving and fixed microphones which are equivalent. Looking at the figure it can be seen that for each observer location the effect of the sweep on far field sound is same for these lower velocities as well. The spectra at $\theta = 123.5^\circ$ and $\theta' = 123.5^\circ$ for both velocities (Figure 2-68 (a) and (c)) show similar effect of sweep angle except for $\alpha = 30^\circ$ for which the attenuation of sound recorded by the moving microphone is reemphasized by the spanwise inhomogeneity of the flow. The spectra at $\theta = 90^\circ$ and $\theta' = 90^\circ$ (Figure 2-68 (b) and (d)) show similar effect of sweeps to those seen for the higher velocity of $U_m = 21.3$ m/s in Figure 2-66.

Figure 2-69 (a)-(d) shows the spectra for swept forward steps scaled on the step-normal component of the undisturbed edge velocity. Note that the scaling for $\alpha = 0^\circ$ shown in Figure 2-69 (a) is the mixed scaling proposed by Catlett *et al.* (2014) (since $U_n = U_m$) that was already shown in §2.2.4. Looking at Figure 2-69 it can be seen that scaling the spectral levels on the 5th power of step-normal velocity and the frequency levels on boundary layer thickness and step-normal velocity collapses the data for all sweep angles considered in the present work. At lowest frequencies there is a slight scatter in the data (~ 3 dB) which was also present in unswept step case in §2.2.4 and is likely due to poor SNR. Note that this scaling does not collapse the data (not shown) where the non-compactness effects are dominant which is similar to the observation made in §2.2.4 for unswept forward steps.

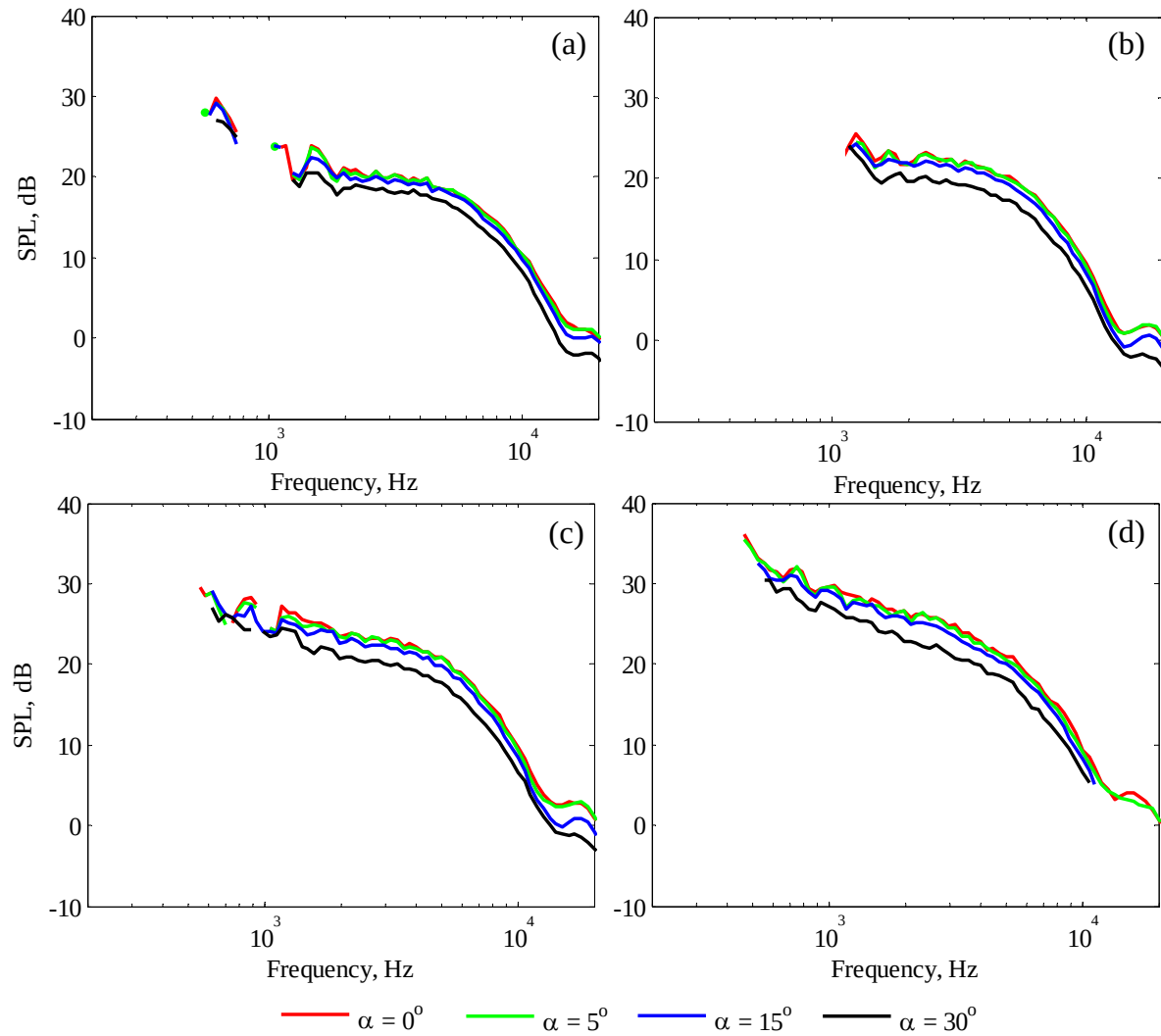


Figure 2-67 Effect of sweeping an unrounded ($r/h = 0\%$) forward step on far field sound at four separate observer locations in the fixed microphone arc, $U_m = 21.3$ m/s. (a) $\theta = 110.5^\circ$, (b) $\theta = 82^\circ$, (c) $\theta = 74^\circ$ and (d) $\theta = 51.5^\circ$.

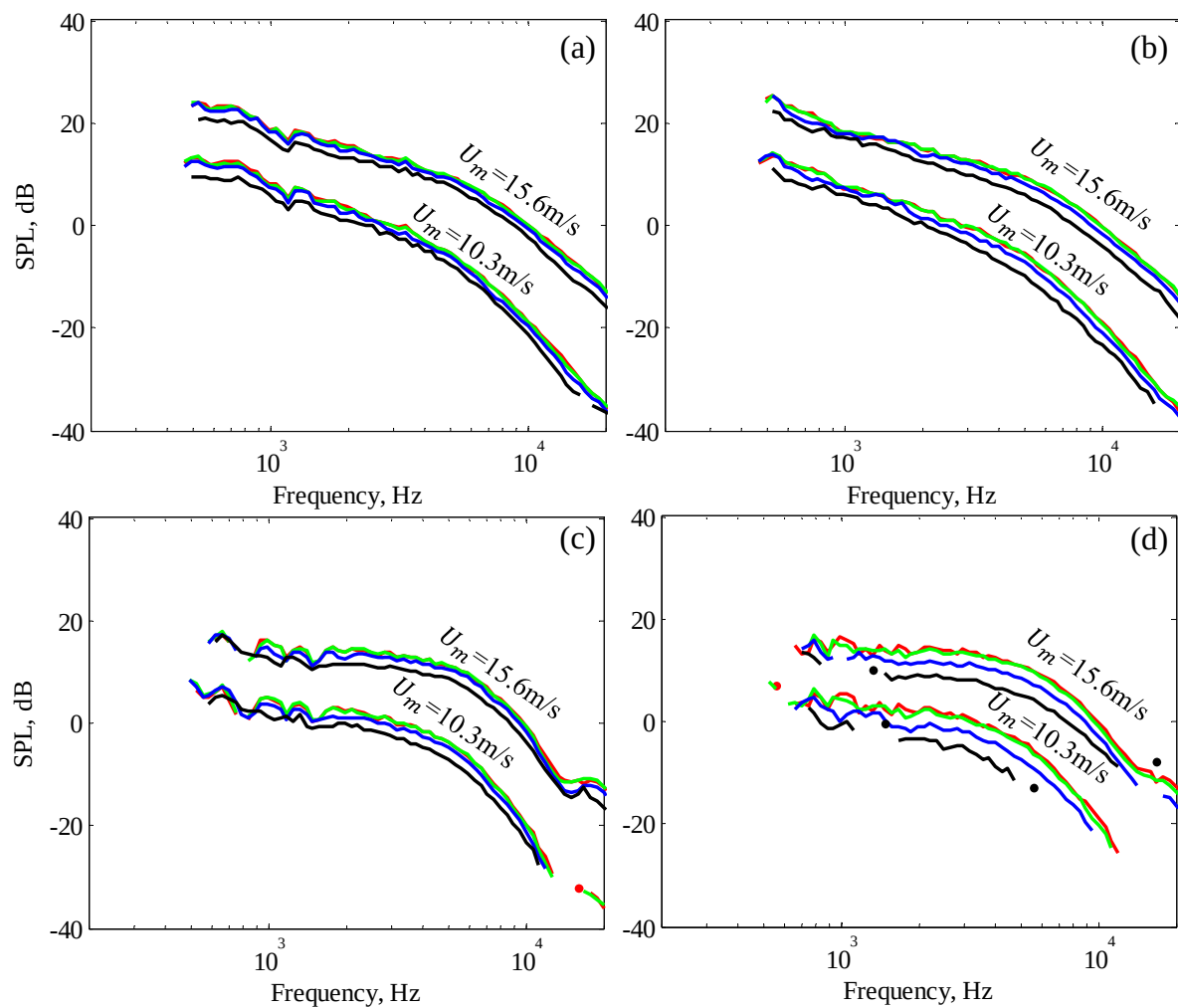


Figure 2-68 Effect of sweeping an unrounded forward step on far field sound at two different edge velocities. (a) $\theta = 123.5^\circ$, (b) $\theta' = 123.5^\circ$, (c) $\theta = 90^\circ$ and (d) $\theta' = 90^\circ$.

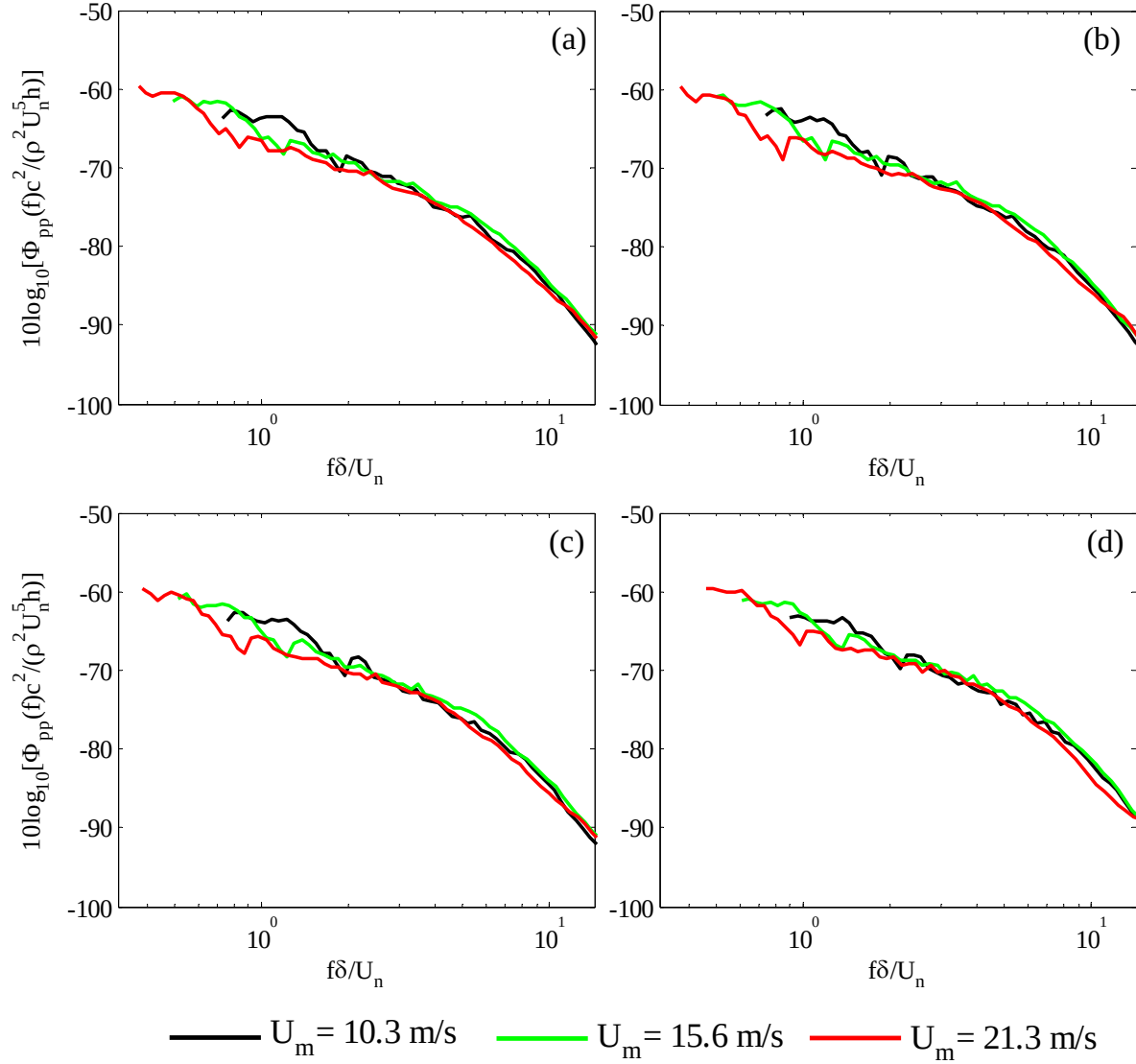


Figure 2-69 Velocity scaling of sound spectra for swept forward steps measured at $\theta = 123.5^\circ$. (a) $\alpha = 0^\circ$, (b) $\alpha = 5^\circ$, (c) $\alpha = 15^\circ$ and (d) $\alpha = 30^\circ$.

2.2.6.3 Scaling of Far Field Sound from Swept Forward Steps: Sweep Independence Principle

The results discussed in §2.2.6.2 reveal that sweep has no qualitative effect on the far field sound and the sound drops by some constant factor throughout the frequency range with increasing sweep angle. This suggests that the far field sound would likely scale on some common variable which is only a function of the sweep angle. This variable as suggested by Hao and Wang (2013) is the step-normal component of the free stream velocity. Hao and Wang (2013) performed LES simulations on swept forward steps with sweep angles of $\alpha=0^\circ$, 15° , 30° and 45° and then used Lighthill's theory to solve for the far field sound. Their results show that the far field sound for sweep angles up to 30° scales on the normal component of the free stream velocity. They have termed this result as the *sweep independence principle*. So we take the results from far field sound measurements on unrounded swept forward steps shown in §2.2.6.2 and put this sweep independence hypothesis to test.

Before we proceed however it should be kept in mind that there are some key differences between the simulations and the present experiments (besides the obvious difference in the flow parameters). The

simulations assume a spanwise homogeneous flow along the step and an observer which is located at the same location in swept step coordinates. However, wall-jet flow properties have a strong streamwise gradient which leads to an inhomogeneous flow across the step span in the present work. Furthermore, while the two moving microphones stay at the same location in the swept step coordinates the location of the tunnel fixed microphone arc changes with every sweep angle. With these key differences in mind we now proceed to application of the sweep independence principle.

During our discussion on the velocity scaling of the far field sound from two-dimensional forward steps and gaps we had introduced a scaling of spectral levels based on step height and 5th power of undisturbed edge velocity (see §2.2.4). This scaling was based on Cole's (1980) dimensional analysis of Curle's (1955) theorem of far field sound from turbulent flow in presence of rigid boundaries and the scaled spectral density levels in this scaling are given by

$$\Phi_{pps}(f) = \frac{\Phi_{pp}(f)c^2}{\rho^2 U_m^5 h} \quad (2-32)$$

And for the frequency axis, scaling was based on Catlett *et al.*'s (2014) mixed scaling which involves undisturbed boundary layer thickness at the step location and the undisturbed edge velocity. This scaling is given by

$$f_s = \frac{f\delta}{U_m} \quad (2-33)$$

In order to apply the sweep independence principle we can simply replace the undisturbed edge velocity U_m in equations (2-32) and (2-33) by the step-normal component of the edge velocity for each sweep angle. This step normal component is given by

$$U_n = U_m \cos(\alpha) \quad (2-34)$$

Additionally we take into account the source to observer distance of each microphone by applying the square of the ratio R''/h to the spectra scaling. Here R'' is the shortest distance from the inside corner of the swept steps to the microphone (see Figure 2-18 in §2.1.6.2 for description of the radial and angular positions of both fixed and moving microphones). Then the scaling of the spectral levels based on the step-normal component of the velocity is given by

$$\Phi_{pp}'(f) = \frac{\Phi_{pp}(f)c^2 \left(\frac{R''}{h}\right)^2}{\rho^2 U_n^5 h} \quad (2-35)$$

And the scaling for frequency is given by

$$f_s = \frac{f\delta}{U_n} \quad (2-36)$$

Note that the distance R'' in equation (2-35) remains constant for the two sweep local microphones. For the moving microphone perpendicular to the vertical face of swept step this distance is $R'' = R' = 578$ mm while for the moving microphone in the plane of vertical face this distance is $R'' = R' \sin(\psi)$, where $\psi = 56.5^\circ$. The angular position of these two microphones also remains unchanged as the step is swept. On the other hand for the 8 microphone arc fixed with respect to the incoming velocity vector the distance R'' and the angular positions change with each sweep angle. This change in the radial and angular coordinates for a tunnel fixed microphone located upstream of the step is illustrated in Figure 2-70. This figure shows the

location of the swept and the unswept step which are represented as lines coincident with the inside corners of the two steps. From the geometry of the figure the shortest distance between the inside corner of the swept step and the center of the microphone is given by

$$R'' = \sqrt{[R\sin(\theta)]^2 + \{[-R\cos(\theta) - h(\sec(\alpha) - 1)]\cos(\alpha)\}^2} \quad (2-37)$$

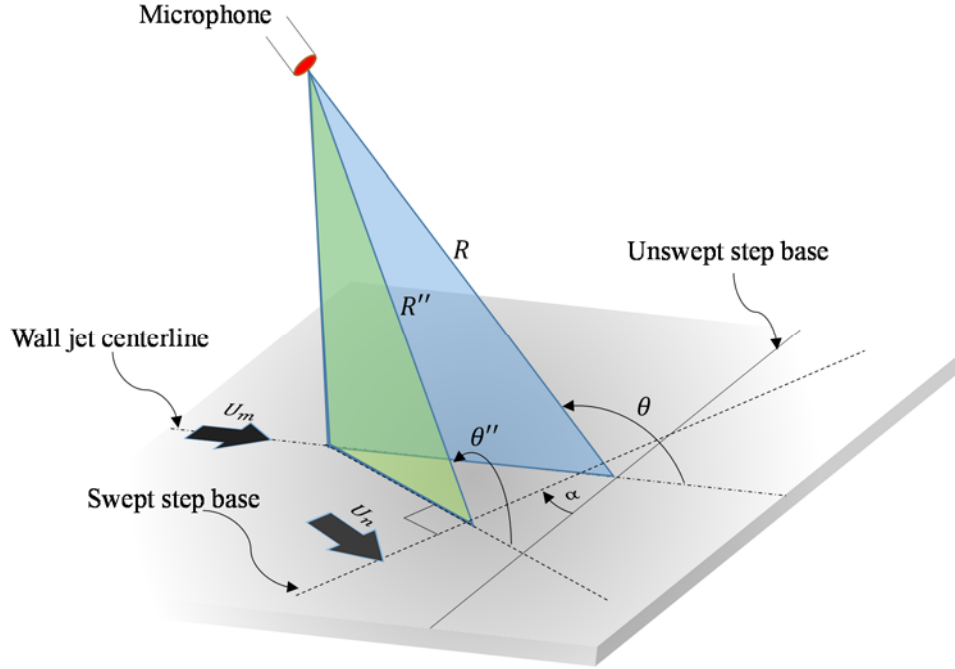


Figure 2-70 Illustration of changing source to observer distance for the microphones in the fixed arc with increasing sweep angle.

And the angular position is given by

$$\theta'' = \begin{cases} 180^\circ - \sin^{-1} \left[\frac{R\sin(\theta)}{R''} \right] & \text{for } \theta \geq 90^\circ \\ \sin^{-1} \left[\frac{R\sin(\theta)}{R''} \right] & \text{for } \theta < 90^\circ \end{cases} \quad (2-38)$$

Table 2-8 shows the corrected radial distance and angular positions of all 8 microphones in the fixed microphone arc for all four sweep angles. Looking at the table it can be seen that as the sweep angle increases the radial distance of each of the microphone becomes smaller and the angular position of the microphone also changes. We can now use the radial coordinates shown in the table below and insert the correct distance R'' in equation (2-35) to account for the changing source to observer distance with sweep angle. Figure 2-71 (a) and (b) shows the spectra measured at $\theta = 123.5^\circ$ and $\theta' = 123.5^\circ$ respectively for $U_m = 21.3$ m/s scaled on the sweep independence principle. These plots show that the spectra for $\alpha \leq 15^\circ$ show a good collapse but the scaled spectrum for $\alpha = 30^\circ$ is higher for both the observer location with the lack of collapse for the highest sweep angle being slightly worse for the fixed microphone ($\theta = 123.5^\circ$). The reason for this is that the scaling used in Figure 2-71 does not account for the inhomogeneity which exists in the wall-jet flow across the swept step span. This inhomogeneity becomes stronger as the sweep

angle increase and can result in higher apparent values of mean square pressure recorded by the microphones. These effects can, to some extent, be accounted for using a simple source distribution model which will be now be discussed.

Table 2-8 Corrected radial and angular positions of microphones in fixed arc

R'' in mm				θ'' in degrees			
$\alpha=0^\circ$	$\alpha=5^\circ$	$\alpha=15^\circ$	$\alpha=30^\circ$	$\alpha=0^\circ$	$\alpha=5^\circ$	$\alpha=15^\circ$	$\alpha=30^\circ$
578	577.3	571.84	554.71	123.5	123.4	122.56	119.67
578	577.7	575.5	568.6	110.5	110.4	109.8	107.8
578	578.0	577.6	576.6	97.5	97.5	97.2	96.3
578	578.0	578.0	578.0	90	90.0	90.0	90.2
578	578.0	577.7	576.8	82	82.0	82.2	82.9
578	577.9	576.6	572.9	74	74.1	74.5	75.9
578	577.5	573.9	562.6	62	62.1	62.8	65.1
578	577.2	570.7	550.3	51.5	51.6	52.4	55.3

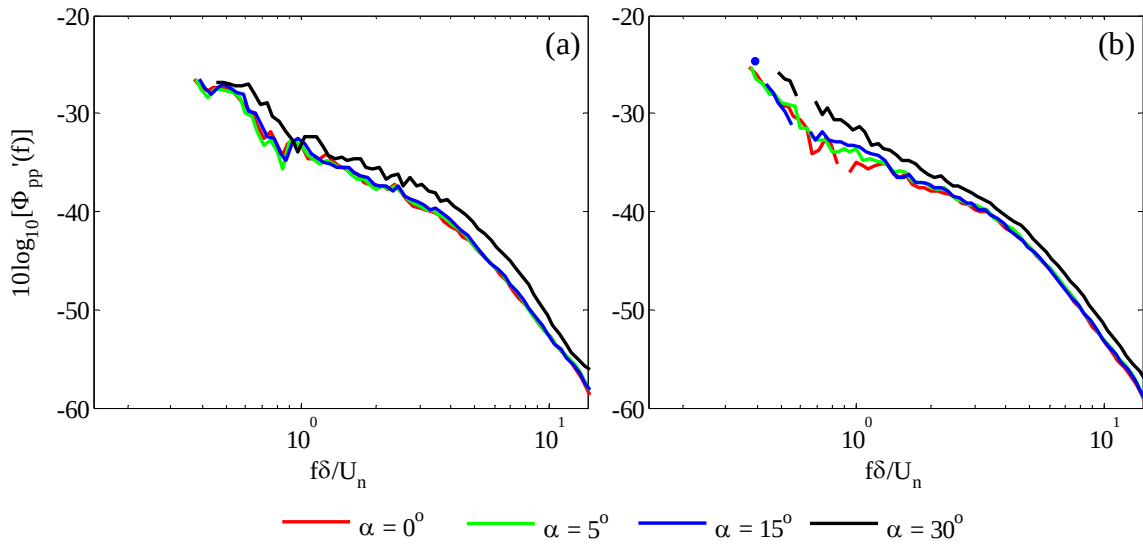


Figure 2-71 Scaling of far field spectra based on the step-normal component of the velocity, $U_m = 21.3$ m/s. (a) $\theta = 123.5^\circ$, (b) $\theta' = 123.5^\circ$.

2.2.6.4 Application of Sweep Independence Principle: Effects of Spanwise Inhomogeneity

To completely assess independence principle hypothesis we have to account for the spanwise inhomogeneity in the flow. There are two ways in which the present swept step differs from that used by Hao and Wang (2013) in their calculations to demonstrate acoustic sweep independence. First, as the step is swept in the wall-jet, the immersed length of the step increases (Figure 2-72). Second, because of the streamwise evolution of the wall-jet flow, the flow becomes non-uniform across the swept step span. As shown in §2.1.1 the variation of the undisturbed wall-jet edge velocity with the distance from the nozzle x_n is given by

$$U_m(x_n) = A_u U_j Re_j^{n+1} Re_{xn}^n \quad (2-39)$$

where U_j is the velocity at the nozzle exit, Re_j is the Reynolds number based on nozzle exit velocity and nozzle height, Re_{x_n} is the Reynolds number based on nozzle exit velocity and the streamwise distance x_n and A_u and n are empirical constants. Equation (2-39) can be used to compute the distribution of the undisturbed edge velocity across the span of each of the swept forward step since the streamwise distance of each point along the span from the nozzle exit is known.

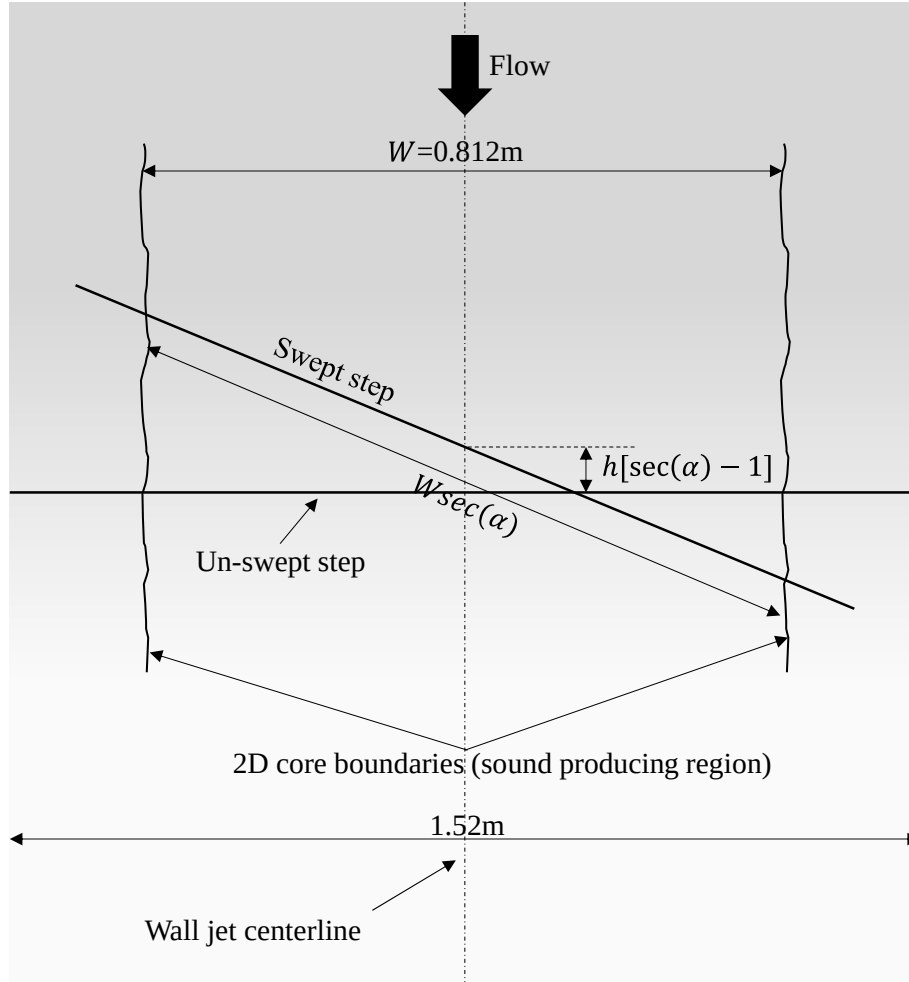


Figure 2-72 Illustration of the increase in the effective span of swept forward steps with increasing sweep angle.

Figure 2-73 (a) shows the spanwise distribution of the undisturbed edge velocity along the swept step span for nozzle exit velocity of $U_j = 60$ m/s as a function of the sweep local spanwise coordinate z' (see Figure 2-15 for definition of z'). Figure 2-73 (b) shows the effects with edge velocity normalized on its unswept value and the sweep local spanwise coordinate normalized on the half width of the two-dimensional core. As seen in the figure the velocity variation for the highest sweep angle ($\alpha = 30^\circ$) are between 7-8% compared to the unswept step configuration and the effective span length increases by about 15% of its unswept value.

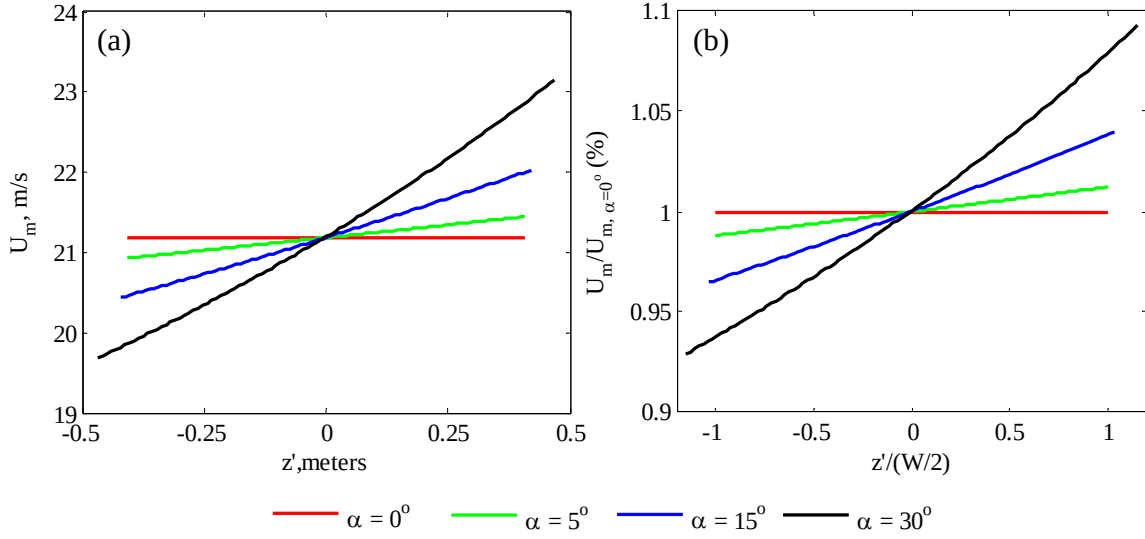


Figure 2-73 Variation of undisturbed edge velocity along the span of the swept forward steps at $U_m = 21.3$ m/s.

The effect of the spanwise inhomogeneity on the sound received by a microphone is highlighted in Figure 2-74. For the sake of illustration this figure shows a microphone in the fixed arc located upstream of the swept step location. As the sweep is increased the closest source to observer distance of the microphones which are located upstream of the unswept step location in the fixed arc lies on the half of the span which moves into the higher velocity region. Microphones downstream of the unswept step location become closer to the half of the step which moves into the lower velocity region. For the two moving microphones the source to observer distance with respect to the swept step remains the same but they are still affected by inhomogeneity due to the change in effective span length and the velocity distribution across the span.

To quantify the effects highlighted above on each microphone we utilize a source distribution model similar to the one used in §2.2.4.2. We assume a dipole point source distribution along the span of the each swept forward step as shown in Figure 2-74. Then for a source located at a distance z' along the sweep local spanwise coordinate which is at a distance r' from the microphone location the mean-square acoustic pressure experienced by the microphone is given by

$$dI(r') \sim \frac{Q(z')}{r'^2} \quad (2-40)$$

where $Q(z')$ is the acoustic source strength which can be assumed to vary as the step-normal component of the edge velocity raised to the 5th power, $U_n^5(z')$ since we have already seen that this 5th power scaling of spectral levels collapses the data for both the unswept and swept forward steps. We assume that the relationship in equation (2-40) is independent of frequency and the quantities on the two sides are related merely by a constant. Integrating over the immersed span W' we get

$$I(\alpha) \sim \int_{-W'/2}^{W'/2} \frac{U_m^5(z')}{r'^2} dz' \quad (2-41)$$

The source to observer distance r' (Figure 2-74) for microphones in the fixed arc is

$$r'^2 = R'^2 + \{z' - R \cos(\theta) \sin(\alpha) - h[\tan(\alpha) - \sin(\alpha)]\}^2 \quad (2-42)$$

Since we will be comparing the acoustic pressure fluctuation levels between different swept steps, a ratio between a swept and an unswept step can be defined as

$$F_I(\alpha) = \frac{I(\alpha)}{I(0^\circ)} \quad (2-43)$$

This ratio we refer to as the acoustic intensity factor F_I . Note that for the unswept step the radial source to observer distance is $r' = R^2 + z'^2$ and the velocity $U_n = U_m$ is just a constant ($U_m = 21.3$ m/s for $U_j = 60$ m/s). Further note that for the two moving microphones the relationship between r' and R'' is slightly different. For the moving microphone perpendicular to the vertical face of the swept steps it is given by

$$r'^2 = R'^2 + (z' + 11h)^2 \quad (2-44)$$

And for the moving microphone in the plane of the vertical face it is given by

$$r' = R'^2 + [z' + 11h + R' \cos(56.5^\circ)]^2 \quad (2-45)$$

Integration was performed numerically for a source distribution of 10,000 sources across the swept step span and this resolution was found to be sufficient for numerical convergence of the integral. The acoustic intensity factors calculated using the source distribution model for each of the 8 fixed microphones and the two moving microphones for every sweep angle and velocity of $U_m = 21.3$ m/s are shown in Table 2-9. Looking at the table it can be seen that the factors increase with increasing sweep angle for each microphone. This is because with increasing sweep the inhomogeneity across the span increases. Also note that for a particular sweep angle the factors decrease slightly as one moves towards the microphones located downstream of the unswept step in the fixed arc. This is because the microphones which are upstream of the step become closer and closer to the higher velocity region (the half of the span which moves upstream of its unswept location) as the sweep increases. Lastly if we compare the factors between the moving and fixed microphones we see that the effect of the spanwise inhomogeneity on the moving microphones is less than that for the fixed microphones. This is the reason why in Figure 2-71 the spectra for $\theta' = 123.5^\circ$ show a better collapse when normalized on step-normal velocity than the spectra for $\theta = 123.5^\circ$.

We now take the acoustic intensity factors $F_I(\alpha)$ and apply those to the normalization of the spectra. We define, for microphones in the fixed arc:

$$\Phi_{pp}''(f, \alpha) = \frac{\Phi_{pp}(f)c^2 \left(\frac{R''}{h}\right)^2}{\rho^2 U_m^5 h F_I(\alpha)} \quad (2-46)$$

Note that the denominator scaling returns to a scaling on the total wall-jet velocity U_m since the effect of the ratio between the normal and total velocities is now accounted for in equations (2-41) and (2-43). For the moving microphone which is perpendicular to the vertical face of the swept step the normalization is given by

$$\Phi_{pp}''(f, \alpha) = \frac{\Phi_{pp}(f)c^2 \left(\frac{R'}{h}\right)^2}{\rho^2 U_m^5 h F_I(\alpha)} \quad (2-47)$$

And for the moving microphone which is in the plane of the vertical face of the step the normalization is

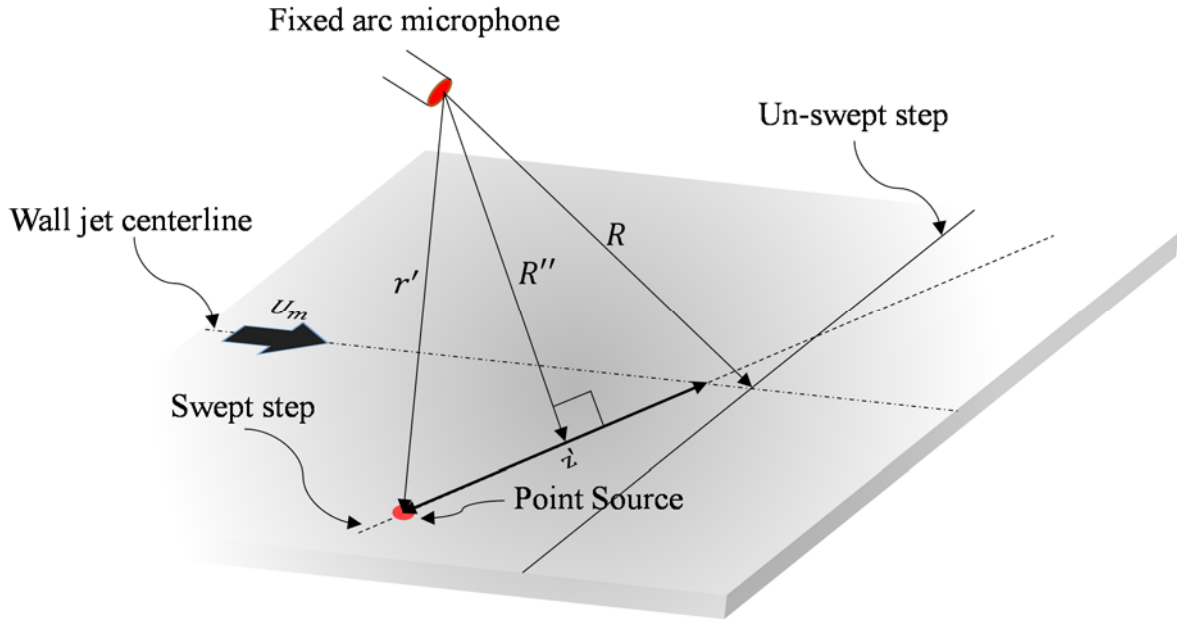


Figure 2-74 Illustration of source distribution model to account for the spanwise inhomogeneity in flow over swept forward steps.

Table 2-9 Acoustic intensity factors for each far field microphone for four sweep angles at $U_m = 21.3$ m/s.

Fixed Microphones					Moving Microphones				
θ (deg)	$\alpha = 0^\circ$	$\alpha = 5^\circ$	$\alpha = 15^\circ$	$\alpha = 30^\circ$	θ' (deg)	$\alpha = 0^\circ$	$\alpha = 5^\circ$	$\alpha = 15^\circ$	$\alpha = 30^\circ$
123.5	1	1.0057	1.0537	1.247	123.5	1	0.9992	1.0212	1.1229
110.5	1	1.0048	1.0449	1.2023	90	1	0.9926	1.0058	1.1019
97.5	1	1.0041	1.038	1.1683					
90	1	1.0038	1.0352	1.1551					
82	1	1.0036	1.0332	1.1453					
74	1	1.0035	1.0322	1.1404					
62	1	1.0035	1.0325	1.1413					
51.5	1	1.0037	1.0342	1.149					

$$\Phi_{pp}''(f, \alpha) = \frac{\Phi_{pp}(f)c^2 \left(\frac{R' \sin(56.5^\circ)}{h} \right)^2}{\rho^2 U_m^5 h F_I(\alpha)} \quad (2-48)$$

Figure 2-75 (b) and (d) shows a comparison of the normalization of far field sound after accounting for the spanwise inhomogeneity and changing source to observer distance for microphones at $\theta = 123.5^\circ$ and $\theta' = 123.5^\circ$ respectively. The normalization based on just the step-normal velocity and corrected source to observer distance ($\Phi_{pp}'(f)$) which was shown earlier in Figure 2-71 is repeated in Figure 2-75 (a) and (c) for these microphones for the sake of comparison. It can be seen that for the fixed microphone applying the corrections for the spanwise inhomogeneity does produces a better collapse of the spectra up to sweep angles of 30° (within about 1.5 dB). This shows that the sweep independence principle is indeed approximately valid for $\alpha \leq 30^\circ$ once the spanwise inhomogeneity is accounted for, which is in agreement with the results for Hao and Wang (2013). For the moving microphone (Figure 2-75 (c) and (d)) the collapse

is even better due to this microphone being in the sweep local coordinates and experiencing less effects of the spanwise inhomogeneity.

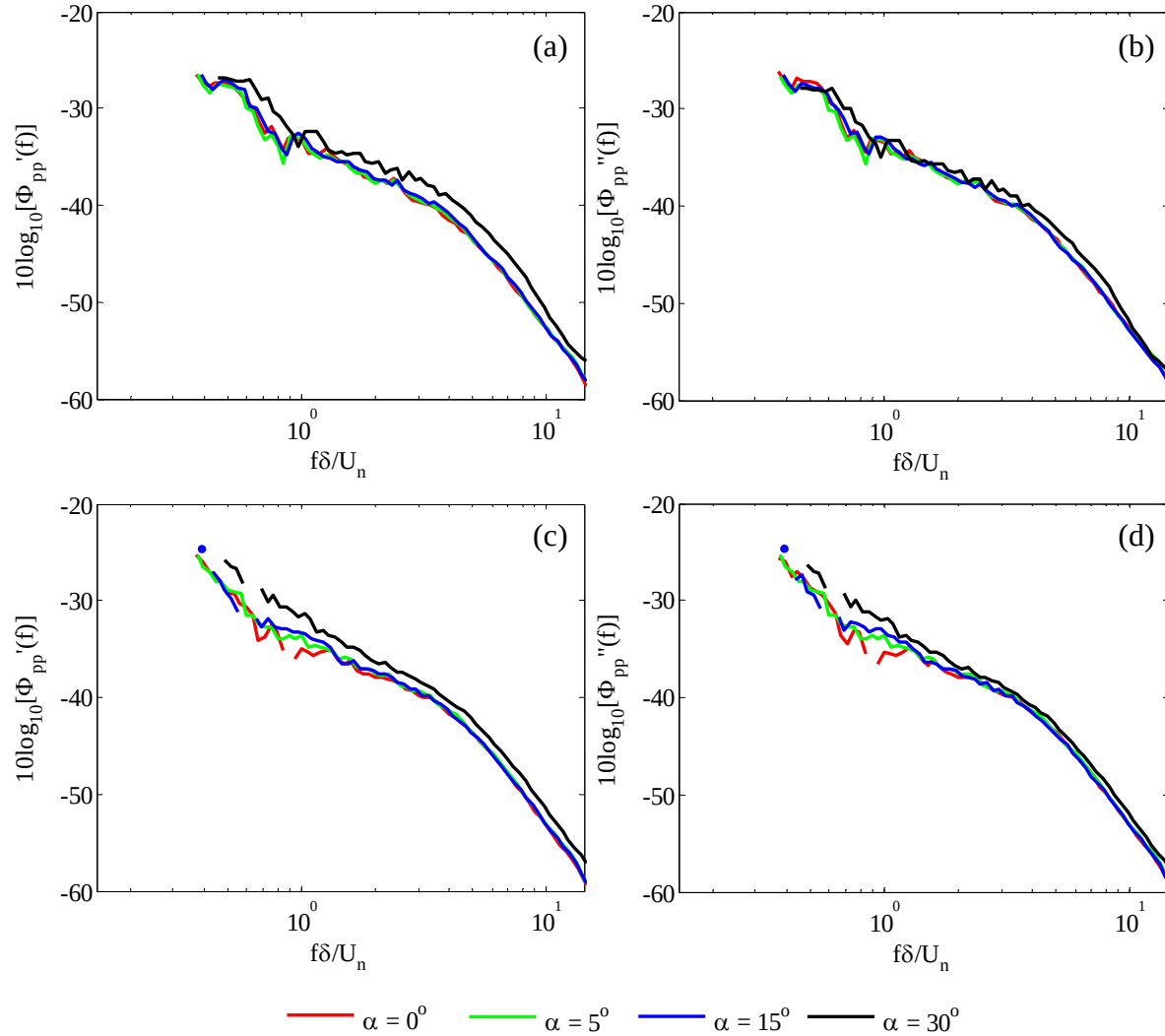


Figure 2-75 Comparison between the scaled far field sound spectra with and without corrections for spanwise inhomogeneity, $U_m = 21.3$ m/s. For fixed microphone at $\theta = 123.5^\circ$: (a) Uncorrected, (b) Corrected. For moving microphone at $\theta' = 123.5^\circ$: (c) Uncorrected, (d) Corrected.

Figure 2-76 (a) and (b) show the sound spectra normalized according to equations (2-46) and (2-48) for $\theta = 90^\circ$ and $\theta' = 90^\circ$ respectively. Again the spectra collapse up to sweep angle of 30° with the collapse being better for the moving microphone. The fact that the spectra for both the moving microphones show better collapse than those for the fixed microphones is clearly explained by the source distribution calculation performed earlier. Looking at Table 2-9 which shows the acoustic intensities for swept steps compared to an unswept step one can clearly see the effects of spanwise inhomogeneity are more intense on the fixed microphones. This is consistent with the effects of large sweep angle seen on the far field sound measured by these microphones.

We now look at the far field sound spectra normalized according to equation (2-46) for other microphones in the fixed arc. Figure 2-77 (a)-(d) shows the scaled spectra for a velocity of $U_m = 21.3$ m/s for four separate observer locations. We see that for the first three observer locations shown (Figure 2-77 (a)-(c)) the collapse of the spectra is good up to sweep angle of 30° within approximately 1.5 dB. For the

most downstream observer in the fixed microphone arc at $\theta = 51.5^\circ$ as shown in Figure 2-77 (d) the SNR at higher frequencies for higher sweep angles is low but at the frequencies for which sound is present a good collapse of the spectra is observed

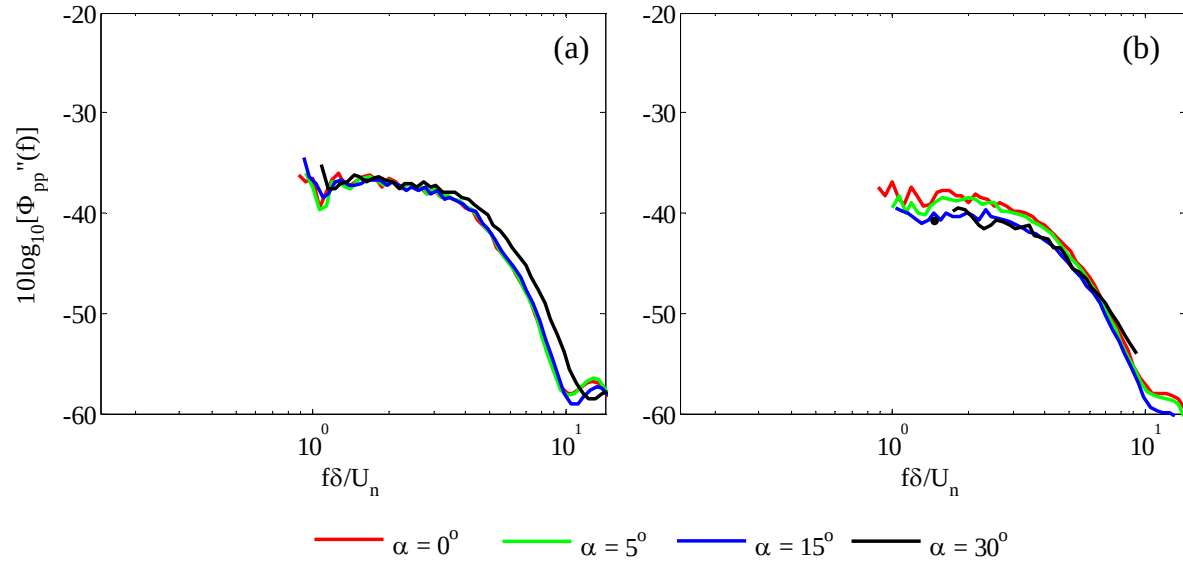


Figure 2-76 Far field sound spectra normalized on sweep-independence principle, $U_m = 21.3$ m/s. (a) $\theta = 90^\circ$ and (b) $\theta' = 90^\circ$. Acoustic intensity factors shown in Table 2-9 for each microphone have been applied to the spectra.

The general trends observed in the scaling of the far field sound from swept forward steps at velocity of $U_m = 21.3$ m/s are also observed in the two lower velocities for which measurements were made. It should be noted that the correction factors for the perceived acoustic intensities shown in Table 2-9 are defined as the ratio between the swept and unswept step case so changing the velocity does not change these ratios. Figure 2-78 (a)-(d) shows the scaled sound spectra for a velocity of $U_m = 10.3$ m/s. The scaling is shown for the two moving microphones and the equivalent microphones in the fixed arc. Looking at Figure 2-78 (a) and (b), for microphones at $\theta = 123.5^\circ$ and $\theta' = 123.5^\circ$, the sound appears to be approximately sweep independent for all the sweep angles being considered in the present work. In fact the scaling and the corrections for acoustic intensities work better for this low speed case compared to that for $U_m = 21.3$ m/s case which was shown earlier in Figure 2-75 (b) and (d). The scaled spectra for the fixed microphone ($\theta = 90^\circ$, Figure 2-78 (c)) also shows a good collapse when scaled on step-normal velocity except at higher frequencies where SNR is low. Lastly the scaled spectra for the moving microphone which is in the plane of the vertical face of the swept steps is shown in Figure 2-78 (d). Similar to the higher velocity flow case shown in Figure 2-76 (b) the collapse of the spectra here is slightly poor. The variation of the sound (as SPL, see Figure 2-68 (d)) with sweep for this particular microphone is most severe. This higher attenuation of sound spectra with sweep for this observer location leads to a slightly poor collapse compared to other observer locations

Figure 2-79 (a)-(d) shows the scaled and corrected sound spectra measured at velocity of $U_m = 15.6$ m/s as a function of sweep angle for the same observer locations shown in Figure 2-78. Looking at the figure it can be seen that the scaling on sweep independence principle approximately collapses the spectra for this velocity also up to a sweep angle of 30° .

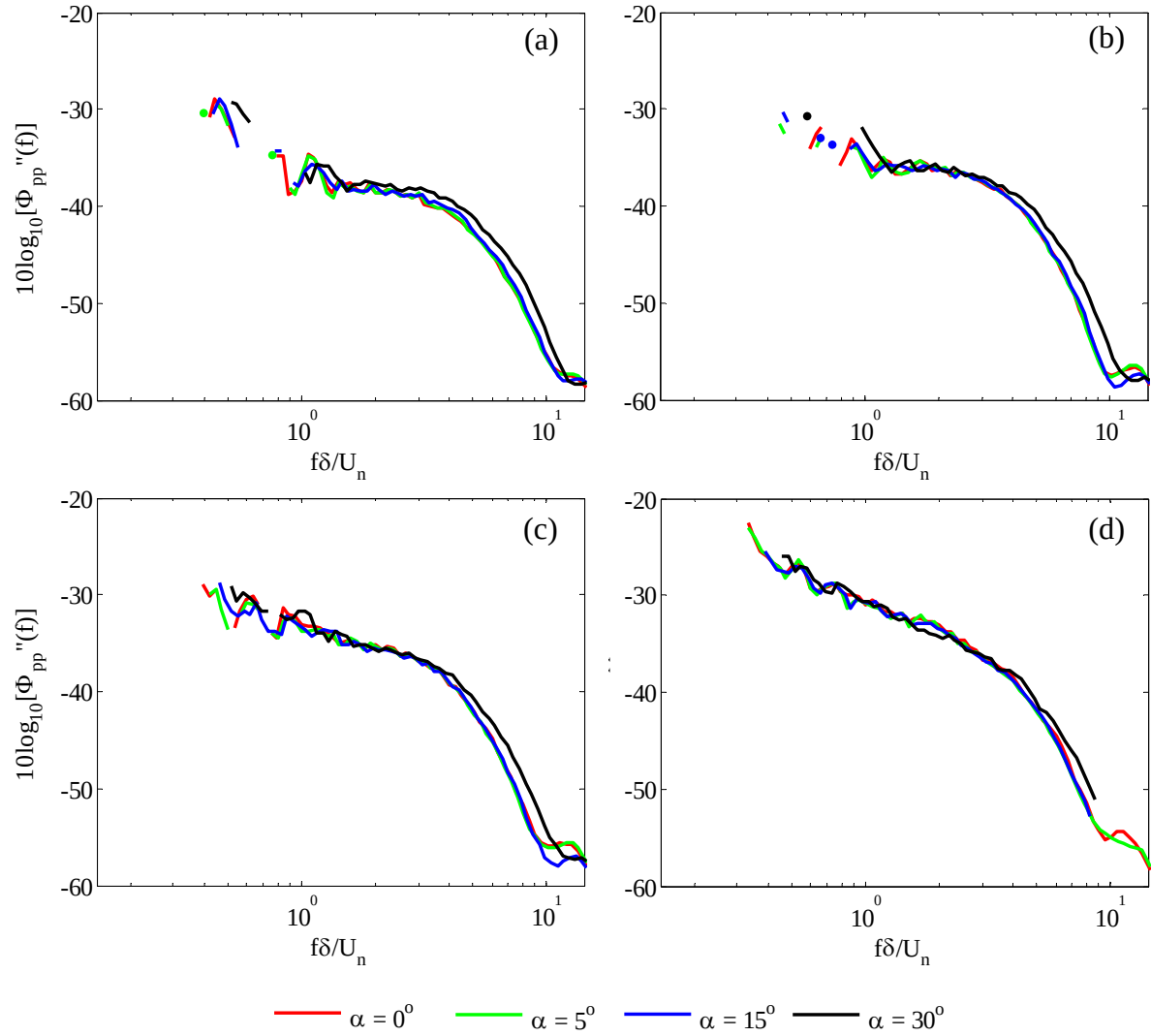


Figure 2-77 Far field sound spectra normalized on sweep independence principle at four separate observer locations in the fixed microphone arc, $U_m = 21.3$ m/s. (a) $\theta = 110.5^\circ$, (a) $\theta = 82^\circ$, (a) $\theta = 74^\circ$ and (a) $\theta = 51.5^\circ$.

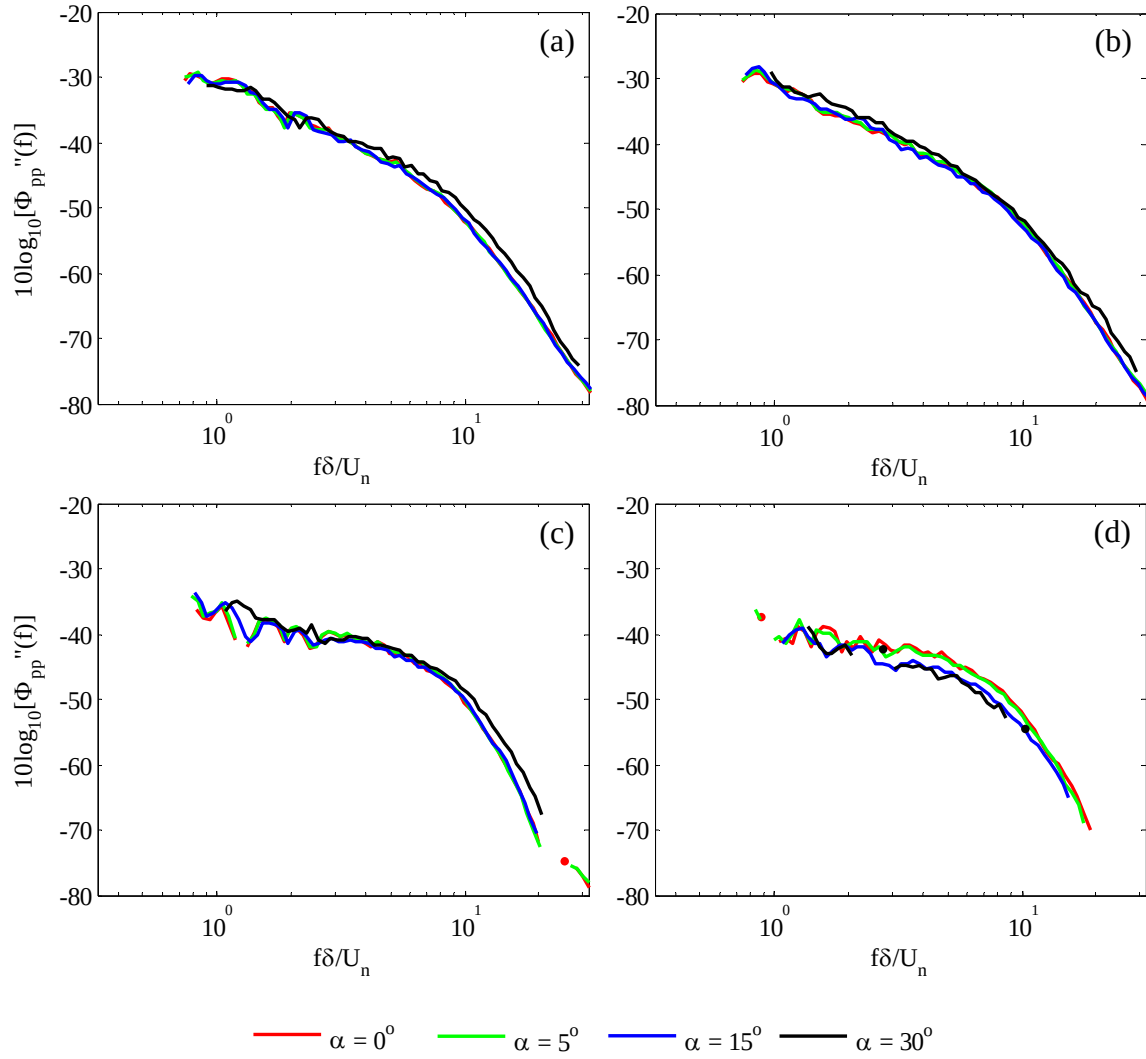


Figure 2-78 Far field sound spectra normalized on sweep-independence principle, $U_m = 10.3$ m/s. (a) $\theta = 123.5^\circ$, (b) $\theta' = 123.5^\circ$, (c) $\theta = 90^\circ$ and (d) $\theta' = 90^\circ$.

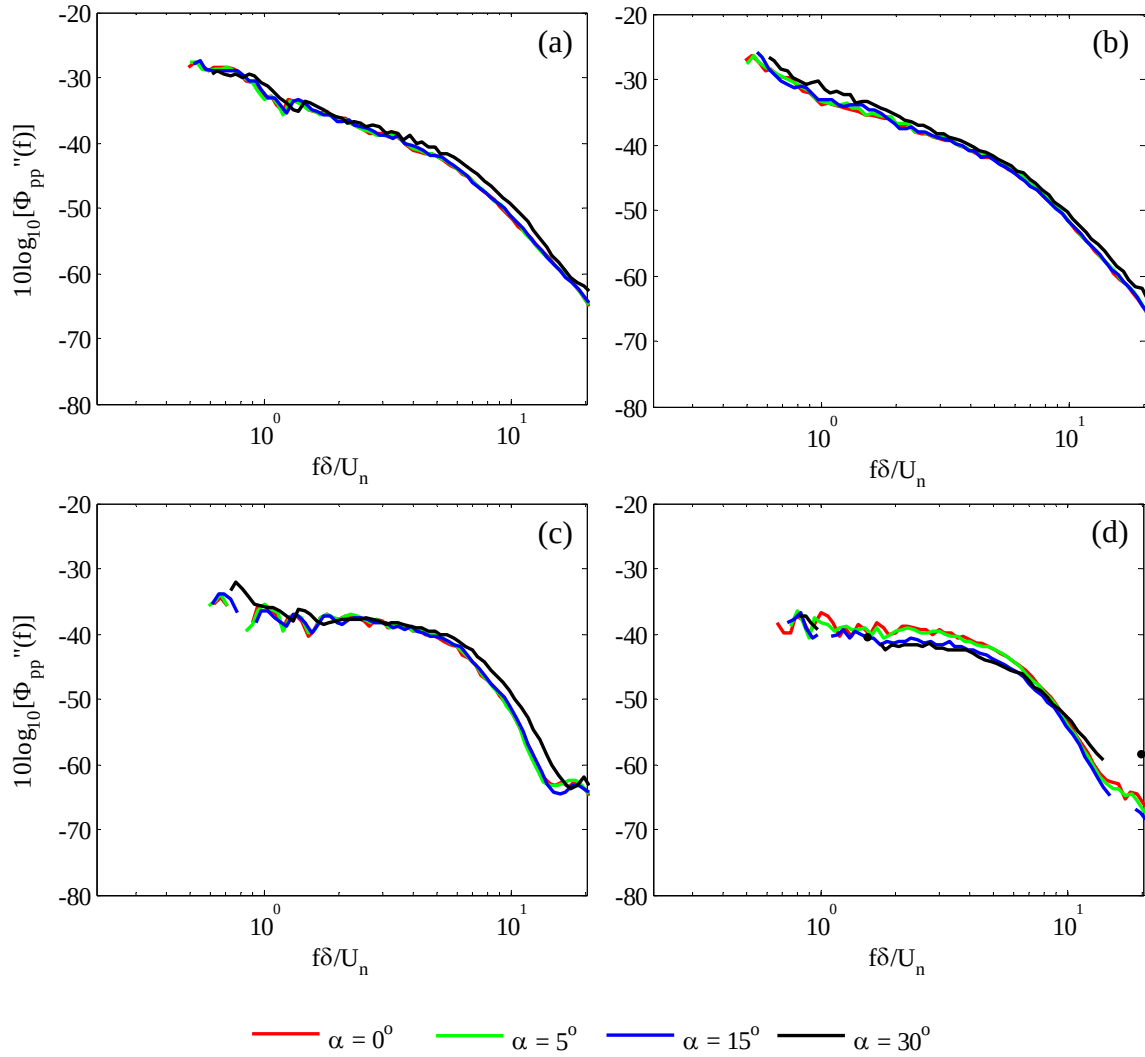


Figure 2-79 Far field sound spectra normalized on sweep-independence principle, $U_m = 15.6$ m/s. (a) $\theta = 123.5^\circ$, (b) $\theta' = 123.5^\circ$, (c) $\theta = 90^\circ$ and (d) $\theta' = 90^\circ$. Acoustic intensity factors shown in Table 2-9 for each microphone have been applied to the spectra.

2.2.6.5 Effect of Corner Rounding on Far Field Sound from Swept Forward Steps

We will now consider the effects of sweeping the forward step and rounding on the far field sound. The rounding radius to step height ratios considered are $r/h = 12.5\%$ and 25% . Though the measurements were also performed for $r/h = 50\%$ this case will not be pursued in this discussion since it has already been shown in §2.2.6 that for the unswept step the sound from this rounding is dominated by the scattered facility noise. The far field sound was measured using the same 8 microphone fixed arc and 2 moving microphones used for unrounded swept steps. The undisturbed edge velocities at the unswept step location for which measurements were made are $U_m = 21.3$ m/s, 15.6 m/s and 10.3 m/s. For the sake of brevity we will only present and discuss the results for $U_m = 21.3$ m/s since those for the other two velocities are qualitatively the same. Figure 2-80 (a)-(c) shows the effect of sweep on sound radiated from the rounded and unrounded forward steps measured by the most upstream microphone in the fixed arc ($\theta = 123.5^\circ$). For $r/h = 12.5\%$ and 25% (Figure 2-80 (b) and (c) respectively) sweeping the step has qualitatively the same effect on the far field sound as it does for the unrounded case in that the sound consistently drops throughout the frequency range as the sweep angle increases due to lowering of step-normal component of the velocity.

Similar to the unrounded step a sweep angle of 5° has barely any effect on the sound spectrum and the sound drops by about 1 dB throughout the frequency range. For the highest sweep angle of $\alpha = 30^\circ$ the effects become more noticeable and the sound is about 3-4 dB lower. Also note that the SNR for $r/h = 25\%$ (Figure 2-80 (c)) is lower than the two lower rounding cases as evident by the missing spectral data points at lower frequencies.

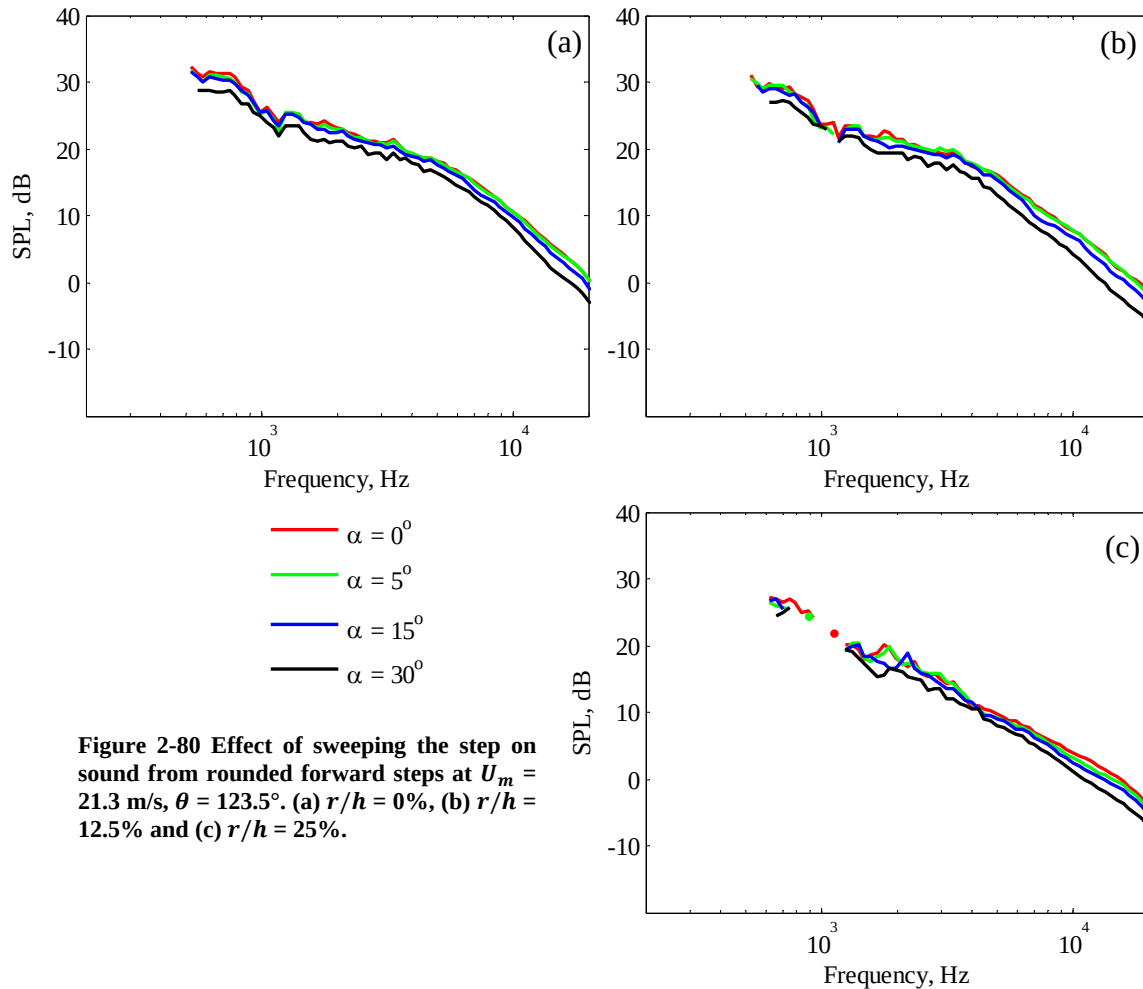


Figure 2-80 Effect of sweeping the step on sound from rounded forward steps at $U_m = 21.3$ m/s, $\theta = 123.5^\circ$. (a) $r/h = 0\%$, (b) $r/h = 12.5\%$ and (c) $r/h = 25\%$.

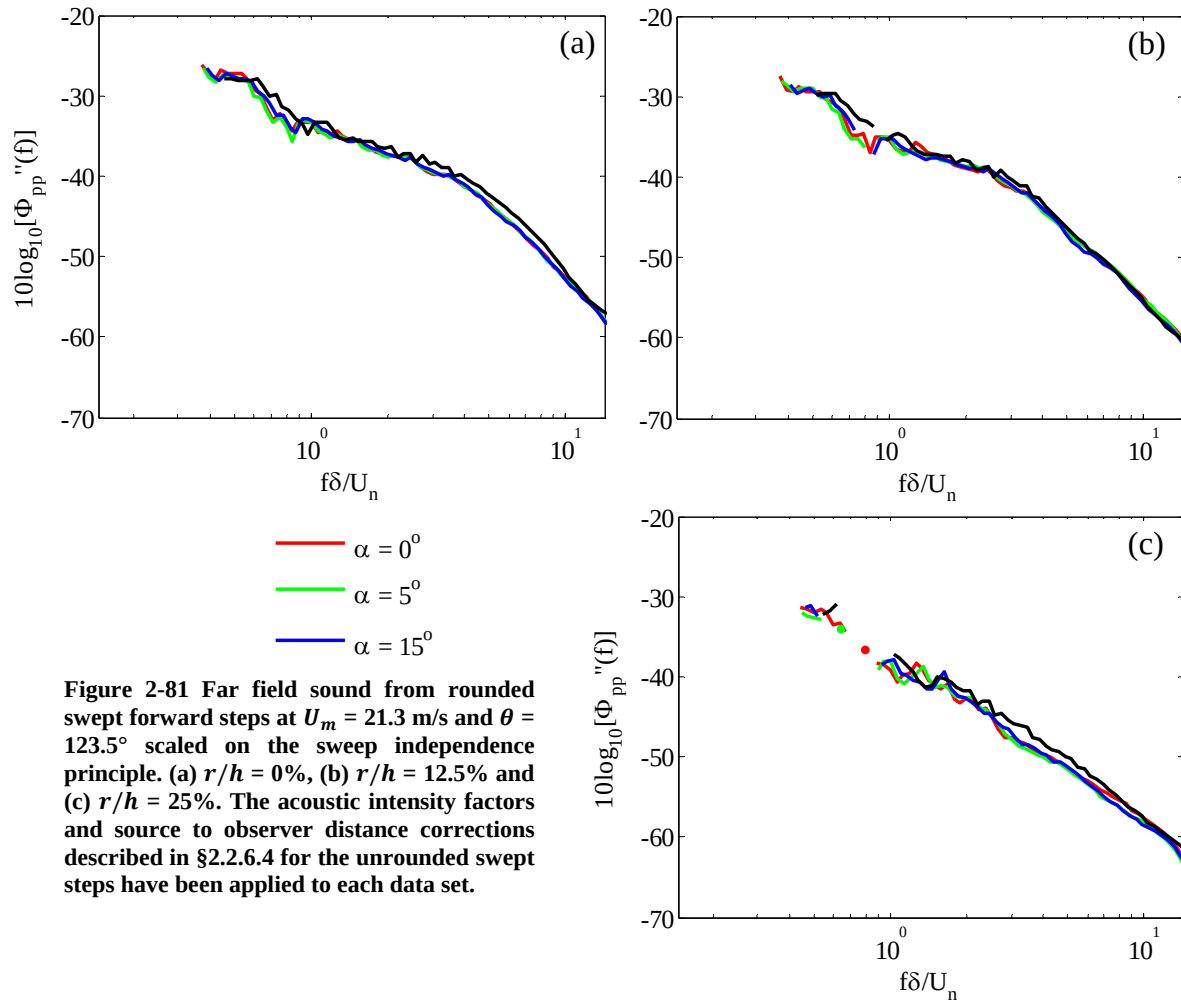
Figure 2-81 shows scaling of equation (2-46) applied to the far field sound measured at $\theta = 123.5^\circ$ for each of these cases. Looking at the figure now we can see that the sound from the $r/h = 12.5\%$ and 25% also approximately (within about 2 dB) follows the sweep independence principle up to $\alpha = 30^\circ$.

Figure 2-82 shows scaled spectra for two other observer locations within the fixed microphone arc for $r/h = 12.5\%$ and 25% at $U_m = 21.3$ m/s. Looking first at the scaled spectra for $r/h = 12.5\%$ (Figure 2-82 (a) and (b)) we see that the sweep independence principle is valid up to $\alpha = 30^\circ$ for this step at these observer locations as well. For $r/h = 25\%$ at $\theta = 90^\circ$ (Figure 2-82 (c)) the collapse is slightly worse with the scatter in the spectra of about 3 dB. For the most downstream observer location (Figure 2-82 (d)) the SNR for this rounding is poor so that at most frequencies no sound above the background is visible. Although not shown here the spectra in the rest of the fixed microphone arc follows the same general trend that the microphones located upstream of the step show a better collapse of spectra due to higher SNR while those located downstream lack data at most frequencies due to lower SNR especially for $r/h = 25\%$.

Consider now the sound measured by the two moving microphones for $r/h = 12.5\%$ and 50% scaled to account for sweep as shown in Figure 2-83. For the moving microphone perpendicular to the step

face ($\theta' = 123.5^\circ$) the spectra for both rounding radii (Figure 2-83 (a) and (c)) show excellent collapse. For $\theta' = 90^\circ$, Figure 2-83 (b) and (d), the collapse for $r/h = 12.5\%$ is slightly worse but still within 3 dB. For $r/h = 25\%$ (Figure 2-83 (d)) there is no sound for sweep angles higher than 5° making any interpretation about the validity of the sweep independence principle difficult.

The results from the rounded swept steps show that the sweep independence principle also approximately applies to rounded steps. For a rounding radius higher than 12.5%, uncertainty in the spectra due to low SNR obscures some of the behavior of the sound field.



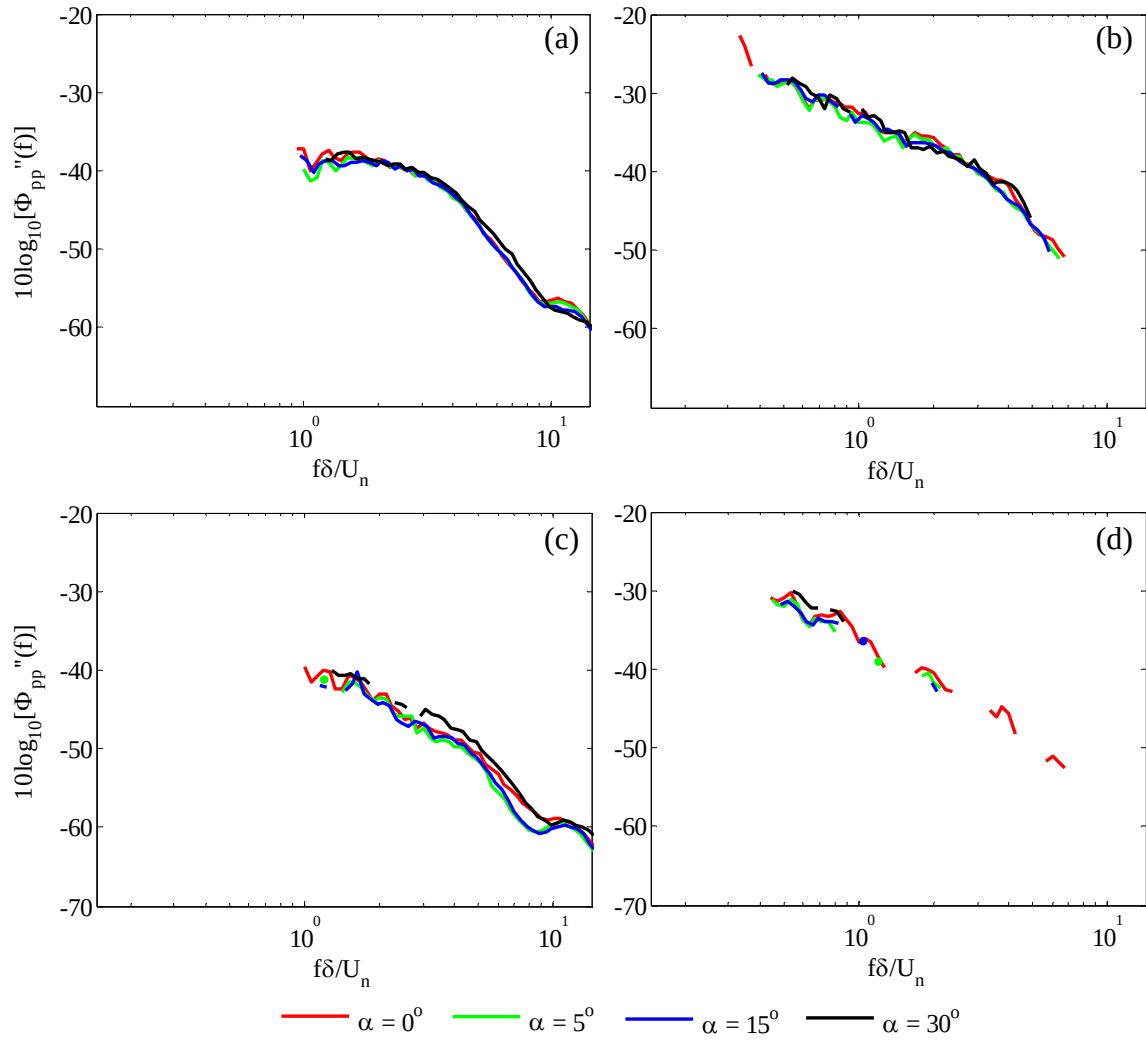


Figure 2-82 Sweep independence principle applied to the sound from rounded swept forward steps measured at two separate locations in the fixed microphone arc, $U_m = 21.3$ m/s. (a) $\theta = 90^\circ$ and (b) $\theta = 51.5^\circ$ for $r/h = 12.5\%$. (c) $\theta = 90^\circ$ and (d) $\theta = 51.5^\circ$ for $r/h = 25\%$.

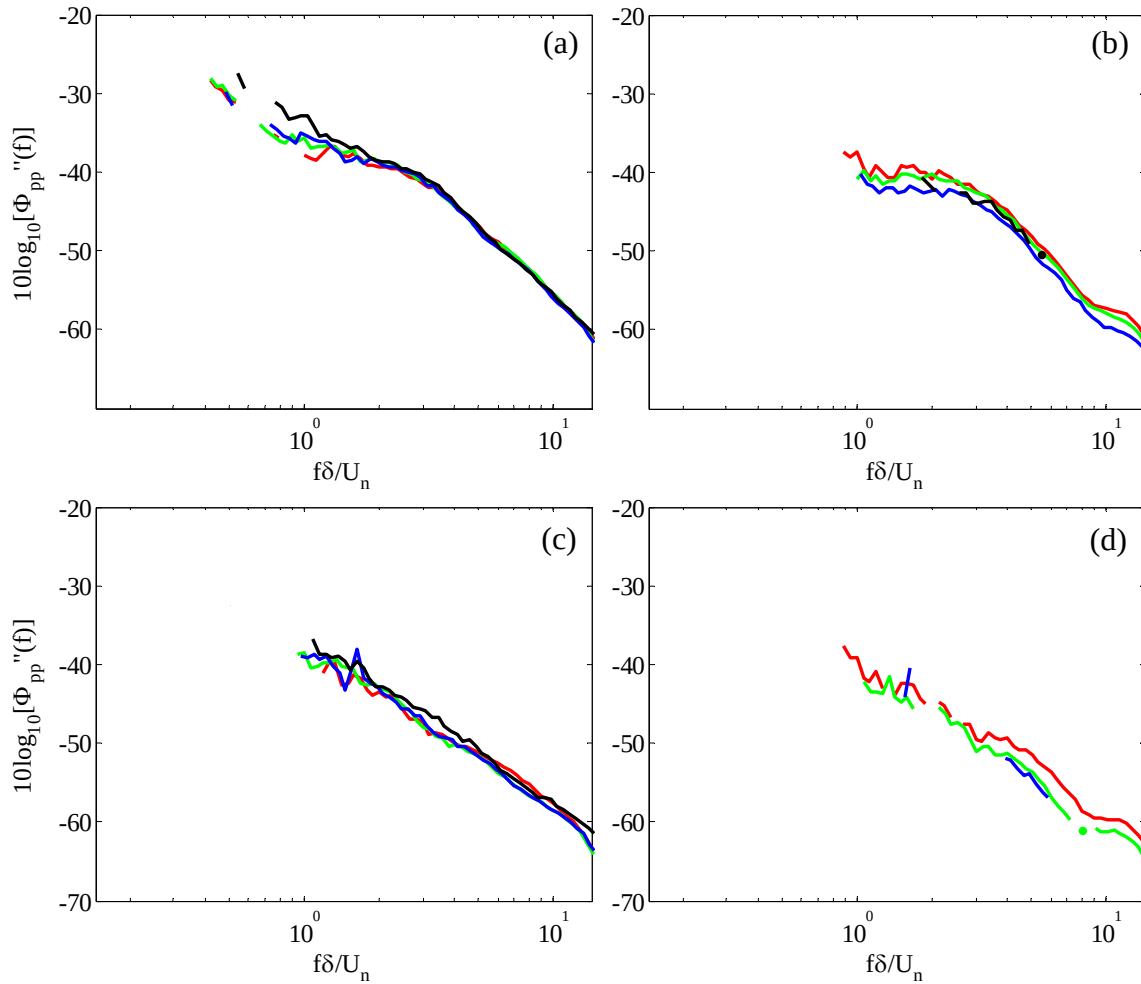


Figure 2-83 Sweep independence principle applied to the sound from rounded swept forward steps measured by the moving microphones, $U_m = 21.3$ m/s. (a) $\theta' = 123.5^\circ$ and (b) $\theta' = 90^\circ$ for $r/h = 12.5\%$. (c) $\theta' = 123.5^\circ$ and (d) $\theta' = 90^\circ$ for $r/h = 25\%$.

2.3 Summary of Results from Measurements of Sound from Surface Discontinuities in Wall-Jet Flow

The purpose of the work done in the Virginia Tech anechoic wall-jet facility was to determine the nature of sound radiated from two and three-dimensional surface discontinuities immersed in a low Mach number turbulent boundary layer flow. The measurements were performed for two-dimensional forward steps with rounded corners, two-dimensional gaps with rounding of both forward and backward step corner and swept forward steps with and without rounding of the corner.

- Six different rounding of forward step corner with rounding radii equal to 0%, 6.25%, 12.5%, 25%, 50% and 100% of the step height were considered. The step height to boundary layer ratios were $h/\delta = 78\%$, 81% and 84% for undisturbed edge velocities at step location of 10.3 m/s, 15.6 m/s and 21.3 m/s respectively. The Reynolds numbers based on the step height for these velocities were $Re_h = 8,300$, 12,450 and 16,800 and flow Mach numbers were $M = 0.03$, 0.045 and 0.06. The results from rounded forward steps show that:

- Far field sound from an unrounded forward step is broadband which shows effects of non-compactness in the region directly above the step due to destructive interference caused by acoustic reflections from the surface upstream of the step.
 - The spectra from unrounded steps scale on the mixed scaling proposed by Catlett *et al.* (2014) except for cases where the non-compactness effects are dominant.
 - Present results for sound from unrounded forward steps are in agreement with Catlett *et al.* (2014) once the differences in flow conditions are accounted for via a simple spanwise source distribution model.
 - As the step corner is rounded slightly to $r/h = 6.25\%$ sound reduction is seen only at the highest frequencies. Rounding the corner further leads to reduction throughout the frequency range with the spectra for $r/h = 25\%$ showing more than 10 dB sound reduction at its peak. The sound from the two highest rounding of $r/h = 50\%$ and 100% is mostly the background noise scattered by the geometry.
 - The directivity of sound is nearly independent of corner rounding and steps with $r/h \leq 25\%$ also show the effect of non-compactness.
 - The sound from rounded steps with $r/h \leq 25\%$ also scales on the mixed scaling of Catlett *et al.* (2014).
- Two different types of rounded gaps were considered. Type I gaps are where the forward step portion of the gaps was kept unrounded and the backward step corner was rounded with rounding radii equal to 0%, 12.5% and 50% of the step height. The gap height was the same as the forward step alone configuration and four different gap width equal to 1.13, 2, 4 and 8 step heights were considered. The results for type I gaps show that:
 - For unrounded gap with gap width of $w = 1.13h$ the spectra at lower frequencies show a different character than the spectra for a forward facing step alone configuration. The higher frequencies for this gap configuration are attenuated compared to a forward step and show scalloping behavior indicative of acoustic reflections from the gap. As the gap width is increased to $w = 2h$ and $4h$ the sound at lower frequencies becomes quantitatively and qualitatively similar to a forward step alone configuration but the higher frequencies still show attenuation and scalloping behavior. For $w = 8h$ the sound resembles that from a forward step alone configuration throughout the frequency range.
 - The unrounded gap spectra also scale on mixed scaling of Catlett *et al.* (2014) except in cases where the non-compactness effects are dominant.
 - Rounding of the backward step corner for any gap width has negligible influence on far field sound and its directivity.
 - Type II gaps are where the backward step portion of the gap was kept unrounded and the forward step portion was rounded with rounding radii equal to 0%, 6.25%, 12.5%, 25%, 50% and 100% of the step height. The gap height was the same as the forward step alone configuration and only a gap width of 4 step heights was considered for these gaps. The results for type II gaps show that:
 - The sound is affected by the rounding of the forward steps corner of the gap in much the same way as it does for a forward step alone configuration with the same rounding radii with the exception that for lower rounding ($\leq 12.5\%$ of step height) more attenuation of sound is seen in case of gaps and for higher rounding ($\geq 25\%$ of step height) less attenuation of sound is seen in gap noise.
 - The effect of non-compactness are also visible in sound from these gaps. The directivity of the sound from these gaps appears to be similar to those for a forward step alone configuration with the same rounding radii.

- Sound from swept forward steps four sweep angles of $\alpha = 0^\circ, 5^\circ, 15^\circ$ and 30° was studied. The step height was the same as that for the two-dimensional steps. Additionally for each sweep angle four different rounding of corner with rounding radii equal to 0%, 12.5% and 25% and 50% of the step height were considered. The main findings of this work are:
 - Increasing the sweep angle leads to a reduction in sound levels throughout the frequency range without affecting the character of the sound spectrum up to a rounding radii equal to 25% of the step height. No sound above the background noise is seen at either sweep angle for the highest rounding (equal to 50% of the step height) considered in the present work.
 - The sound spectra for unrounded swept steps show approximate sweep independence up to a sweep angle of 30° once the spanwise inhomogeneity is accounted for. This result is in agreement with past results from computations of Hao and Wang (2013).
 - Sweep independence is also seen up to sweep angles of 30° for rounded swept steps with rounding radii less than equal to 25% of the step height.

3 Surface Discontinuities in High Reynolds Number Boundary Layer

The results of the measurements in the wall-jet boundary layer presented in §2.2 have helped us to understand some key aspects of behavior of far field sound from flow over surface discontinuities. The wall-jet is unlike a conventional boundary layer found on practical surfaces of interest in that it is not bounded by a free stream flow at its edge. Furthermore the maximum Reynolds number (Re_h) provided by the Virginia Tech Anechoic Wall-jet facility is 16,800 and in practical applications this can be much higher. In light of these facts the work on surface discontinuities was extended to Virginia Tech Stability Tunnel which is a large scale low speed wind tunnel that can provide much high Reynolds number flows. Specifically, the flow of a conventional high-Reynolds number boundary layer over two configurations were studied in this tunnel – a two-dimensional forward facing step with variable corner rounding, and an unrounded three-dimensional step formed around the perimeter of a squat circular cylinder (referred to as an embossment). These measurements were used to test the most important observations from the wall-jet study such as the noise-suppressing effects of corner rounding and the applicability of the sweep independence principal to truly three-dimensional configurations. Furthermore, the larger scale of the Stability Tunnel experiments allowed for additional instrumentation to be used to reveal the fundamental aerodynamic source of the step sound. In the following sections the experimental setup and related instrumentation used in this work will first be described followed by the presentation of results and associated discussion.

3.1 Apparatus and Instrumentation

3.1.1 Virginia Tech Stability Wind Tunnel

The Virginia Tech Stability Wind Tunnel is a low speed facility. The wind tunnel is a conventional closed return facility capable of providing flow with free stream velocity (U_∞) up to 80 m/s which corresponds to Reynolds number per meter of approximately 5 million. The facility is unique in that it can operate in both aerodynamic and anechoic configuration. Figure 3-1 shows a top view schematic of the facility in its anechoic configuration. The flow in the tunnel is generated by a 4.3m diameter propeller which is powered by a 0.45 MW DC motor and can operate up to 600 RPM. Flow from the fan enters an air exchange tower which is open to the atmosphere for temperature stabilization. After leaving the air exchange tower the flow enters a 5.5 m x 5.5 m settling chamber which contains seven screens to reduce turbulence levels. The flow then goes through a contraction with 9:1 area ratio and finally enters the test section after which it goes through a diffuser section back to the fan. The flow in test section has very low free stream turbulence levels of approximately 0.024% and 0.037% at $U_\infty = 30$ m/s and 57 m/s respectively. In the anechoic configuration as shown in Figure 3-1, the test section is flanked by two anechoic chambers on both the port and the starboard side. The inside walls of these anechoic chambers are lined with acoustic grade foam wedges to minimize the acoustic reflections by test section itself that can contaminate sound measurements.

While the most of the structure of the facility is outdoors, the test section itself is housed indoors. The test section is 1.83 m x 1.83 m in cross section and is 7.3 m in length. It is removable and it is this capability which enables the facility to be operated in both aerodynamic and anechoic configuration. Figure 3-2 offers a view of the anechoic test section looking downstream. The floor and ceiling of the anechoic test section are lined with perforate panels covered with Kevlar fabric. This fabric is acoustically transparent and at the same time is capable of containing the flow. Behind these floor and ceiling panels acoustic grade foam wedges (not shown) are placed to minimize the reflections from the supporting structure of the test section. The port and starboard side walls of the test sections are formed by a continuous tensioned Kevlar fabric panels through which sound passes into the anechoic chambers. Recently, Devenport *et al.* (2013) who studied the aerodynamic and acoustic behavior of this facility, demonstrated through the use of transfer

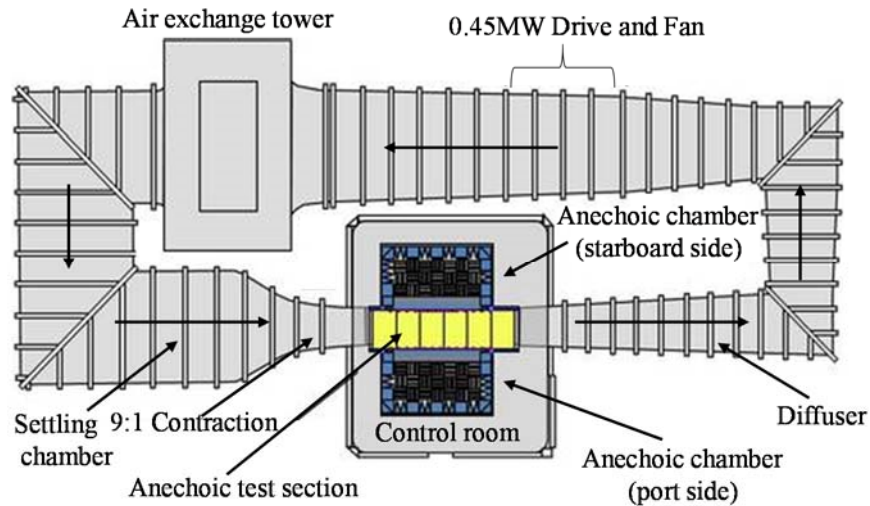


Figure 3-1 Top view schematic of Virginia Tech Stability Wind Tunnel.

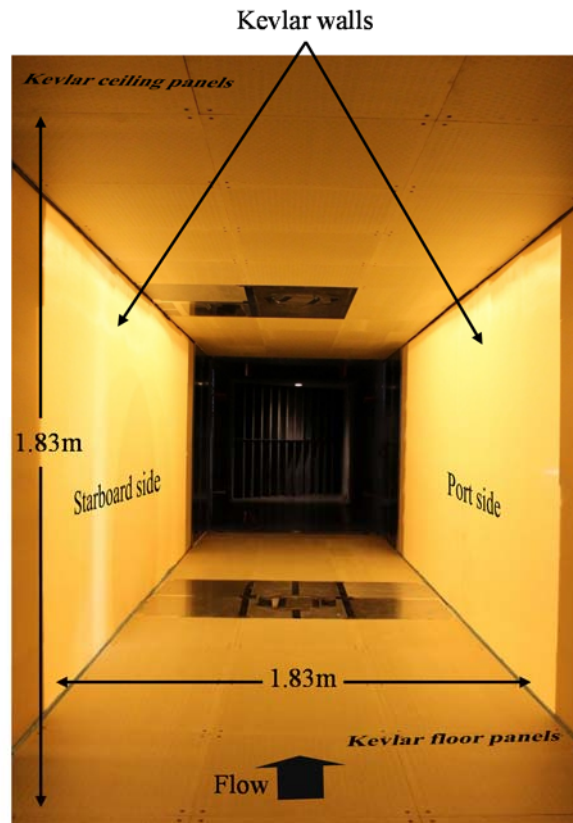


Figure 3-2 Anechoic test section of Virginia Tech Stability Wind Tunnel (looking downstream).

function between a noise source and microphones placed in these chambers, that the facility is anechoic above 180 Hz. They also determined that the flow along the porous Kevlar walls is well behaved and the blockage effects are minimal. Furthermore, through a series of measurements they have also developed empirical relationships to determine acoustic transmission losses through the boundary layer on Kevlar wall and the Kevlar fabric itself. Their results show that the sound attenuation through the Kevlar window is less than 1 dB below 10 kHz whereas the boundary layer losses can be significant at higher velocities (about 5

dB at 72m/s). These corrections are further discussed in 3.1.6.2 where they have been applied to present measurements.

3.1.2 Semi Anechoic (False Wall) Configuration of Virginia Tech Stability Wind Tunnel

In order to study boundary layer noise the anechoic test section is modified by replacing the starboard side Kevlar wall with a hard surface made of six polycarbonate panels. This false wall is used to grow the boundary layer, and surface discontinuities of interest are installed on this wall. This semi-anechoic configuration has been used in the recent past to study the fluid dynamics aspects of roughness and forward steps by Forest (2011) and Awasthi (2012) respectively. Details regarding the construction and aerodynamic behavior of the false wall can be found in these aforementioned studies.

Figure 3-3 shows the false wall configuration looking downstream. The starboard anechoic chamber behind the wall is also visible in the figure. To install this false wall the Kevlar floor/ceiling panels on the starboard side were replaced with same size aluminum panels on which mounting rails for the polycarbonate panels were installed. These mounting rails are 80/20 slotted profile aluminum beams that are 50.8 mm wide and 25.4 mm high. The polycarbonate panels are mounted on these rails using right angled corner brackets. Each polycarbonate panel is 1.78 m tall and 1.22 m wide and is supported by a frame built from 80/20 slotted profile aluminum beams. Note that the height of the test section is 1.83 m which is slightly larger than that of the wall panels which leaves approximately a 25.4 mm wide gap between the edges of the panels and floor/ceiling. This gap is covered using 1.6 mm thick right angled aluminum corners which run throughout the length of the test section and are faired to the wall and floor/ceiling using aluminum foil tape.

The slotted cross section profiles of both the panel support structure and the mounting rails provide each panel two degrees of freedom, one in the streamwise direction and the other in the flow normal direction. The streamwise movement allows the wall to be adjusted to form a single continuous surface. The flow-normal movement allows adjustment of the wall so a zero pressure gradient along the wall can be obtained. Note that, as shown in the Figure 3-3, while the height of the test section in this false wall configuration is the same as fully anechoic configuration (Figure 3-2), the width is about 0.08 m less. Once the wall is positioned correctly, the upstream edge of the wall is joined to the contraction wall using a fairing. This fairing which has been shown in Figure 3-3 consists of a plastic board mounted on a wooden frame which is bolted directly to the contraction wall of the facility. The upstream edge of this fairing is located approximately 2.4 m from the leading edge of the test section and this edge is faired to the contraction using 1.5 mm thick aluminum plates. The junction between the fairing and the false wall is covered with a 0.25 mm thick brass shim as shown in the figure. More details on the construction and installation of this fairing can be found in Forest (2012). The contraction fairing is also where a 9.5 mm tall trip (Figure 3-3) is installed to grow turbulent boundary layer on the false wall. The trip is located 1.36 m upstream of the leading edge of the test section. The trip is simply an L shaped cross section aluminum beam with one leg measuring 9.5 mm and the other leg being 12.7 mm. The thickness of this trip is about 1.6 mm and it is installed by bolting the 12.7 mm leg to the fairing. The contraction fairing/trip junctions are sealed using aluminum foil tape and the contraction fairing/contraction floor/ceiling junctions are also sealed using foil tape running in the streamwise direction.

Figure 3-4 shows a top view schematic of the false wall configuration. Shown in the figure are the contraction and test section area along with the two anechoic chambers on the side. The location of false wall's leading edge relative to the trip (also shown in previous figure) has been shown in the figure. The six polycarbonate panels, each 1.22 mm wide, that make up the false wall and their support beams have been also depicted in this figure. The entire false wall is accessible from the starboard side anechoic chamber and this accessibility is utilized for any near field measurement instrumentations.

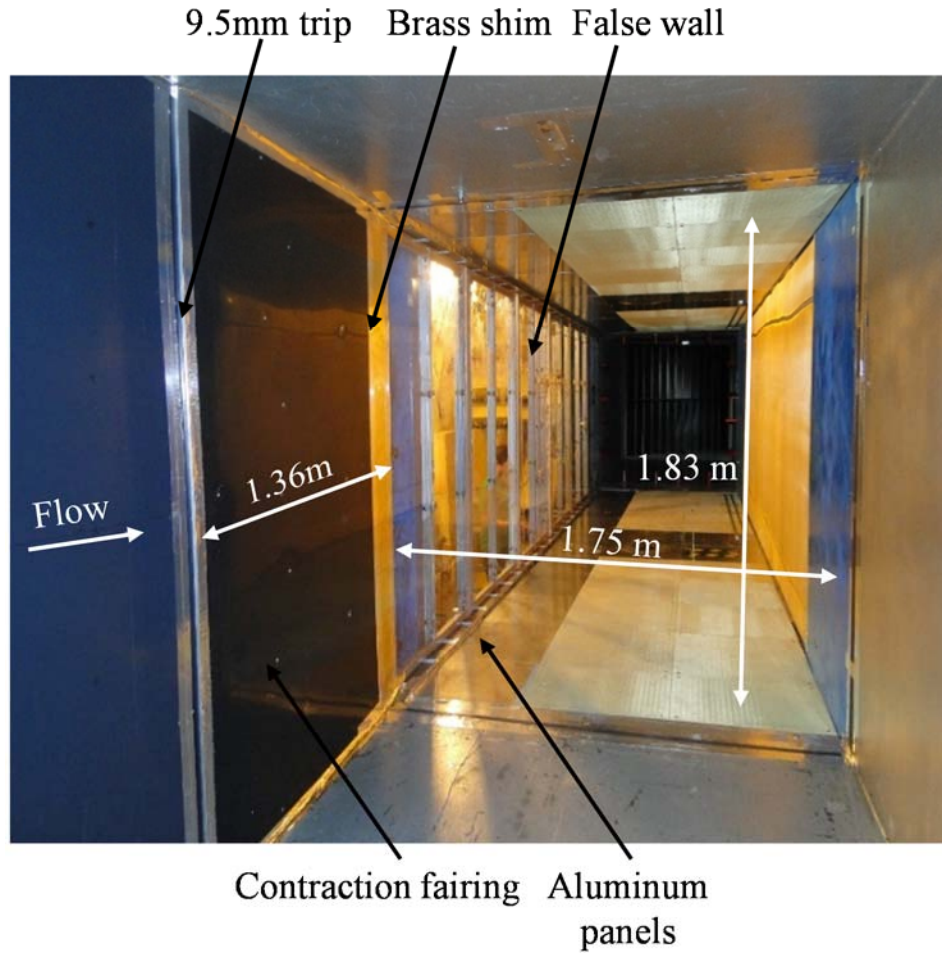


Figure 3-3 False wall configuration of Virginia Tech Stability Wind Tunnel (looking downstream).

To provide direct access through the false wall a 1.02 m x 0.55 m access hatch was created on one of its panels as shown in Figure 3-5. The upstream edge of the access hatch is located 4.37 m downstream of the boundary layer trip. As will be shown in §3.1.1, the two-dimensional forward step was installed covering this entire hatch. The hatch thus provided direct access to the downstream surface and the face of the step from the starboard side anechoic chamber. When the step was not installed (in case of undisturbed boundary layer or the circular embossment measurements) a polycarbonate sheet of the same dimensions as the access hatch is installed flush with the flow side surface of the false wall to cover this access hatch. Further details on the use of this hatch to enable easy access to the instrumentation of surface discontinuities will be provided during the discussion on instrumentation used to measure near field flow quantities in a later section.

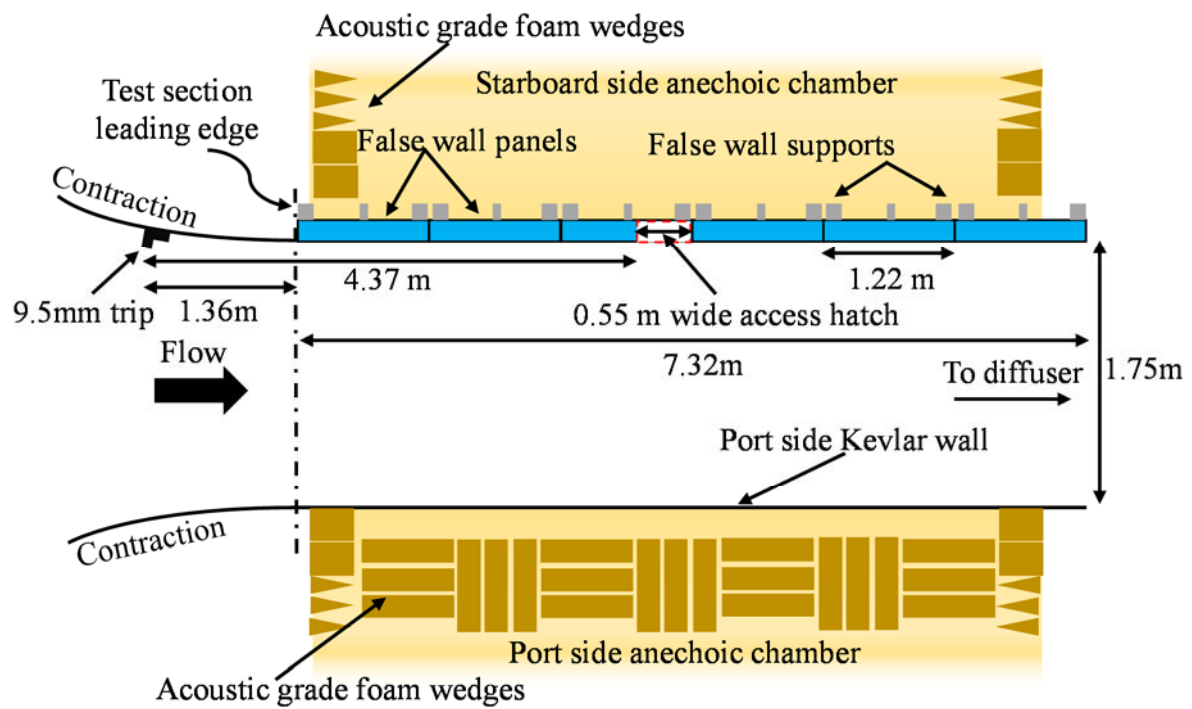
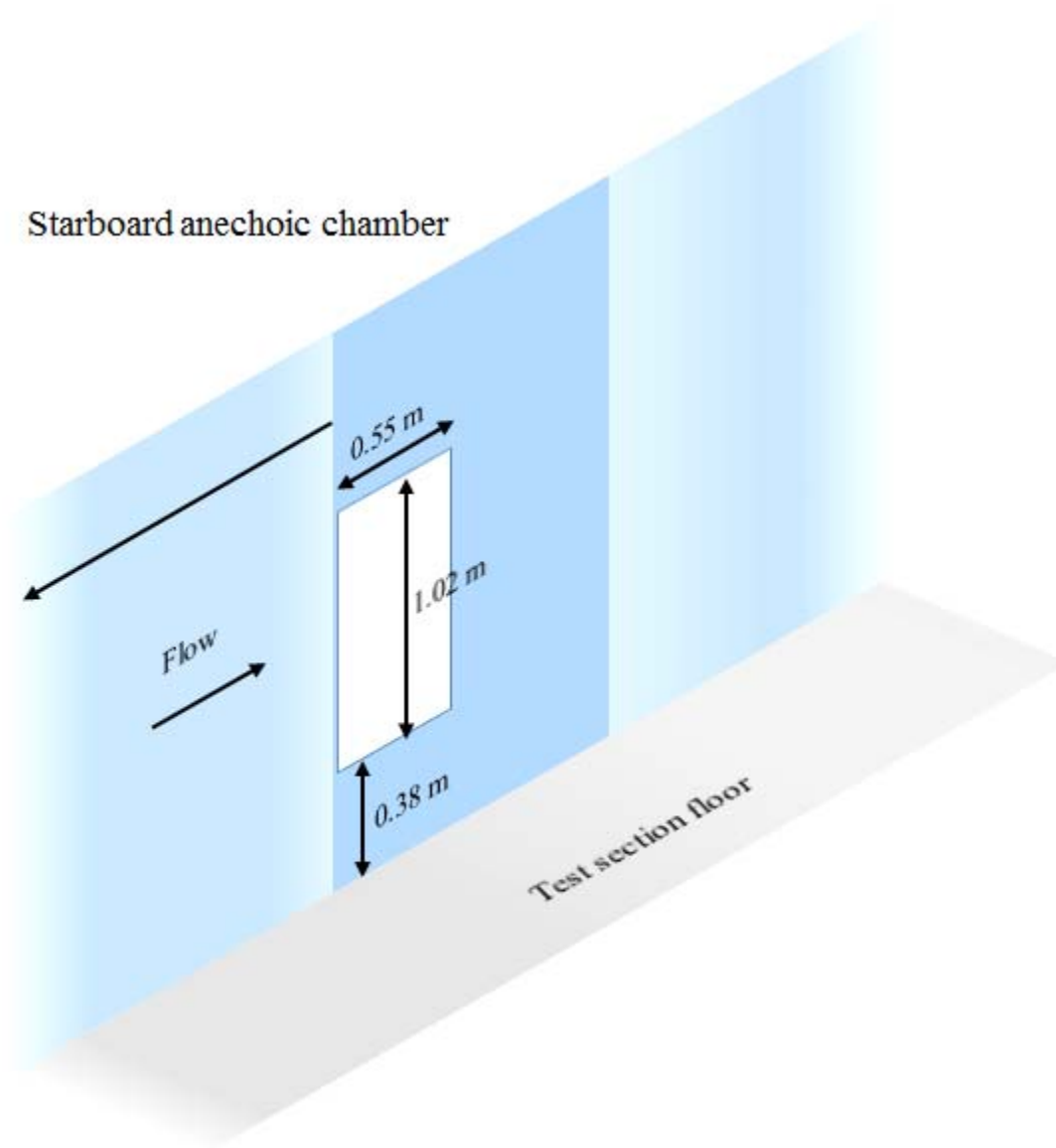


Figure 3-4 Top view schematic of the semi anechoic false wall test section of Virginia Tech Stability Wind Tunnel.



3.1.3 Undisturbed Boundary Layer Flow Instrumentation and Characteristics

3.1.3.1 Free Stream Velocity Measurement

The flow velocity in the test section is monitored through measurement of the total pressure in the settling chamber (p_{SC}) and free stream static pressure in the test section (p_{∞}). The total pressure is measured using a pressure port in the settling chamber of the facility which is located approximately 7.5 m upstream of the test section entrance. The static pressure port is located on the most upstream false wall panel approximately 1.69 m downstream of the boundary layer trip and 1.38 m from the floor of the test section. Both p_{SC} and p_{∞} are measured by passing the pressure signal to an Esterline NetScanner model 98 RK-1 pressure scanner which has a full scale range of about 2.49 kPa and an estimated uncertainty of ± 3.7 Pa. The velocity is then calculated using Bernoulli's equation. It should be noted that since the flow velocity in settling chamber is not exactly zero a correction factor was applied to the settling chamber pressure measurement. The corrected dynamic pressure is given by

$$q_{\infty} = \frac{p_{sc} - p_{\infty}}{Cp_s} \quad (3-1)$$

where Cp_s is the correction factor for the settling chamber which is given by

$$Cp_s = \frac{p_{sc} - p_{ref}}{p_{0,ref}} \quad (3-2)$$

where p_{ref} and $p_{0,ref}$ are the static and total pressure measured in the free stream of the test section using a pitot static probe also connected to a 98 RK-1 pressure scanner which has an uncertainty of ± 3.7 Pa. The value of the correction factor was found to be $Cp_s = 0.987$. The flow velocity is then given by

$$U_{\infty} = \sqrt{\frac{2(p_{sc} - p_{\infty})}{\rho_{\infty} Cp_s}} \quad (3-3)$$

The free stream density in equation (3-3) is given by

$$\rho_{\infty} = \frac{p_0}{RT_0} \quad (3-4)$$

where p_0 is the ambient pressure measured using a Validyne DB-99 digital barometer (uncertainty $\pm 0.05\%$ of full scale), T_0 is the ambient temperature monitored using an Omega ON-905-44005 Air Sensor which was installed in the contraction area of the facility (uncertainty of ± 0.2 K) and R is the ideal gas constant for air. Then combining equations (3-3) and (3-4) the free stream velocity is given by

$$U_{\infty} = \sqrt{\frac{2(p_{sc} - p_{\infty})RT_0}{p_{\infty} Cp_s}} \quad (3-5)$$

A jitter analysis was performed on equation (3-5) using the uncertainties in each of the measured quantity and assuming that the uncertainty in the correction factor Cp_s was negligible. The resulting uncertainty values for five different free stream velocities have been listed in Table 3-1. Looking at the table it can be seen that as the velocity increases the uncertainty in the measurement drops and is only about 0.08 m/s for $U_{\infty} = 60$ m/s.

Table 3-1 Uncertainties in free stream velocity measurements through jitter analysis

U_{∞} (m/s)	20	30	40	50	60
δU_{∞} (m/s)	0.23	0.15	0.11	0.09	0.08

The velocity in the test section was adjusted by changing the RPM of the tunnel fan which has approximately a linear relationship with the test section flow velocity. This relationship is shown in Figure 3-6 which shows the fan RPM as a function of flow velocity for nine different velocities and four separate test cases. We can see that the flow velocity is consistent across the different test runs and has a linear relationship with the fan RPM.

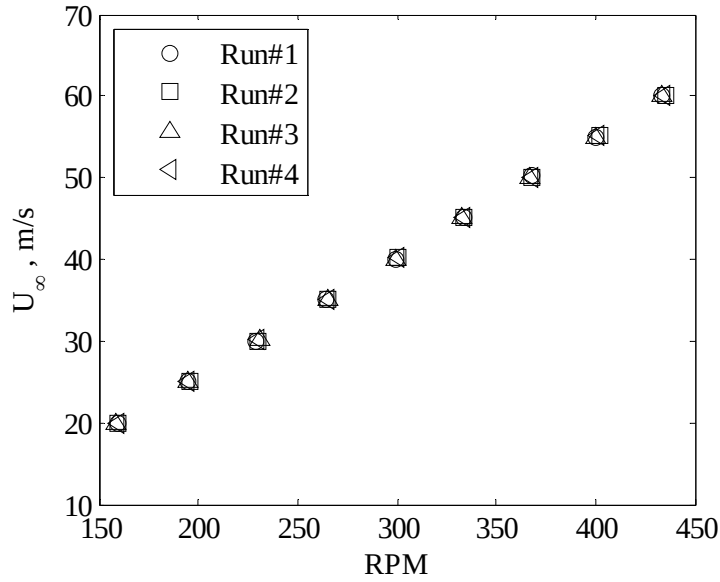


Figure 3-6 Relationship between wind tunnel fan speed (in RPM) and test section velocity.

3.1.3.2 Empty Test Section Pressure Gradient

Before the surface discontinuities could be installed it was necessary to measure the pressure gradient present in the empty test section. This gradient, intended to be close to zero, is an important boundary condition for the step flows studied. Static pressure along the false wall was measured through the use of static pressure ports. Figure 3-7 (a) shows a schematic with pressure tap installed flush with the flow surface of the false wall. As shown in the figure these pressure taps use a 50.4 mm long tube with outside diameter (OD) of 1.6 ± 0.02 mm and inside diameter (ID) of 1.3 ± 0.02 mm. The rear end of the tube was connected to the Esterline 98 RK-1 NetScanner (± 3.7 Pa uncertainty) using Tygon tubing with an OD of 3.17 mm and inside diameter (ID) of about 1.6 mm. The locations along the false wall at which mean static pressure was measured are shown in Figure 3-7 (b). The abscissa of the figure shows the streamwise distance of each pressure tap from the location of the trip and the ordinate represents the spanwise location measured from the floor of the test section. The measurement locations consist of two streamwise rows which are located approximately 0.4 m and 1.4 m from the floor of the test section. As mentioned in the previous section, the free stream static pressure p_∞ was measured by a pressure port on the false wall. The location of this pressure port has also been shown in Figure 3-7 (b).

To look at the streamwise pressure gradient along the false wall we will use the pressure coefficient

$$C_p = \frac{p - p_\infty}{q_\infty} \quad (3-6)$$

where p is the pressure measured at a specific location along the wall, p_∞ is the free stream static pressure and q_∞ is the free-stream dynamic pressure. Figure 3-8 (a) and (b) shows the measured pressure distributions for free stream velocities of 30 m/s and 60 m/s respectively which correspond to Reynolds number per meter of approximately 1.96 and 3.88 Million respectively. The pressure coefficient is plotted as a function of streamwise distance in meters with the origin defined at the junction between the contraction and the anechoic test section. Each plot also shows a vertical dashed line which denotes the location at which the leading edge of forward steps and that of circular embossment were later placed along the false wall. Also shown on the plots are the uncertainty bounds for the pressure coefficients calculated using jitter

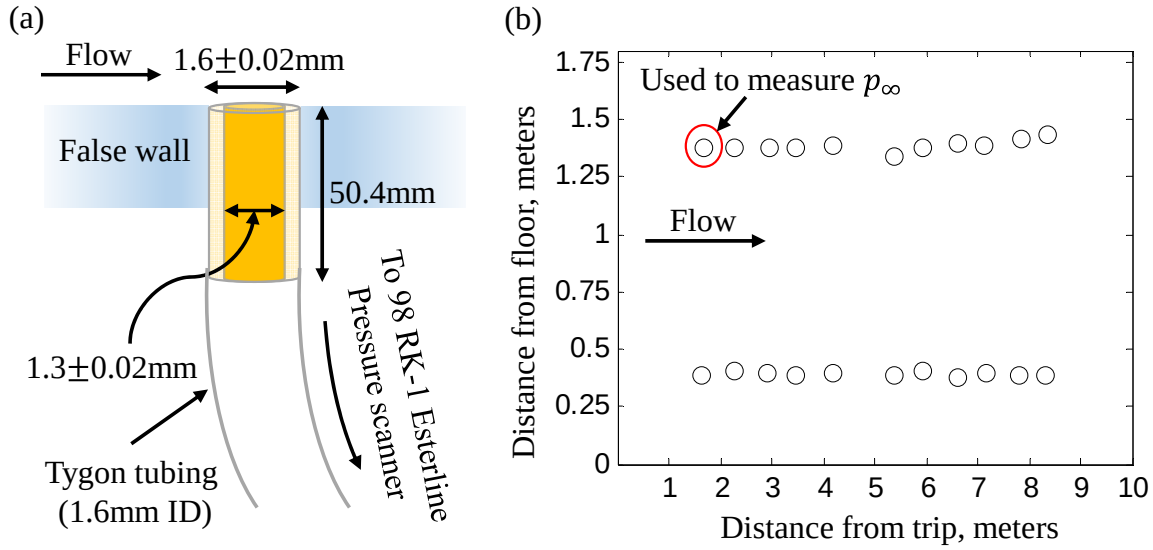


Figure 3-7 Mean wall pressure gradient measurement setup. (a) Pressure tap installed flush with the flow surface of the false wall, (b) Pressure tap locations along the false wall. Streamwise distance measured from the trip and spanwise distance measured from floor of the test section.

analysis¹. For both velocities the pressure gradient upstream of the leading edge of the step locations is near zero. Downstream of the discontinuity, measurements show a slightly lower pressure coefficient (about 4%). This is believed to be due to a slight curvature in the polycarbonate false wall panel on which these particular pressure taps were installed. This curvature is small and is not present further downstream where the mean pressure coefficient again approaches zero for both the velocities. Since the location of the pressure drop is far downstream of either the step or the circular embossment (approximately 41 discontinuity heights downstream of the leading edge of the geometry) it is expected that this drop did not affect any measurements made in the present work.

¹ The relationship between the uncertainty in C_p and measured quantities can be written as

$$\delta(C_p) = \sqrt{\left(\frac{\delta p}{q_\infty}\right)^2 + \left(\frac{\delta p_\infty}{q_\infty}\right)^2 + \left[\frac{p - p_\infty}{q_\infty^2} \delta(q_\infty)\right]^2}$$

Where $\delta p = \delta p_\infty = 3.7 \text{ Pa}$ is the uncertainty in the pressure scanner being used to measure the wall static pressure and ambient pressure. The uncertainty in dynamic pressure δq_∞ comes from the measurement of free stream density ρ_∞ and free stream velocity U_∞ . The uncertainties in U_∞ have already been discussed previously and have been shown in Table 3-1. The uncertainties in ρ_∞ comes from measurements of ambient pressure and temperature and when combined with uncertainty in U_∞ using a jitter analysis results in uncertainties of 5.2 Pa and 10.7 Pa in q_∞ at 30 m/s and 60 m/s respectively. Substituting these uncertainties and measured static and dynamic pressure in equation above yields the bounds shown in Figure 3-8.

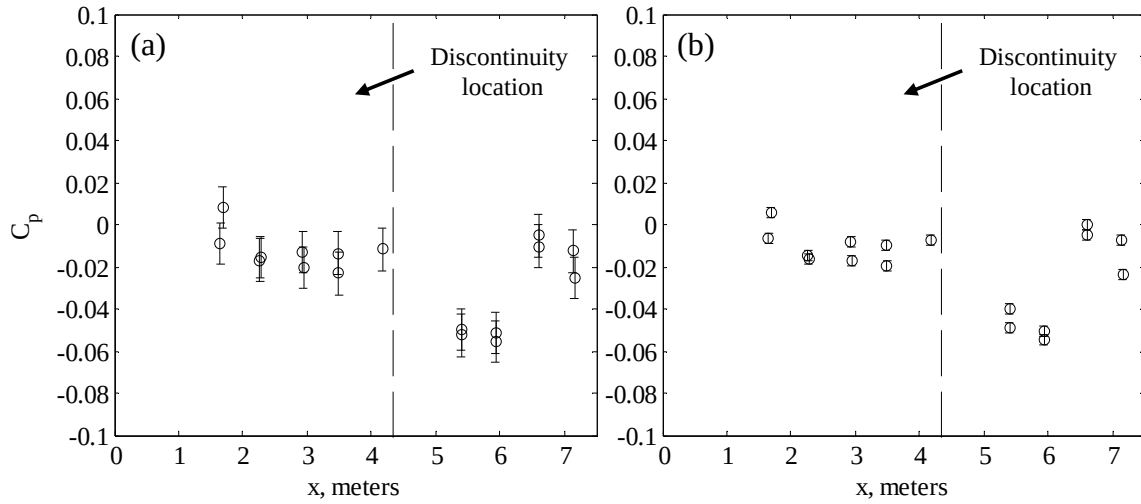


Figure 3-8 Mean pressure coefficient in undisturbed boundary layer flow along false wall at two different free stream velocities. (a) $U_{\infty} = 30$ m/s and (b) $U_{\infty} = 60$ m/s.

3.1.3.3 Undisturbed Boundary Layer Velocity Parameters

The undisturbed boundary layer on the false wall was measured previously by Awasthi *et al.* (2014) as part of a different study. This work involved measurements of mean velocity and turbulence intensity profiles using a four sensor hot-wire anemometer (quadwire probe). These measurements were made for free stream velocities of 30 m/s and 55 m/s with the same boundary layer trip as used in the present work. The profiles were measured 4.69 ± 0.003 m downstream of the trip, 0.34 m downstream of the location at which the leading edge of the steps were placed in the present work. Since the boundary layer growth is gradual Awasthi *et al.*'s (2014) measurements are expected to be representative of inflow conditions in present work.

Table 3-2 Undisturbed boundary layer parameters for flow over false wall
(Values taken from Awasthi *et al.* (2014))

U_{∞} (m/s)	δ (mm)	θ (mm)	δ^* (mm)	Re_{δ}	Re_{θ}
30	97	8.5	10.8	177000	15500
55	95	7.3	8.8	346000	26600

The undisturbed boundary layer mean velocity and turbulence stress profiles of Awasthi *et al.* (2014) are shown in Figure 3-9 (a) and (b) respectively. The mean velocity profiles are plotted on a semi-log scale and were fitted to a log-law curve with constants $\kappa = 0.41$ and $C^+ = 5.1$ which is also shown on the figure. The turbulence stresses shown in Figure 3-9 (b) also behave as expected from a turbulent boundary layer with high stresses within the boundary layer and near zero values outside it. Note that u , v and w here refer to the streamwise, wall-normal and spanwise velocities respectively. The velocity profiles at both speeds follow the log-law closely and are not much different from one another. The turbulent stresses are also similar at the two speeds with slight reduction with increasing velocity. The profiles show typical boundary layer behavior with higher stress close to the surface which that approach asymptotic values close to zero (relative to U_{∞}^2) as one moves outside the boundary layer.

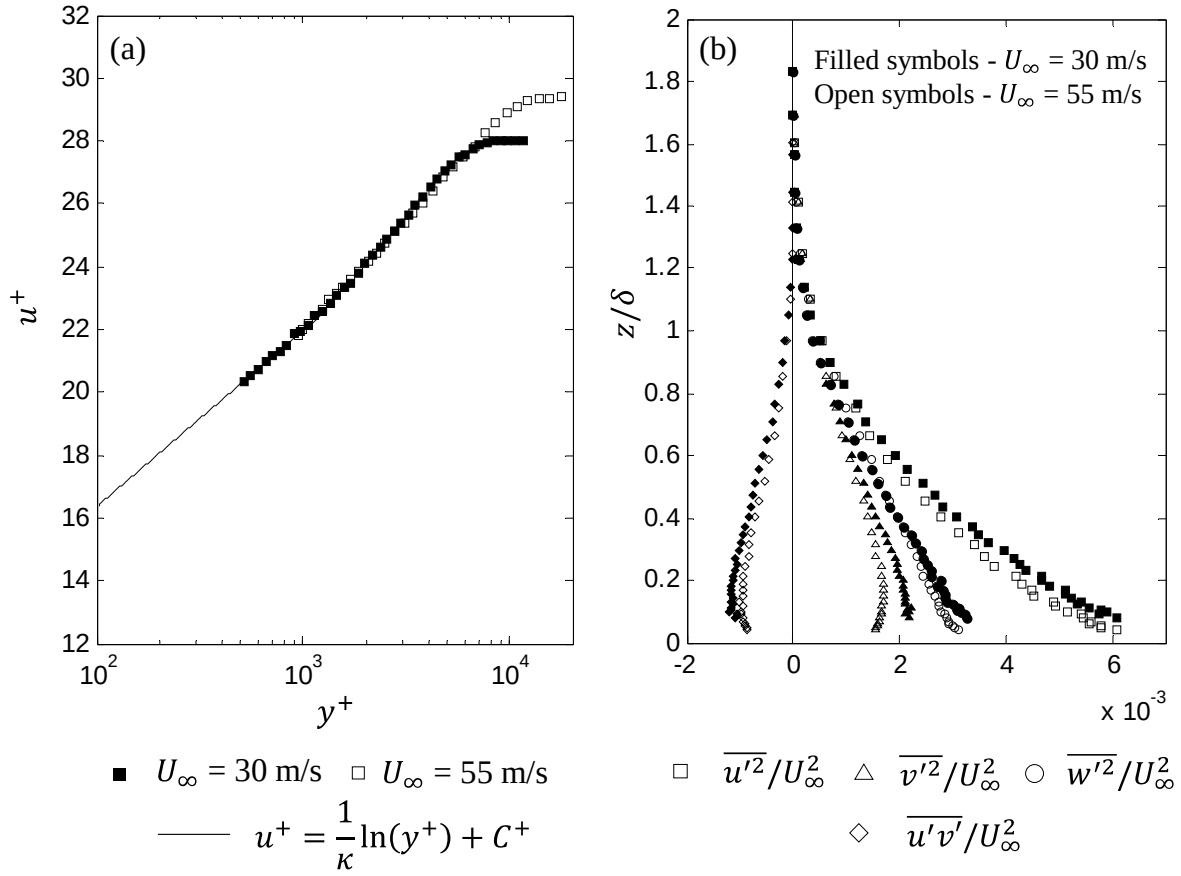


Figure 3-9 Undisturbed boundary layer velocity and turbulence profiles as measured by Awasthi *et al.* (2014). (a) Velocity profiles compared to log-law and (b) Turbulence stresses. Plots adapted from Awasthi *et al.* (2014).

3.1.1 Two-Dimensional Forward Facing Step Configuration

Figure 3-10 shows a top view schematic of the forward facing step geometry installed on the false wall. The step, of height $h = 25.4$ mm ($\sim 26\%$ of the undisturbed boundary layer thickness), was installed 4.35 ± 0.003 m downstream of the trip and 2.99 ± 0.003 m downstream of contraction/test section junction. The step spanned the entire height of the test section (1.83 m) and its upstream surface is formed by the false wall itself. The downstream surface of the step is 695 mm long in the streamwise direction followed by a 304 mm curved transition ramp. The transition ramp serves to smoothly ramp down the flow on the downstream surface of the forward step back to the false wall surface. This finite streamwise geometry was designed based upon the findings during the work on swept forward steps in the wall-jet facility (see §2.2.6.1), that a forward step that extended at least 12 step heights downstream was sufficient to acoustically simulate an infinitely extending forward facing step. The step face assembly allowed for the installation of different step corners with rounding radii of $r/h = 0\%$, 6.25%, 12.5% and 25%.

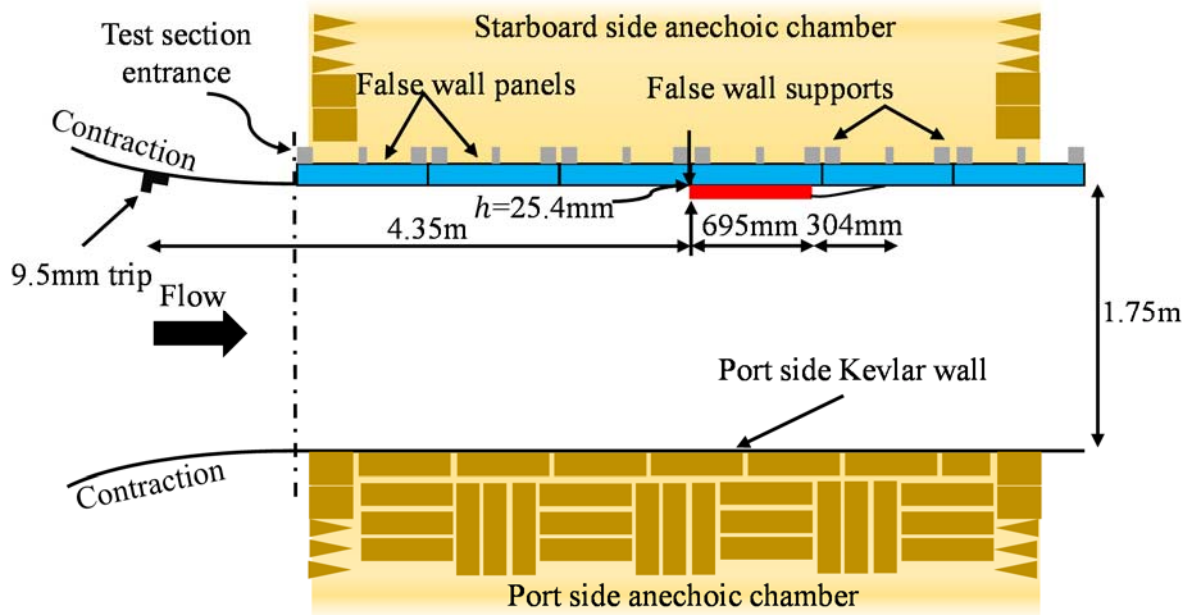
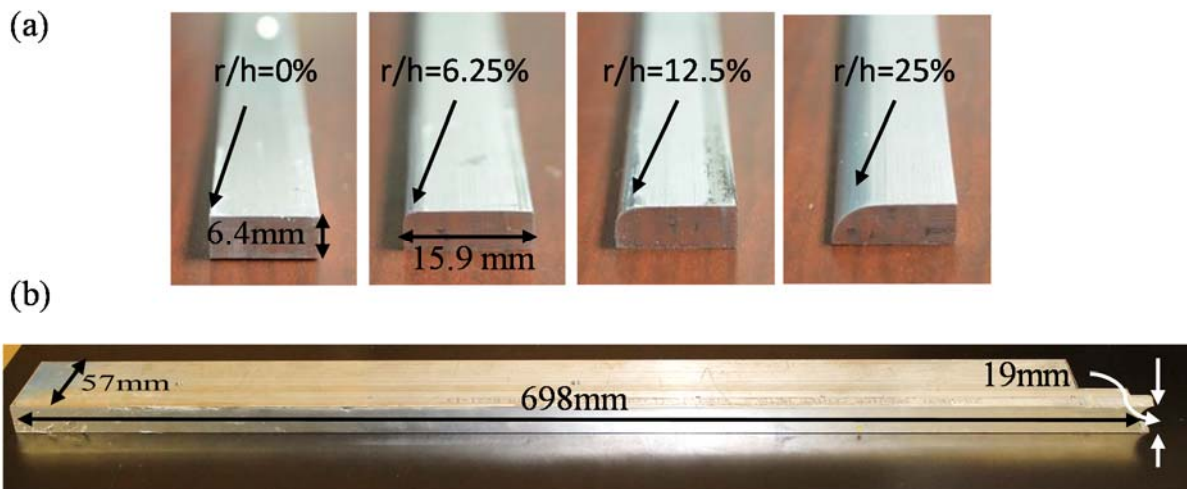
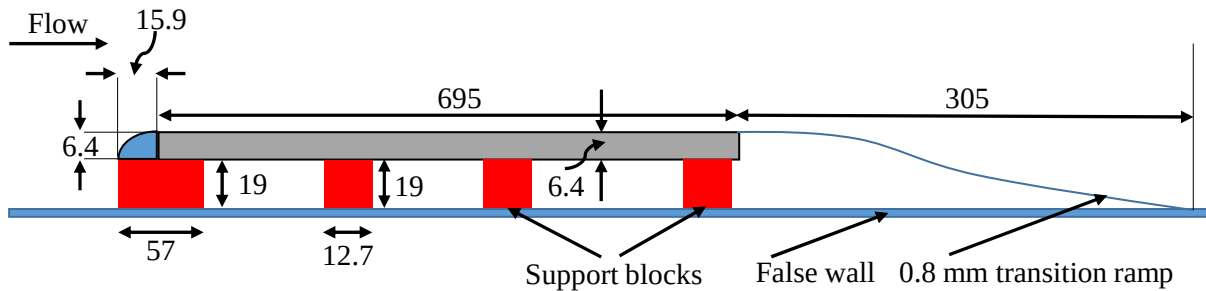
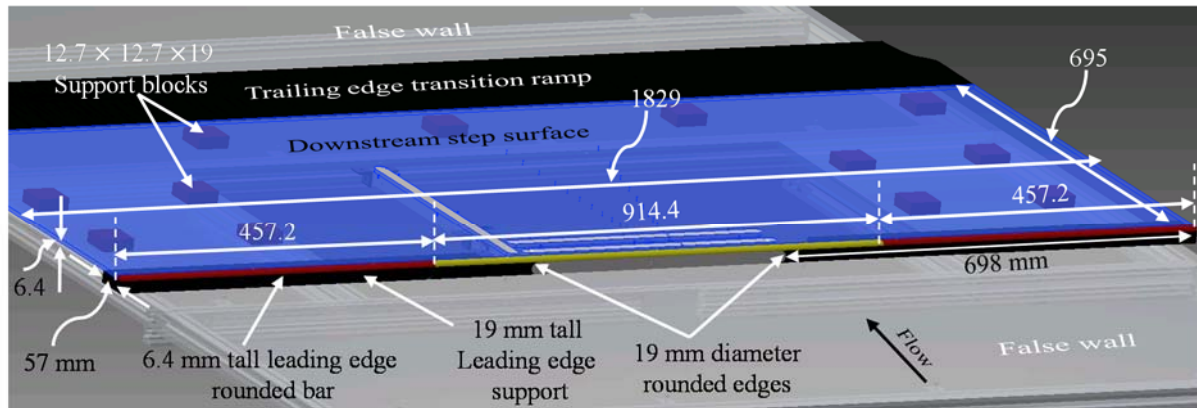


Figure 3-10 Top view schematic of the forward step setup.

Figure 3-11 shows a schematic of the assembly which was used to create the 695 mm long flow surface of the forward step. This assembly consists of an 1829 mm x 695 mm x 6.4 mm aluminum sheet which sits atop twelve 19 mm high support blocks, yielding a total step height of 25.4 mm. Each support block has a square cross section of 12.7 mm. The leading edge of the aluminum sheet is supported by two 698 mm x 57 mm aluminum bars which each contain a gap with a 19 mm diameter rounded edge towards the spanwise center of the assembly. The purpose of these rounded edges was to install instrumentation in the vertical face of the step which will be further discussed in §3.1.5. The leading edge supports were bolted to the 6.4 mm high aluminum sheet and were installed such that they protruded 15.9 mm ahead of the aluminum sheet in the upstream direction. Figure 3-12 shows a cross-sectional view of this assembly which illustrates this protrusion. As seen in this figure, this protrusion creates a lip ahead of the aluminum sheet on which bars with rounded edges can be installed to create rounding of the step corner. The leading edge aluminum bars used to create rounding of step corner are pictured in Figure 3-13 (a). As seen in the figure each of these bars is 15.9 mm wide which is equal to the width of the lip created by the leading edge support bars (pictured in Figure 3-13 (b)) that are placed underneath the aluminum sheet. The height of each of these bars is same as the height of the aluminum sheet which forms the downstream surface of the step. For each rounding two bars of span 457.2 mm and one bar of span 914.4 mm are combined to give the leading edge the same span as the aluminum sheet which forms the downstream step surface. The cross-sectional view of the assembly shown in Figure 3-12 also shows a 0.8 mm thick transition ramp which was installed downstream of the aluminum sheet to ramp the flow down to the false-wall surface. This transition ramp has the same span as the rest of the step assembly and is made of 0.8 mm thick steel. Figure 3-14 shows a schematic of the cross sectional view of the transition ramp. As seen in the figure, the ramp is installed at the trailing edge of the step assembly and has the same height as that of the forward step. The ramp also has a u-bend at its upstream edge which is used to bolt it down to the false wall. The junction between the step assembly and the transition ramp is covered using aluminum foil tape. The total streamwise length of the ramp is 304 mm and it has a gentle gradient along this length. The trailing edge of the ramp was thin and light enough so that it could be simply held down to the false wall using aluminum foil tape.



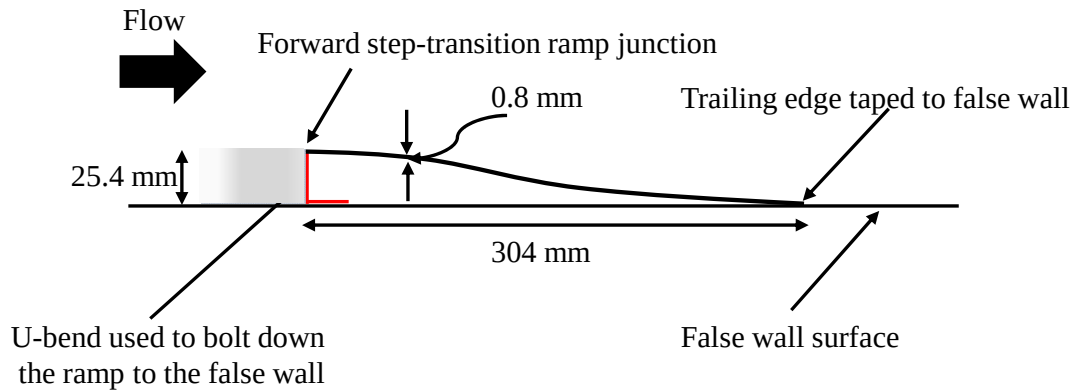


Figure 3-14 Schematic of trailing edge transition used to ramp down the flow on downstream surface of the forward step back to the false wall surface.

Another CAD rendering of the entire step assembly mounted on the false wall is shown below in Figure 3-15. This figure illustrates how the leading edge aluminum bars with corner rounding shown in Figure 3-13 (a) were installed to yield rounding of the step corner. Also shown in the figure is the trailing edge ramp of Figure 3-14.

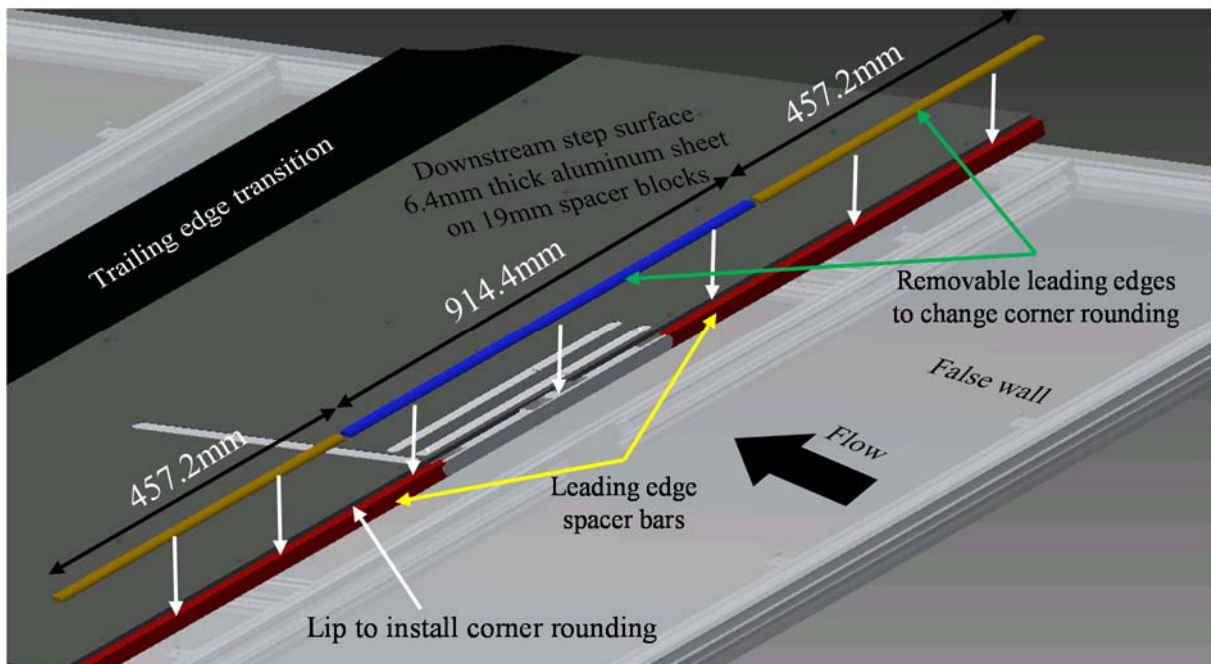


Figure 3-15 CAD rendering of the rounded forward step assembly installed on the false wall. Shown are the rounded leading edge aluminum bars that sit atop the 15.9 mm lip created at the upstream edge of the assembly. Also shown is the trailing edge transition ramp which is made of 304 mm long, 0.8 mm thick steel.

Figure 3-16 (a) and (b) show the actual setup of the forward step on the false wall looking downstream and upstream respectively. As shown in Figure 3-16 (a) the step spans almost the entire height of the test section and consists of a 695 mm long flat surface followed by a trailing edge transition ramp. Also shown in the figure is the step-test section ceiling junction which was faired using a 90° corner covered with foil tape. In order to minimize the noise that would be produced at the junction of the step face and the floor/ceiling of the test section, two rapid prototyped fairings were installed at each junction. These fairings

were designed so that they had gentle gradient in both the streamwise and the spanwise direction. Aluminum foil tape was used to hold these fairings against the step and the false wall. One of these fairings installed at the step-test section ceiling junction is shown in Figure 3-16 (a). If we now look at the view of the step assembly looking upstream (Figure 3-16 (b)) we can see the trailing edge transition ramp and its junctions with the step assembly and the false wall covered using foil tape.

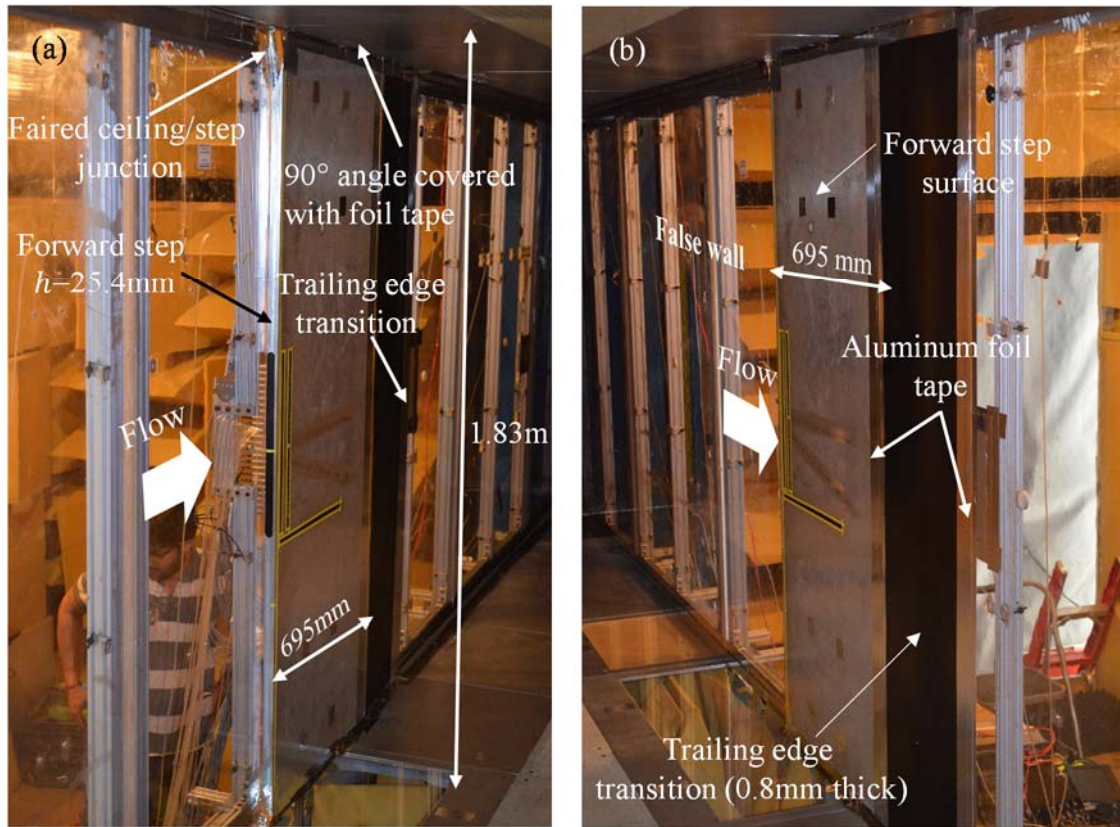


Figure 3-16 Forward facing step installed on the false wall of Virginia Tech Stability Wind Tunnel. (a) Looking downstream and (b) Looking upstream.

The seams between the leading edge bars and the downstream step surface were sealed using a 40 micron thin electrical tape which ran across the step span. The seams between the central 914.4 mm span leading edge bar and the two 457.2 mm side bars were also closed using this tape. Care was taken to ensure there was no discontinuity at these junctions that would disturb the flow. The seam formed on the face of the step between the leading edge bars and the 19 mm tall lip underneath them was sealed using thick motor oil grease. Note that the rounded slot on the step face created by the leading edge support as seen in Figure 3-11 which was used for instrumentation along the step face was sealed by using a plug of the same dimension as the slot when measurements on the step face were not being performed. This face plug which is visible in the CAD rendering of Figure 3-15 will be described further in the instrumentation section. The inside corner of the step (junction between the false wall and step assembly at the step face) was sealed using silicone rubber. Any gap at the corner between the step assembly and the roof/ceiling of the test section was sealed using a 2 mm thick aluminum right angle beams whose legs were 25.4 mm wide. Aluminum foil tape was used on both of the legs of these right angles to attach it to the step surface and the floor/ceiling of the test section.

Figure 3-17 shows a schematic of the near field coordinate system which will be used throughout the discussion. The origin of the coordinate system is located at the inside corner of the forward step and at the spanwise center of the test section i.e. 915 mm from the test section floor or the ceiling. The streamwise

coordinate is x which is defined positive in the flow direction, y is the wall-normal coordinate define positive away from the wall and the spanwise coordinate z completes the coordinate system according to the right hand rule which leads to it being positive towards the test section floor.

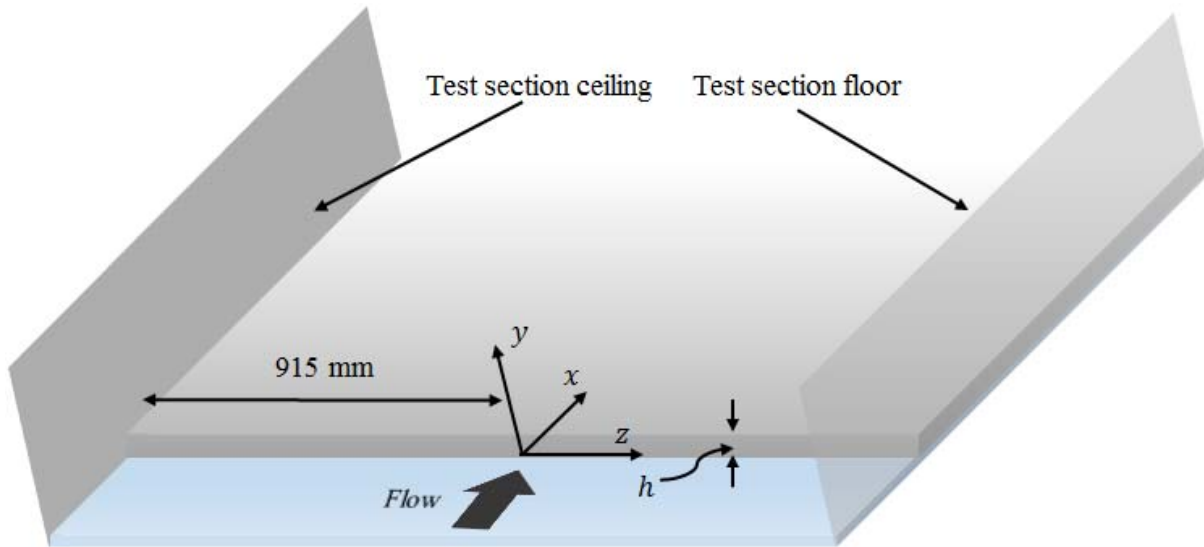


Figure 3-17 Near field coordinate system used for forward facing steps.

3.1.2 Mean Pressure Measurement Instrumentation

The mean surface pressure was measured using static wall pressure ports at several different streamwise locations both upstream and downstream of the step face. The measurement system and taps were the same or of the same design as those used in establishing the zero pressure gradient for undisturbed boundary layer flow which was described in detail in §3.1.3.

The mean pressure tap locations upstream and downstream of the rounded forward steps are shown in Figure 3-18. The vertical dashed line on the plot represents the location of the vertical face of steps. As shown in the figure the pressure taps are installed closed to the spanwise center of the false wall along lines which are 15° from the horizontal. This was done to ensure that closely spaced pressure taps do not disturb the flow and corrupt measurements by the pressure taps located immediately downstream. The pressure taps in the streamwise direction are logarithmically spaced so that there is high spatial resolution near the step location with the most upstream and downstream measurement located approximately 6 and 16 step heights from the step respectively.

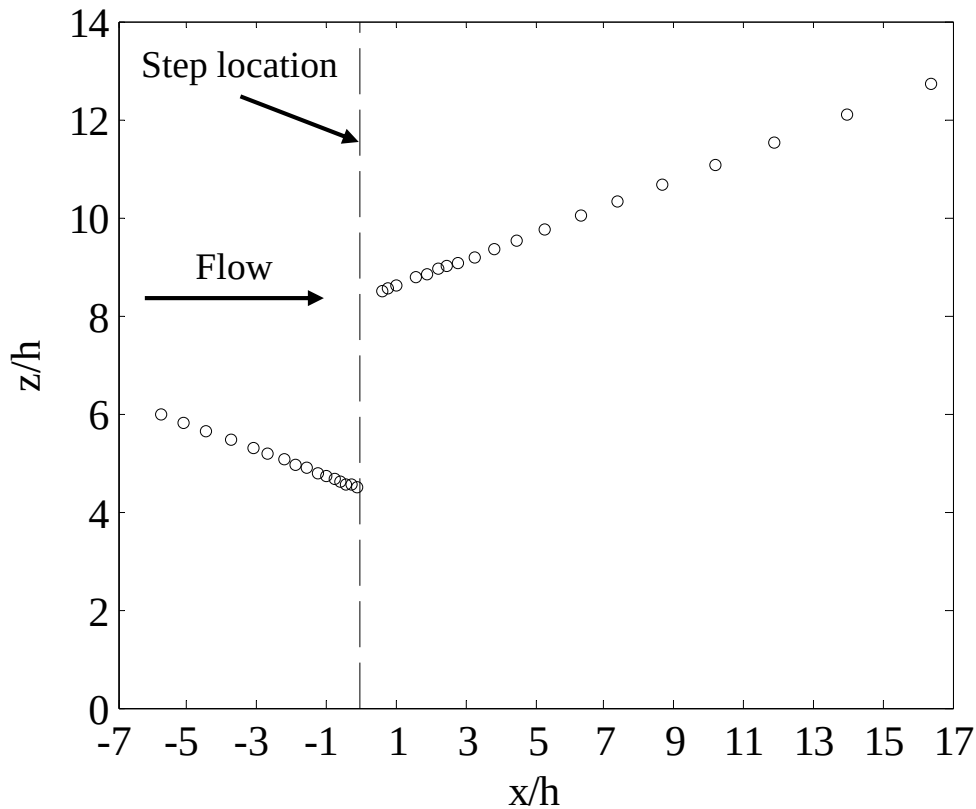


Figure 3-18 Measurement locations for mean wall pressure upstream and downstream of forward steps.

3.1.3 Oil Flow Visualization

The upstream separation length (x_s) and downstream reattachment lengths (x_r) associated with the step flows were measured using oil flow visualization. A mixture of Kerosene, titanium dioxide and oleic acid with a parts per volume ratio of approximately 10:4:1 was used. This mixture was applied to a flat black contact paper which was placed upstream and downstream of the vertical face of the step for each rounding. The flow was then turned on for approximately 120 seconds allowing the mixture to flow over the contact paper revealing the separation and reattachment regions. A ruler with 1/16th of an inch divisions on it was then placed on top of the contact paper with flow pattern and images were captured for further analysis. Images were post processed to calculate the separation and reattachment lengths. Typical oil flow visualizations upstream and downstream of the unrounded forward step are shown in Figure 3-19 (a) and (b) respectively. The figure also illustrates how the separation and reattachment lengths are calculated. The reference line shown on these figures are the lines with known distances. Since the separation or reattachment phenomenon is unsteady the flow visualization results form a band the center of which is assumed to be a time mean separation or reattachment length. The distance from this center to the extremities of the band are then defined as the uncertainty bounds for these lengths. This has been illustrated in Figure 3-19 for both the lengths.

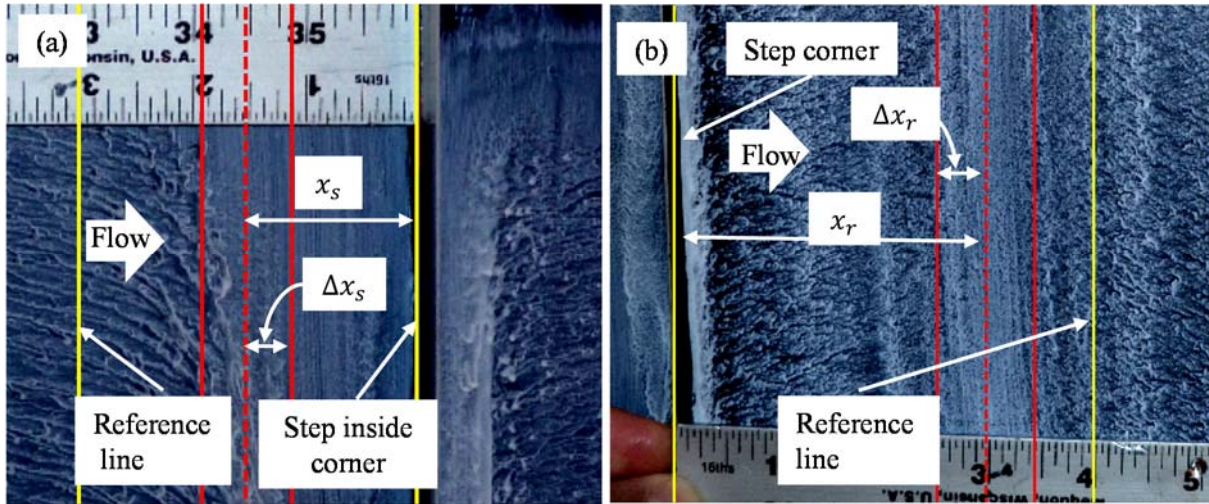


Figure 3-19 Oil flow visualization images and image analysis technique to calculate separation and reattachment lengths along with their uncertainties. (a) Upstream separation for $r/h=0\%$ forward step at $U_\infty=30\text{m/s}$, (b) Downstream reattachment for $r/h=0\%$ forward step at $U_\infty=30\text{m/s}$.

3.1.4 Single Point Surface Pressure Fluctuation Instrumentation

Pressure fluctuations were measured using a B&K 4182 probe type microphone. This has a smooth frequency response between 1 Hz and 20 kHz. The microphone (Figure 3-20) consists of a housing which contains the microphone diaphragm and the preamplifier. A stiff (stainless steel) probe tube of 1.24 mm diameter (OD) can be connected to this housing with the end of this tube installed flush with the flow surface to measure surface pressure fluctuations. Different length tubes can be used depending on the requirements of measurements to be made. There is also a flexible plastic tubing which can be attached at the end of one of the stiff probe tubes to enable measurements in locations which are otherwise difficult to reach. For the present work the measurements upstream and downstream of the step locations were made either using a 25 mm or 50 mm stiff tube depending upon accessibility of measurement locations. For the measurements on the step face a flexible tube arrangement was used.

The stiff and flexible tube measurement setup is illustrated in Figure 3-21. The measurement scheme was designed so that a single probe microphone could be used to measure pressure fluctuations at multiple locations by an experimenter in the starboard anechoic chamber (i.e. behind the test wall) without stopping the flow in the test section. The schematic on the left in Figure 3-21 illustrates the setup used to accomplish this upstream and downstream of the step. Shown in the figure is the probe microphone with a stiff probe attachment (25 mm or 50 mm long) which is inserted into a counterbore hole in the test surface. This counterbore hole has dimensions such that its smaller diameter is approximately the same as the inside diameter of the probe tube and has a length of 1mm. The larger diameter of the hole is 1.27 mm which is slightly larger than the outside diameter of the stiff probe tube. This counterbore allows the probe tube to be inserted up to the lip created by the smaller diameter hole essentially extending the probe tube's length by 1 mm. The experimenter stands in the starboard anechoic chamber and inserts the probe microphone until it hits the lip of the counterbore hole and then makes the measurement. This process is then repeated for all the measurement stations. Once the measurement for a particular location is finished the counterbore hole is sealed from inside the anechoic chamber through the use of plugs precisely made using rapid prototyping. These plugs are designed such that the diameter of their neck has a diameter of 0.74 mm with a shoulder length of 1 mm. A typical rapid prototyped plug is shown at the bottom of Figure 3-21. The setup to measure pressure fluctuations on the face of the step (Figure 3-20 right) is slightly different. This consists of a 6.4 mm long steel tube which has the same inner and outer diameter (0.75 mm and 1.24 mm respectively) as the stiff probe. This tubing is inserted in a counter bore hole on the face of the step which

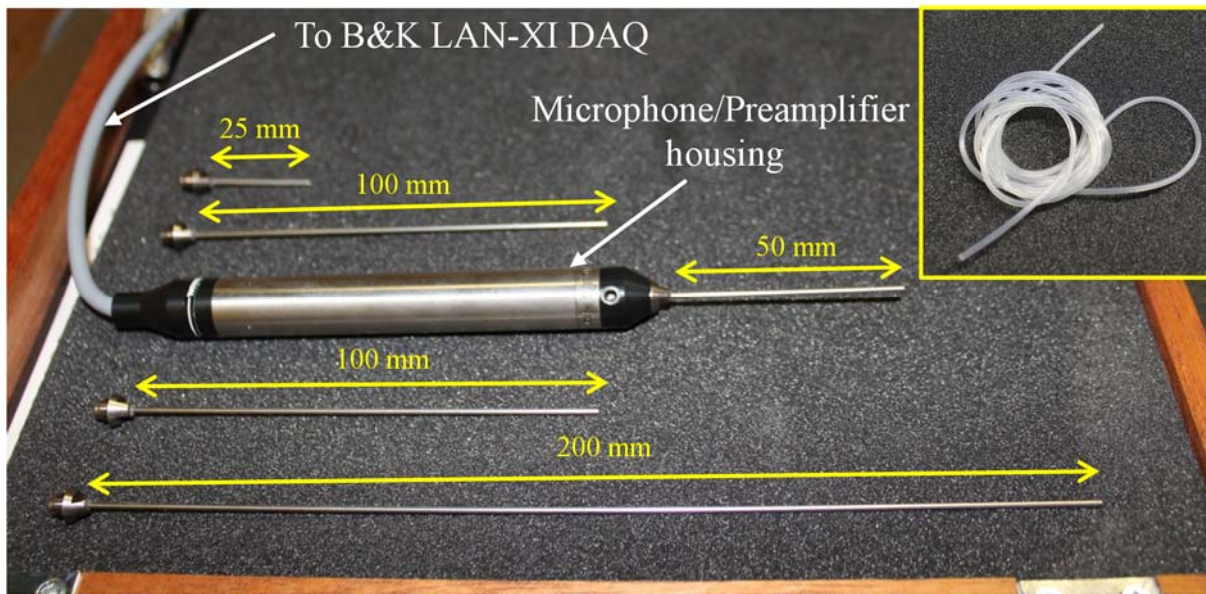


Figure 3-20 B&K 4182 probe microphone with a 50 mm long stiff probe tube attached to the microphone/preamplifier housing. Also shown are four other stiff probe tubes and a flexible tubing (inset) which can be attached to the stiff probe tube for measurement. Note that for present measurement a 25 mm and 50 mm stiff probe and the flexible tubing attached to 50 mm stiff probe were used.

has the same inner and outer diameter as that of the hole shown on the left of Figure 3-21. The aft end of this steel tube is connected to the 50 mm stiff tube of the probe microphone using a 76 mm long flexible tube as shown on the right schematic of Figure 3-21.

Measurement of pressure fluctuations were made at several locations upstream and downstream of the step as shown in Figure 3-22. Measurement locations extended from about 10 step heights upstream of the step to 20 step heights downstream. Measurement locations were logarithmically spaced to provide enhanced spatial resolution in the vicinity of the step location. Additionally, measurements were also made on the step face at points located at 0.56 and 0.69 step heights from the inside corner of the step.

The unsteady pressure signal from the probe microphone was acquired using B&K LAN-XI DAQ and was passed through B&K Nexus type 2692 to enable simultaneous near-far field measurements in the same fashion as done for the unsteady force array. The signal from the probe microphone (and B&K 4138 microphone) was sampled at 65536 Hz for 32 seconds and was then Fourier transformed to yield the auto and cross spectral densities.

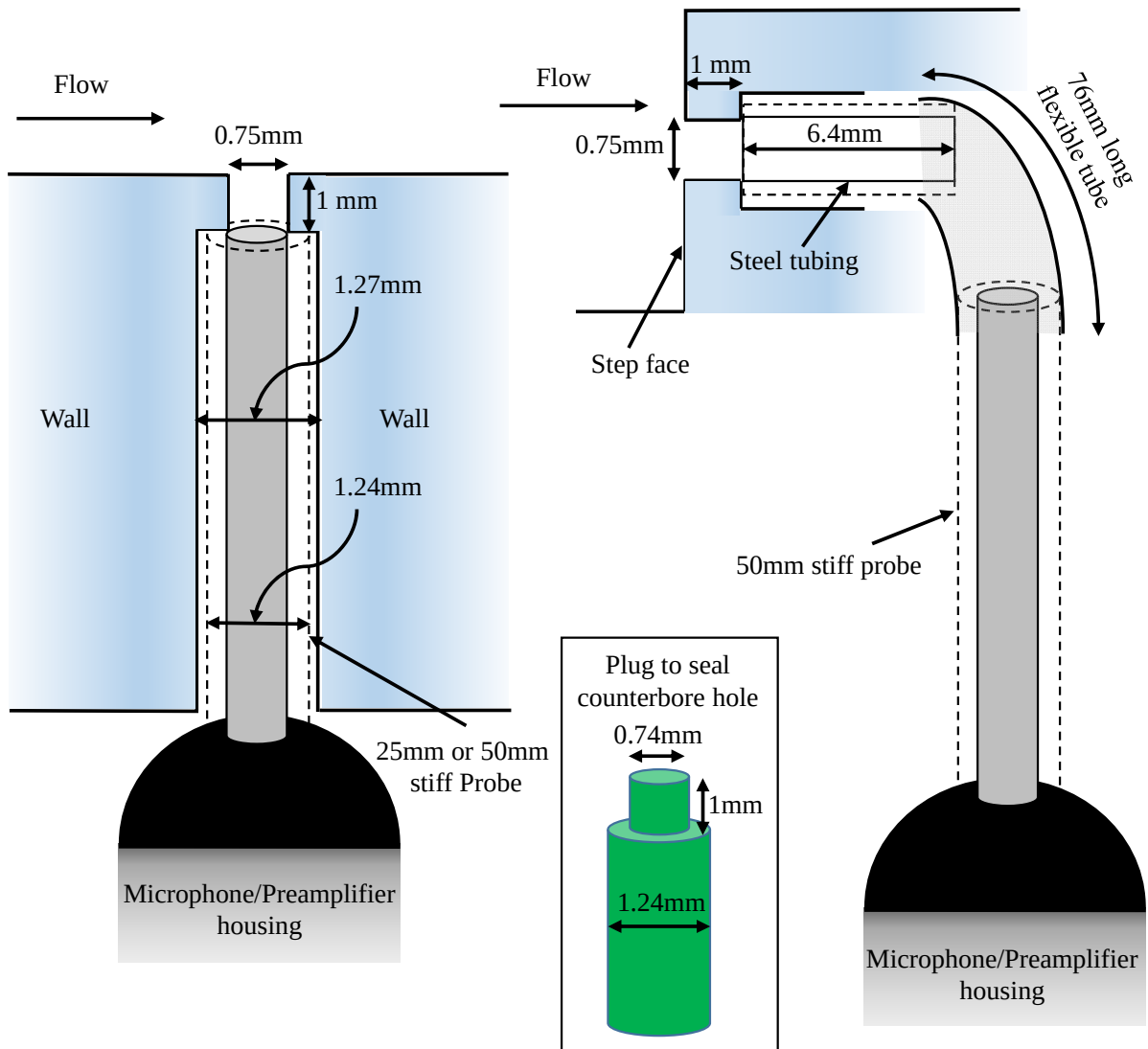


Figure 3-21 Description of setup to measure pressure fluctuations without stopping the flow using stiff (left) and flexible tube (right). Rapid prototyped plug to seal the measurement holes when they are not in use is shown at the bottom.

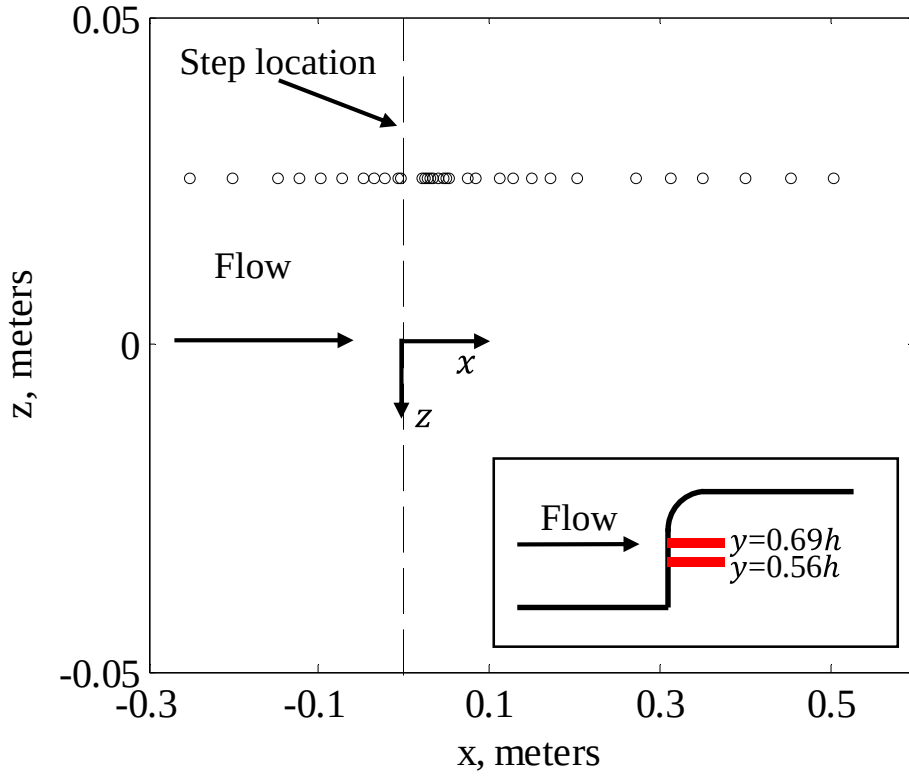


Figure 3-22 Measurement locations for wall pressure fluctuations upstream/downstream of the step and on the step face (inset).

3.1.4.1 Probe Microphone Calibration

The stiff and flexible probe microphone setups were calibrated using a comparison calibration method where the response to a sound source was compared to that of a reference microphone to the same sound source. This calibration procedure is the same as used by Smith (2008). The probe microphone calibration curve M_{cal} as a function of frequency is given by

$$M_{cal}(f) = \frac{\phi_{SM}}{\phi_{SS}S_{cal}} \quad (3-7)$$

where ϕ_{SM} is the cross spectral density measured between the input to the sound source (in Volts) and the probe microphone output (also in Volts), ϕ_{SS} is the autospectrum of the sound source input signal and S_{cal} is the sound source calibration which is given by

$$S_{cal} = \frac{\phi_{SR}}{\phi_{SS}S_{ref}} \quad (3-8)$$

where ϕ_{SR} is the cross spectral density between the speaker input signal and the reference microphone output and S_{ref} is the (constant) sensitivity of the reference microphone in Volts/Pascal. For the present calibration method the measurement microphone and the reference microphone are both exposed simultaneously to the same speaker input signal so combining the two equations the microphone calibration curve is given by

$$M_{cal}(f) = \frac{\phi_{SM} S_{ref}}{\phi_{SR}} \quad (3-9)$$

The setup used to calibrate the probe microphone is shown in Figure 3-23. The probe microphone and a reference microphone are exposed to a white noise sound source inside a B&K UA 0922 calibration coupler. The reference microphone is a B&K 4938 microphone with a 6.35 mm diameter (without safety grid). This microphone has a flat frequency response between 6 Hz and 20 kHz and a dynamic range between 30 dB and 172 dB. The microphone directly faces the diaphragm of the sound source. The sound source signal is a B&K 4188 microphone connected to a transmitter adapter UA 0920. The input voltage to the transmitter adapter/microphone assembly was generated using an NTI MR2 Minirator Audio Generator and was amplified using a Crown D60 power amplifier. After amplification the voltage signal received by the transmitter adapter/microphone assembly was a white noise signal of about $\pm 8V$ r.m.s. The input voltage signal to the sound source, output voltage response of the probe microphone and the reference microphone were all recorded simultaneously using B&K LAN-XI DAQ hardware. The signals were sampled at 65536 Hz for 32 seconds and were then Fourier transformed to yield the auto and cross spectral densities required to obtain the calibration. The sensitivity of the reference microphone (S_{ref}) required to calculate the calibration curve using equation (3-7) was measured using a pistonphone (B&K type 4228) operating at a constant frequency of 251.2 Hz. The measured sensitivity of the reference microphone was 1.55 mV/Pa.

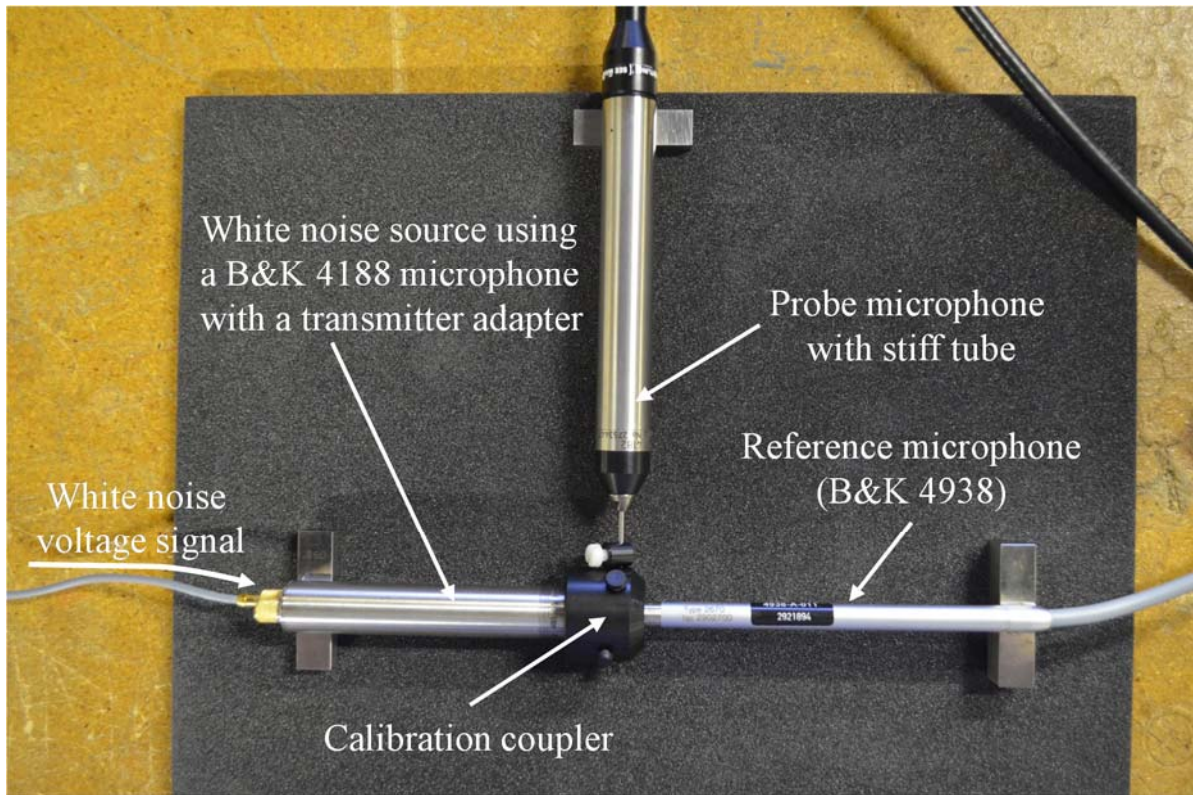


Figure 3-23 Probe microphone response calibration setup.

Magnitude calibration curves are shown in Figure 3-24. The figure shows the calibration curves for the 25 mm, 50 mm and the flexible tube arrangements. At the lowest frequencies ($f < 200$ Hz) all the calibration curves show significant uncertainty due to the inability of the transmitting microphone to produce significant sound in this range. Magnitude calibrations in this range can be safely assumed constant

and linear with frequency, respectively. Other than this, the magnitude calibration curves for the two stiff probe remain flat up to about 1 kHz after which an attenuation up to about 5 dB at the highest frequencies is seen. The calibration curve also develops a ripple like structure due to the acoustic reflections from the ends of the tube. For the flexible tube arrangement the flat response region is limited only to about 500 Hz and the attenuation at highest frequencies can be as high as 9 dB due to increased distance through which the acoustic waves must travel before reaching the microphone diaphragm.

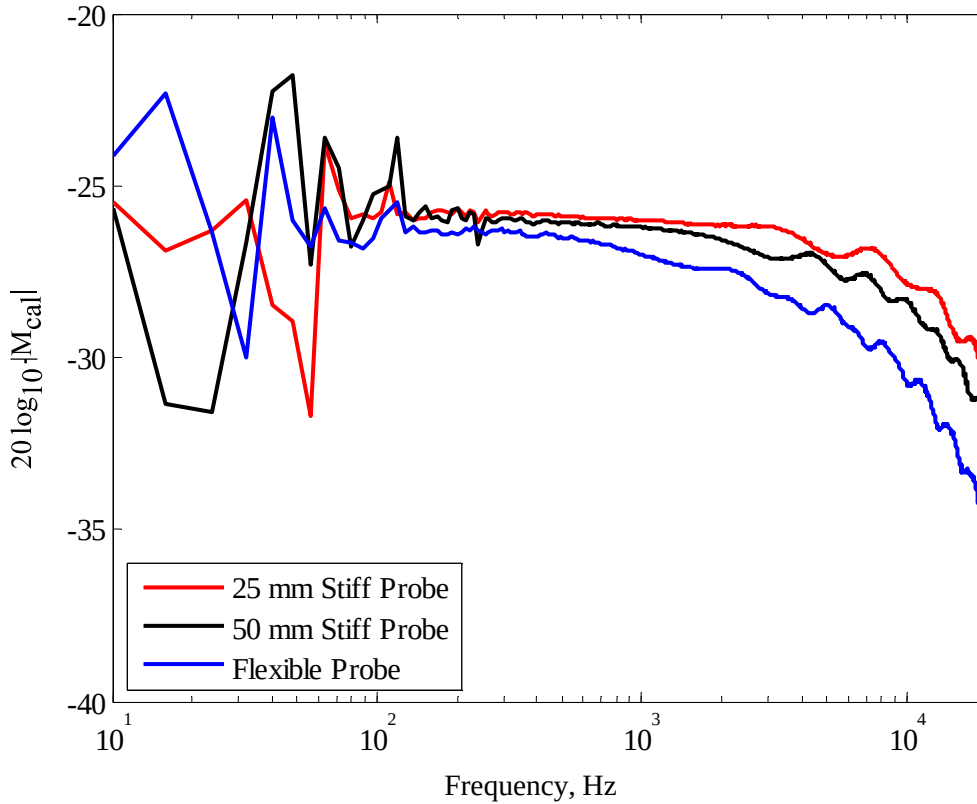


Figure 3-24 Calibration curves for probe microphone with stiff and flexible probe tube arrangement.

3.1.4.2 Background Acoustic Subtraction and Uncertainty

To remove the background acoustic contamination from the pressure signal measured by the probe microphone, produced for example by the wind tunnel fan, a reference B&K 4138 microphone was used. The location of this microphone is shown in Figure 3-25. This microphone was installed 279.4 mm (11 step heights) downstream of the step and 508 mm (20 step heights) from the measurement locations in spanwise direction. This separation is more than 5 undisturbed boundary layer thicknesses and is large enough so that any coherence between them would be a result of background acoustics rather than due to step flow pressure fluctuations. To subtract the acoustics the magnitude of cross-spectrum between measurement and reference microphone was subtracted from the autospectral density measured by the probe microphone. The statistical background of the subtraction performed in the present work has been described in more detail by Awasthi (2012).

As an example, Figure 3-26 (a) shows raw and background subtracted probe microphone spectra 7.9 step heights upstream of the unrounded step at $U_\infty = 30$ m/s. The subtracted spectrum is almost identical to the raw version at most frequencies. At lower frequencies (below about 50 Hz) the subtracted spectrum is approximately 2 dB lower. The uncertainty in the subtracted spectrum as a function of frequency is shown in Figure 3-26 (b). The uncertainty has been calculated using a procedure similar to that described in §2.2.2

for uncertainty far field sound measurements. The uncertainty does not exceed 2 dB at any frequency in this sample case.

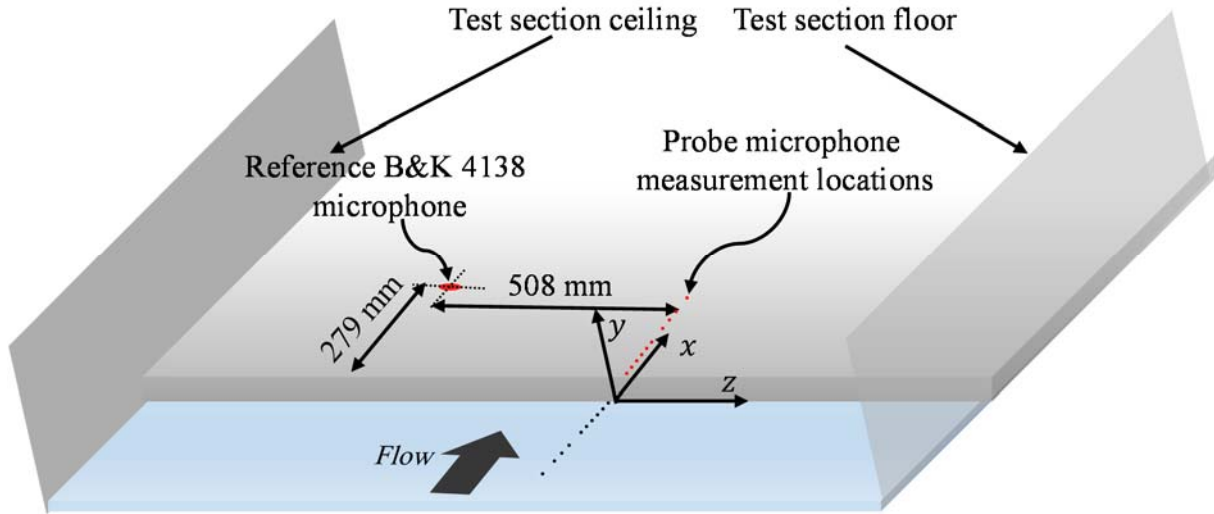


Figure 3-25 Location of reference B&K 4138 microphone (3.2 mm diameter) with respect to probe microphone measurement locations.

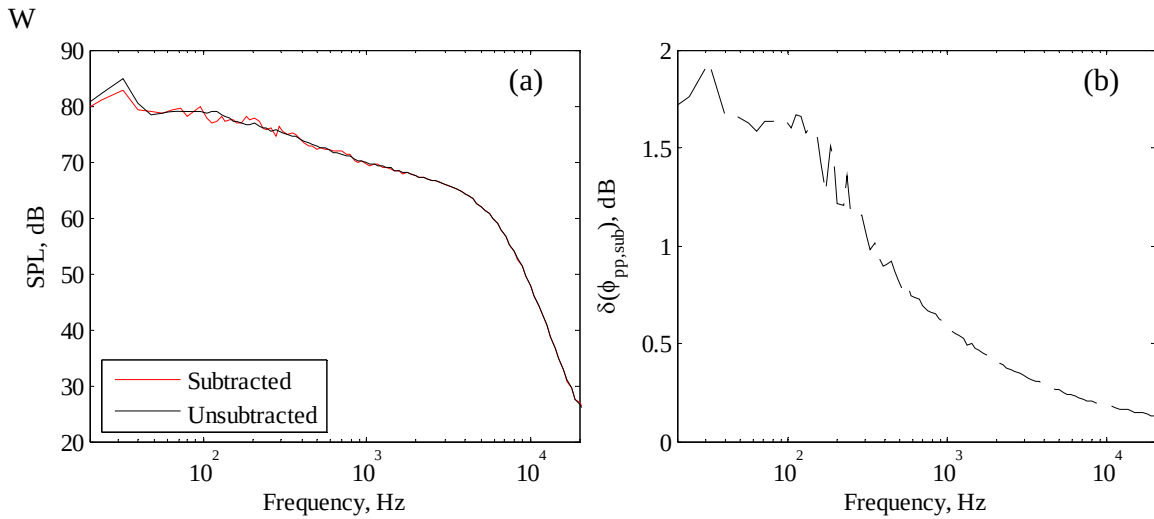


Figure 3-26 Background acoustic subtraction from probe microphone measurement. (a) Comparison between measured spectrum and background subtracted spectrum, $r/h=0\%$, $x/h=-7.9$, $U_\infty=30\text{m/s}$, (b) Uncertainty in the subtracted spectra in dB as a function of frequency.

3.1.5 Unsteady Surface Force Measurement Instrumentation

The unsteady surface force represents the fundamental acoustic source in the limit of zero Mach number. Therefore one of the goals of the present work was to obtain a measurement of this unsteady force in the vicinity of the forward step configurations. Since a microphone embedded in the surface measures the unsteady pressure and force is just the pressure integrated over an area, a microphone array which would

provide spatial averaging of unsteady pressure can be used to obtain a measure of the unsteady surface force. So, in present work a 24 microphone array with linear spacing between the microphones was installed in the vicinity of rounded forward steps to obtain unsteady surface forces. This array and its setup will now be described.

Unsteady forces at select locations in the vicinity of the forward step were measured using an evenly spaced array of 24 microphones each separated by a center to center distance of $d = 14.9$ mm. The array consisted of 23 B&K 4190 microphones used without their factory made salt and pepper shaker safety grids, the diameter of the exposed diaphragm being 11.1 mm. These microphones have a flat frequency response between 3.2 Hz and 20 kHz (± 2 dB) and a dynamic range of 14.6 to 146 dB. In addition a single B&K 4138 microphone was used at the array center position. The B&K 4138 has a flat frequency response from 6.5 Hz and 140 kHz (± 2 dB) and a dynamic range of 52.2 to 168 dB. The sensitivity of each microphone used in the array was measured using a B&K type 4228 pistonphone operating at 251.2 Hz. The B&K 4138 microphone was used with its safety grid on since the small size of this microphone is expected to generate little self-noise. The diameter of the sensitive portion of this microphone is 3.2 mm. Figure 3-27 shows a photograph of the array taken when the array was installed in the vertical face of the step in spanwise orientation. The array local coordinate system (x_A, y_A, z_A) is also shown in the figure along with the flow coordinate system (x, y, z). The origin of this coordinate system is located at the center of the first microphone at the extreme left and lies at $z = -166$ mm and $y = 9.5$ mm. The streamwise coordinate x_A is defined positive in the flow direction, the wall-normal coordinate y_A is defined positive away from the wall and the spanwise coordinate z_A completes the right hand rule.

The array was used to measure the spanwise spatially averaged unsteady pressure (equivalent to unsteady force per unit area) at three separate streamwise location for each rounded forward step. These measurement locations with respect to the flow coordinate system have been shown in Figure 3-28. As seen in the figure the array was installed on the face of the step ($x/h = 0$) such that the center of each microphone in the array was 9.5 mm ($y = 0.38h$) from the inside corner of the step. On the downstream side the two measurement locations chosen were $x = 1.3h$ and $x = 3.1h$. In each spanwise configuration the distance of the microphone at which the origin of the coordinate system depicted in Figure 3-27 lies at $z = -166$ mm in the spanwise direction. The center of the farthest microphone in the spanwise array lies at $z = 179$ mm from the origin. The distance between the centers of the microphones at the extremities of the array is approximately 345 mm which is about 13.6 step heights.

The array was also positioned in streamwise orientation downstream of the forward step as shown in Figure 3-29. When placed in this orientation the centers of the most upstream microphone and most downstream microphones are located 2.4 step heights and 16 step heights downstream of the vertical face of the step respectively. The streamwise axis which coincides with the centers of the microphone lies at $z = 235$ mm in the spanwise direction.

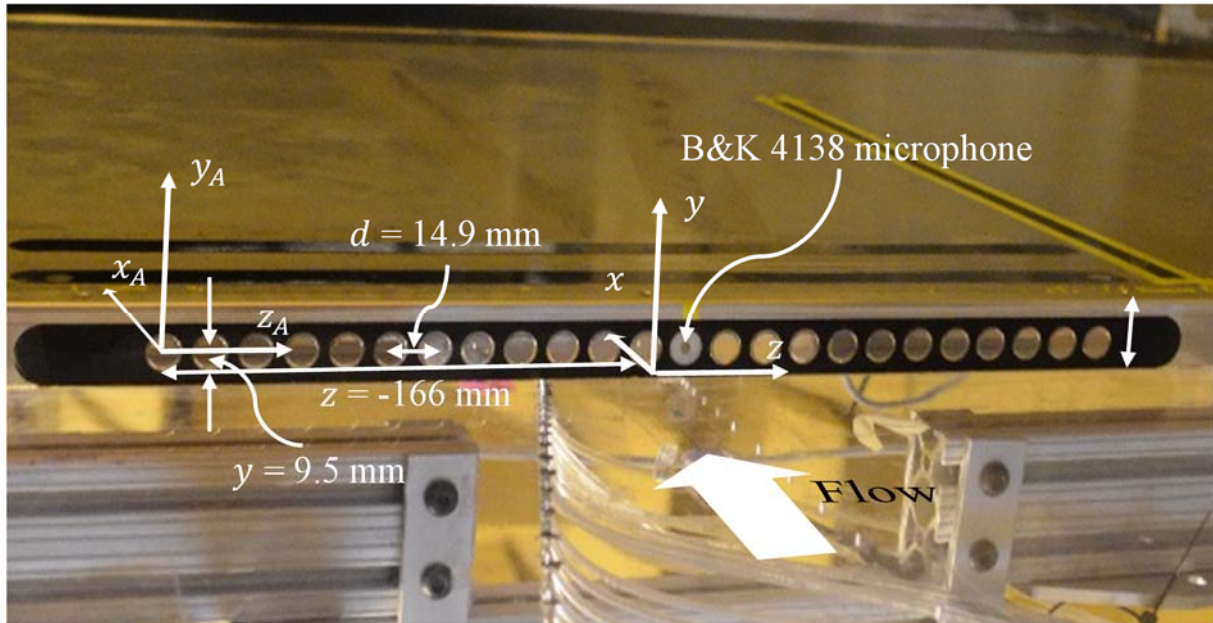


Figure 3-27 24 microphone linear surface pressure array used to measure unsteady force in the vicinity of rounded forward steps. The picture here shows the array installed in the vertical face of the step. Also shown is the array local coordinate system and flow coordinate system.

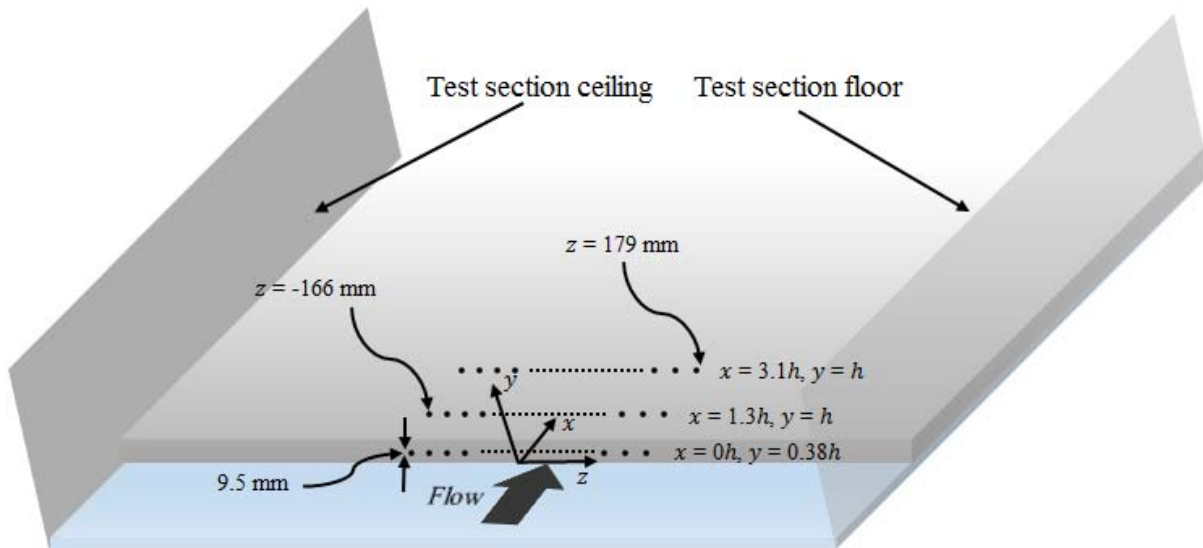


Figure 3-28 Measurement locations of spanwise linear microphone array in the vicinity of rounded forward steps.

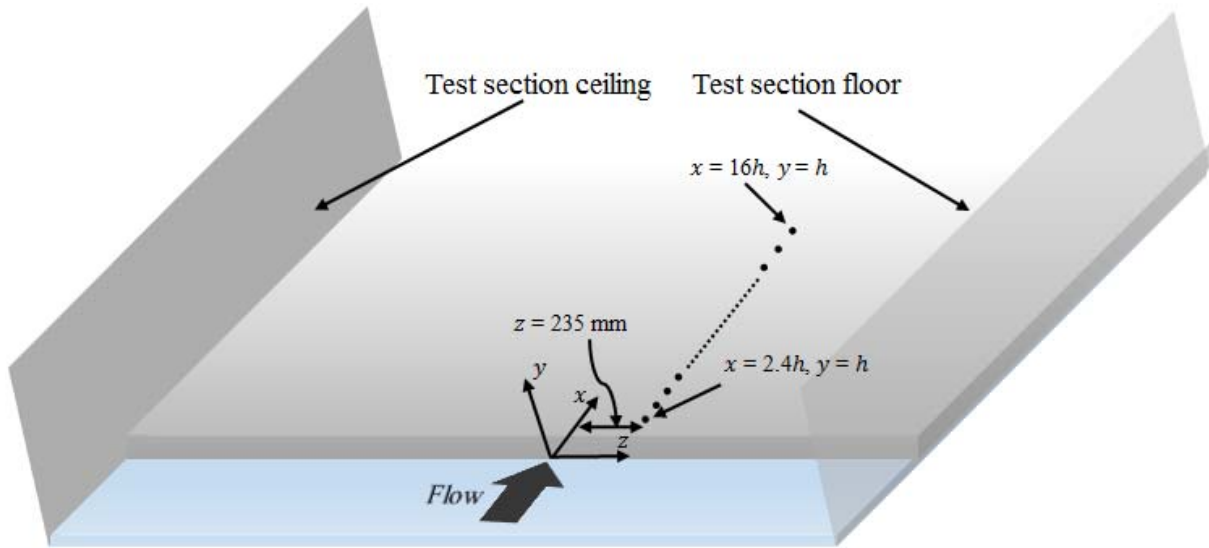


Figure 3-29 Measurement location of streamwise linear microphone array downstream of rounded forward steps.

The array was also used to measure the spatially averaged unsteady pressure for the undisturbed boundary layer flow. Figure 3-30 shows the streamwise and spanwise array locations for these measurements. In spanwise orientation the array was installed at $x = h$ and in streamwise orientation the most upstream and most downstream microphones are located at $x = 6.5h$ and $x = 20.1h$ respectively. The streamwise axis which coincides with the center of the microphones when the array is in streamwise orientation is located at $z = 332$ mm.

For both the undisturbed boundary layer flow and flow over rounded forward steps the measurements with the array were made for free stream velocities between $U_\infty = 20$ -40 m/s in increments of 5 m/s.

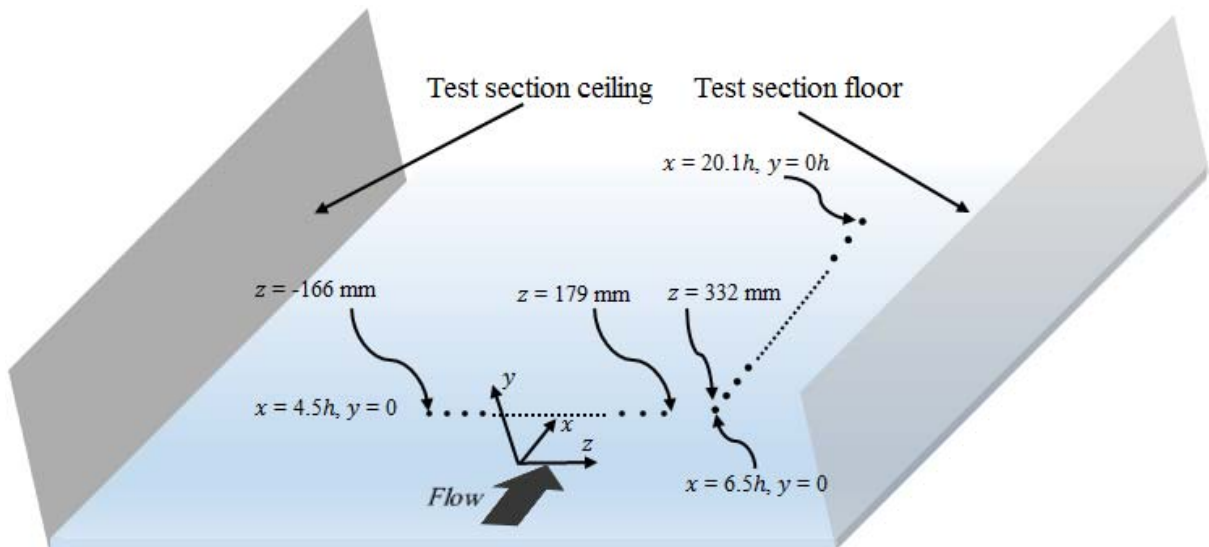


Figure 3-30 Measurement locations of linear microphone array for undisturbed boundary layer flow.

The microphone array holder was machined from a 432 mm x 19.1 mm x 70 mm Delrin block with rounded edges as shown in Figure 3-31. This holder consists of 24 precisely drilled holes each 12.1 mm

diameter corresponding to the diameter of the B&K 4190 microphone without safety grid (the 11.1-mm diameter diaphragm of this microphone is surrounded by a 1 mm thin metallic ring) bringing the diameter of the entire face to 12.1 mm. In one of these holes a rapid prototype sleeve of outside diameter of 12.1 mm and inside diameter of 3.2 mm is inserted to accommodate the smaller B&K 4138 microphone as shown in the figure. The array holder also included tapped holes to the side of each microphone hole for set screws to hold the microphones in place.

To install the array in the step surface slots of equal dimensions as the array holder were created on upper surface of the step and the step face. The slot for the array on the face is formed by the leading edge support bars which were earlier described in §3.1.1 and shown in Figure 3-11. Figure 3-32 (a) shows the array installed downstream of the step at $x/h = 1.3$ from the flow side. Once the array is mounted flush with the surface, the seams between the array holder and the step surface were covered with a 40 micron thin tape to avoid any flow leakage. The figure also shows the plug at the center of the array which accommodates the B&K 4138 microphone. The slots which are not in use during the measurements are plugged with plugs which are the same dimension as the array holder and are also made of Delrin.

Figure 3-32 (b) shows a photograph of the array holder and one of the plugs taken from the back side of the step assembly. The figure shows all 24 microphones installed in the array holder along with the Nylon set screws which hold them in place. The array holder and plugs are mounted to back side of the step surface using corner brackets which are also shown in the figure. Note that each microphone in the array also consists of a preamplifier which is attached to it. The B&K 4138 microphone uses a B&K type 2670 preamplifier while each of the B&K 4190 microphones use a B&K type 2669 preamplifier.

The signal from the array microphones combination were acquired using four B&K LAN-XI DAQ units which were connected in tandem so that the time series pressure was acquired simultaneously for all 24 microphones. The pressure signal from each microphone was sampled at a sampling rate of 65536 Hz for 32 seconds. The resulting time series data was Fourier transformed to yield the auto spectral density of each microphone pressure signal and cross spectral density between each microphone.

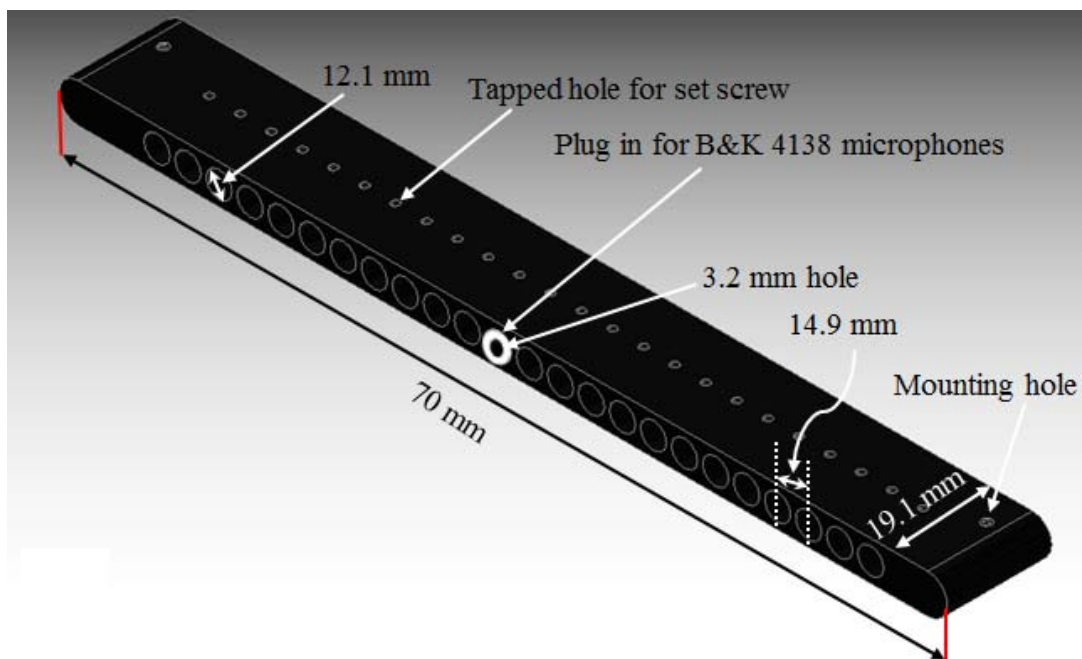


Figure 3-31 24 Microphone array holder.

Background acoustic cancellation for each microphone in the array was provided by the same B&K 4138 microphone which was used to subtract acoustics from the probe microphone signal. The subtraction method is also the same and details on it can be found in Awasthi *et al.* (2014). Figure 3-33 shows the effect of the subtraction process on the spectra measured by the two types of microphone used in the array for undisturbed boundary layer flow at $U_\infty = 30$ m/s. The data shown for the B&K 4192 microphone (Figure 3-33 (a)) is from the most upstream microphone when the array was in streamwise orientation. Looking at the figure it can be seen that the subtraction only effects the spectra below about 100 Hz in each case where the contribution from facility noise is expected to be significant. In each case the subtraction results in reduction of the background noise by at most 2-3 dB.

Figure 3-34 shows the effect of background subtraction on the spectra measured at $x/h = 3.1$ for each rounded steps by one of the B&K 4190 microphones in the spanwise array. Unlike the undisturbed boundary layer case the acoustic subtraction does not affect the measured spectra (except for a slight reduction in case of $r/h = 25\%$ at low frequency). The reason for this is that as shown by Awasthi *et al.* (2014) the pressure fluctuations downstream of the step can exceed the undisturbed boundary layer levels by as much as 30 dB. Therefore, the boundary layer pressure for step flows dominates the measured spectra and hence no acoustic contribution from the background noise is seen in spectra of Figure 3-34. Furthermore, these results are actually encouraging because this means that any acoustic contribution from the events within the boundary layer which might contribute to the far field sound radiation (and hence are part of the step noise source) are not being subtracted out.

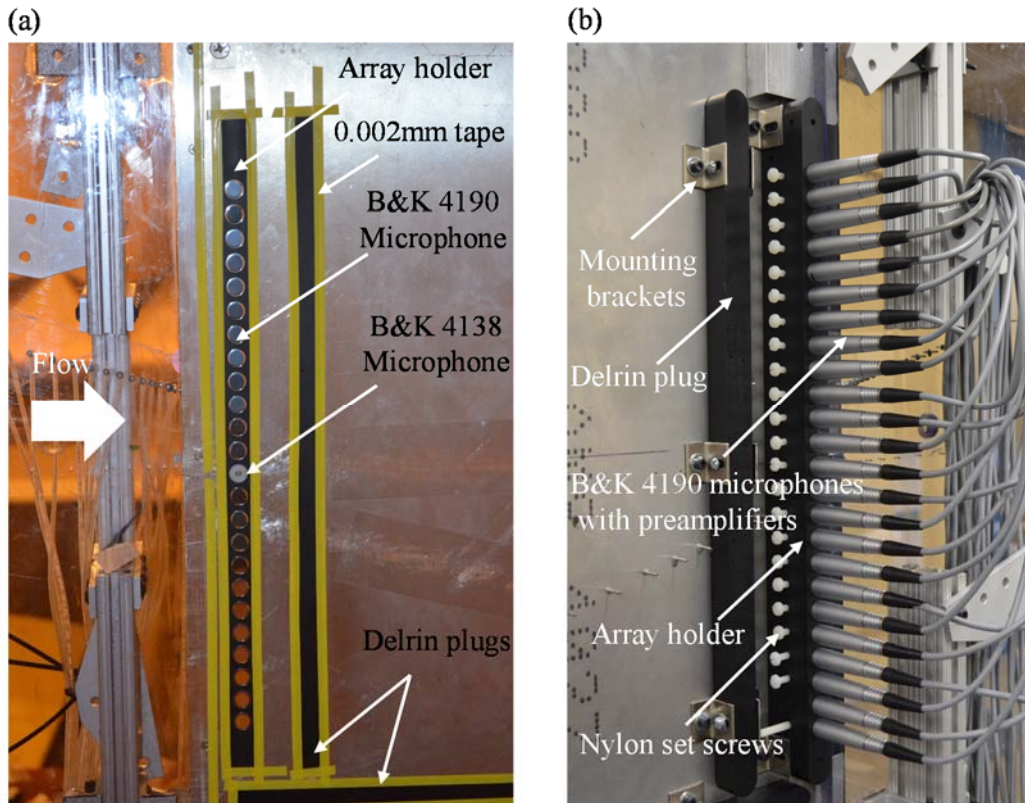


Figure 3-32 24 Microphone linear surface pressure array used to measure unsteady surface force on rounded forward steps. (a) Flow side view of the array installed in spanwise orientation 1.3 step heights downstream of the step, also shown are the Delrin blocks used to plug the array slots when they are not in use and 40 micron tape used to cover the seams around the array holder. (b) Array holder with B&K 4190 microphones installed seen from the starboard side anechoic chamber.

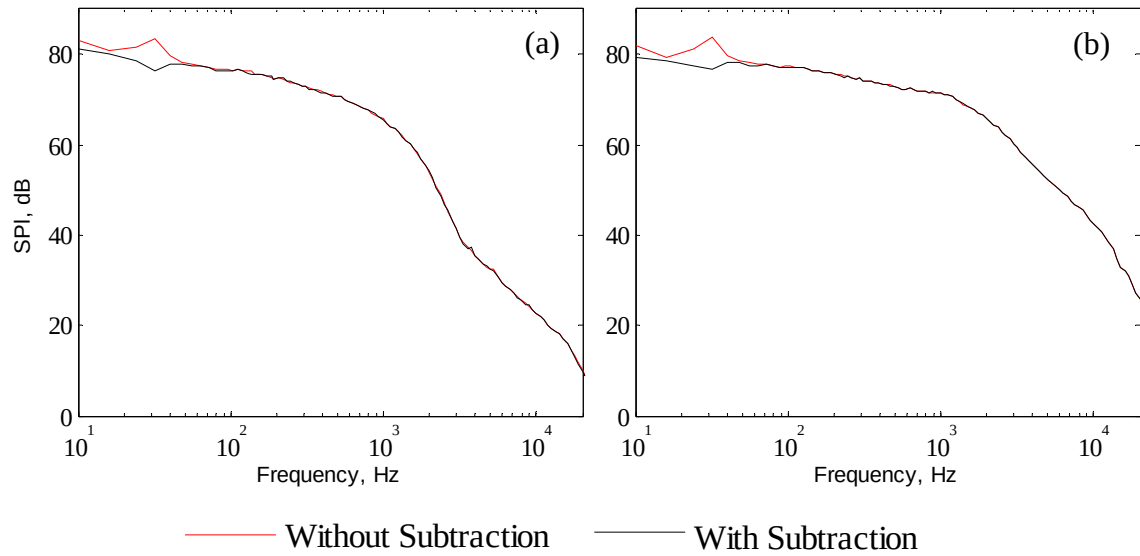


Figure 3-33 Surface pressure spectra measured by the two different array microphones with and without background subtraction for undisturbed boundary layer flow at $U_{\infty} = 30$ m/s. (a) B&K 4190 microphone and (b) B&K 4138 microphone. Data shown was measured with the array in streamwise orientation.

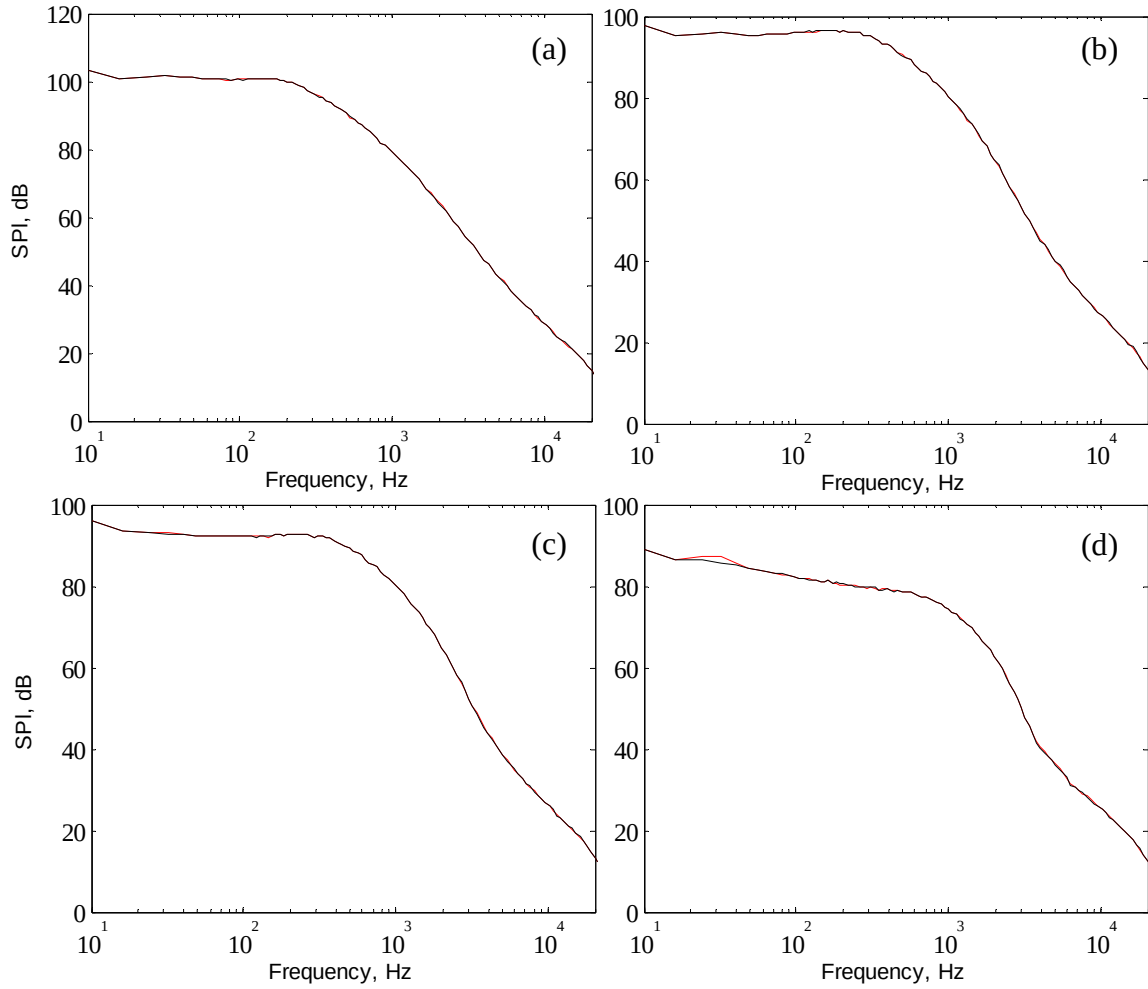


Figure 3-34 Effect of background subtraction on spectra measured downstream of rounded forward steps at $x/h = 3.1$ for $U_\infty = 30$ m/s. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$ and (d) $r/h = 25\%$. Data in each case is taken from the pressure measured by the microphone closest to the ceiling of the test section when the array is in spanwise orientation.

3.1.5.1 Using Individual Microphone as an Area Averaged Pressure Sensor

The microphones used in this array have a large diameter which results in them acting as a low pass wavenumber filter. This effectively results in these microphones having an attenuated response at higher frequencies. The purpose of this section is to assess the response of the individual microphones used in the array through an analytical approach. Additionally, we will perform an analysis similar to the one presented here on the entire microphone array in §3.2.4 in order to estimate the limitations of the microphone array when it is used as an unsteady force sensor. Thus, the following analysis also serves as a preamble to the analysis done in §3.2.4.

It is well known that the ability of a finite size microphone to resolve higher frequencies within the flow is limited due to spatial filtering of the wavenumber-frequency spectra of surface pressure fluctuations by the sensitivity function of its diaphragm (Blake and Chase (1970)). To understand this spatial filtering it is convenient to consider the response of the microphone in the wavenumber-frequency domain where the wavenumber is defined as

$$\kappa = 2\pi/\mathcal{L} \quad (3-10)$$

where $\mathcal{L}$ is some length scale associated with the flow. Note that the wavenumber can be a vector (called wavevector) since length scales can exist along the three spatial dimensions. In that case the wavevector can be written as

$$\vec{\kappa} = \kappa_x \hat{i} + \kappa_y \hat{j} + \kappa_z \hat{k} \quad (3-11)$$

where κ_x , κ_y and κ_z are the wavenumbers in the streamwise, wall-normal and spanwise direction respectively. Now, for an ideal pressure transducer (with infinitesimally small diameter) whose response does not suffer from spatial averaging effects or is frequency dependent the measured spectral density of surface pressure fluctuations as a function of frequency is given by

$$\phi(\omega) = \int_{-\infty}^{\infty} \int_{-\infty}^{\infty} \Phi(\kappa_x, \kappa_z, \omega) d\kappa_x d\kappa_z \quad (3-12)$$

where Φ is the wavenumber-frequency spectrum of surface pressure which is a function of the length scales (i.e. wavenumbers) in both the streamwise and spanwise direction as well as the frequency of these length scales and, ω is the radial frequency given by $\omega = 2\pi f$. Now if a finite transducer such as a microphone is being used to measure the pressure fluctuations the kernel of the above integral will also contain a microphone response function within it. This response function is a characteristic of the particular microphone being used to measure the surface pressure fluctuations. In presence of this microphone the measured frequency spectrum is given by

$$\tilde{\phi}(\omega) = \int_{-\infty}^{\infty} \int_{-\infty}^{\infty} \Phi(\kappa_x, \kappa_z, \omega) |H(\kappa_x, \kappa_z, \omega)|^2 d\kappa_x d\kappa_z \quad (3-13)$$

where $\tilde{\phi}(\omega)$ now is the spectral density obtained as a result of the spatial filtering action of the microphone response function $H(\kappa_x, \kappa_z, \omega)$ on the wavenumber-frequency spectra of pressure fluctuations. It must be noted that the microphone response function is dependent on both wavenumber and frequency. The dependency on frequency can be removed if the microphone has a calibrated frequency response and the pressure sensed by the microphone is averaged over an area. This latter requirement is met if the time domain spatial sensitivity function of a microphone which we call $S(x, z)$ satisfies

$$\int_{-\infty}^{\infty} \int_{-\infty}^{\infty} S(x, z) dx dz = 1 \quad (3-14)$$

The frequency responses of the microphones used in the present work (B&K 4190 and B&K 4138) are flat within the range of interest and thus we assume that the spatial response of the microphones used in present work is separable from the temporal response upon the satisfaction of the criteria in equation (3-14). Then the integral in equation (3-13) becomes

$$\tilde{\phi}(\omega) = \int_{-\infty}^{\infty} \int_{-\infty}^{\infty} \Phi(\kappa_x, \kappa_z, \omega) |H(\kappa_x, \kappa_z)|^2 d\kappa_x d\kappa_z \quad (3-15)$$

The microphone response function $H(\kappa_x, \kappa_z)$ can be obtained using the Fourier transform of its spatial sensitivity function. This spatial sensitivity function for a circular microphone like the ones used in the present work has been previously obtained by Farabee and Geib (1991) who modified an earlier version of the spatial sensitivity function presented by Blake and Chase (1970). This modified spatial sensitivity function given by the former is

$$\frac{S(r/R)}{S_0} = \frac{J_0(\alpha) - J_0(\alpha r/R)}{J_0(\alpha) - 1} \quad (3-16)$$

where $S(r/R)$ is the spatial sensitivity function of a circular microphone with radius R as a function of the radial coordinate r which is defined as 0 at the center of the microphone and equal to R at the periphery, $J_m()$ the m^{th} order Bessel function of first kind and α is a constant with a value of 2.675 which was obtained by Farabee and Geib (1991) by fitting the above relationship to experimental data. Note that as stated by the authors this value of α is about 10% less than that obtained previously by Blake and Chase (1970). The term S_0 above is a constant which depends on the radius of the microphone and is used to satisfy the criteria specified in equation (3-14) that ensures that the microphone spatial and temporal responses are separable. The constant S_0 can be found by combining equations (3-14) and (3-16) as

$$S_0 \int_0^{2\pi} \int_0^R S\left(\frac{r}{R}\right) r dr d\theta = 1 \quad (3-17)$$

where we have switched the integral from Cartesian coordinates to polar coordinates to be consistent with the radially symmetric microphone spatial sensitivity function. Then, solving the above equation for S_0 yields the following expression

$$S_0 = \frac{1 - J_0(\alpha)}{\pi R^2 J_2(\alpha)} \quad (3-18)$$

Substituting equation (3-18) in (3-16) yields the following expression for the spatial sensitivity of a circular microphone

$$S(r) = \frac{1 - J_0(\alpha)}{\pi R^2 J_2(\alpha)} \left[\frac{J_0(\alpha) - J_0\left(\frac{\alpha r}{R}\right)}{J_0(\alpha) - 1} \right] \quad (3-19)$$

We can now Fourier transform this expression to obtain the microphone wavenumber response function. The Fourier transform definition which will be used to obtain the microphone wavenumber response function and in future discussion as well is given by

$$F(\kappa_x, \kappa_z) = \int_{-\infty}^{\infty} \int_{-\infty}^{\infty} f(x, z) e^{-i(\kappa_x x + \kappa_z z)} dx dz \quad (3-20)$$

where $f(x, z)$ is some generic function of spatial variables x and z and $F(\kappa_x, \kappa_z)$ is its Fourier transform in the wavenumber space. The inverse pair of this Fourier transform is given by

$$f(x, z) = \frac{1}{2\pi} \int_{-\infty}^{\infty} \int_{-\infty}^{\infty} F(x, z) e^{i(\kappa_x x + \kappa_z z)} d\kappa_x d\kappa_z \quad (3-21)$$

Then using the definition of Fourier transform the microphone wavenumber response function is given by

$$H(\kappa_x, \kappa_z) = \int_{-\infty}^{\infty} \int_{-\infty}^{\infty} \frac{1 - J_0(\alpha)}{\pi R^2 J_2(\alpha)} \left[\frac{J_0(\alpha) - J_0\left(\frac{\alpha r}{R}\right)}{J_0(\alpha) - 1} \right] e^{-i(\kappa_x x + \kappa_z z)} dx dz \quad (3-22)$$

Since the kernel of above integral is a radially symmetric function the above Fourier transform can be obtained using Hankel transform which yields the following expression for the microphone response function

$$H(|\kappa|) = 2\pi \int_0^R \frac{1 - J_0(\alpha)}{\pi R^2 J_2(\alpha)} \left[\frac{J_0(\alpha) - J_0\left(\frac{\alpha r}{R}\right)}{J_0(\alpha) - 1} \right] J_0(2\pi|\kappa|r) r dr \quad (3-23)$$

where $\kappa = \sqrt{\kappa_x^2 + \kappa_z^2}$ is the magnitude of the wave-vector. Solving the above integral finally leads to the wavenumber response function of a single circular transducer as

$$H(|\kappa|) = \frac{2\alpha[|\kappa|R J_0(|\kappa|R) J_1(\alpha) - \alpha J_0(\alpha) J_1(|\kappa|R)]}{|\kappa|R J_2(\alpha) [\alpha^2 - |\kappa|^2 R^2]} \quad (3-24)$$

Figure 3-35 shows the calculated wavenumber response function as a function of wave-vector magnitude for the two microphones used in the linear surface pressure array. It can be seen that for both the microphones the response is flat at lower wavenumbers and equal to 1. The wavenumber range for which the flat response is seen is larger for B&K 4138 microphone because of its capability to resolve higher wavenumbers due to smaller diameter. The attenuation of the response for both microphones becomes significant for wavenumbers larger than $|\kappa| = 2\pi/d$ where d is the microphone diameter. At wavenumbers higher than those corresponding to the microphone diameter the response shows attenuation and develops side lobes with nulls occurring at wavenumbers equal to $|\kappa| = n2\pi/d$ where n is an integer multiple.

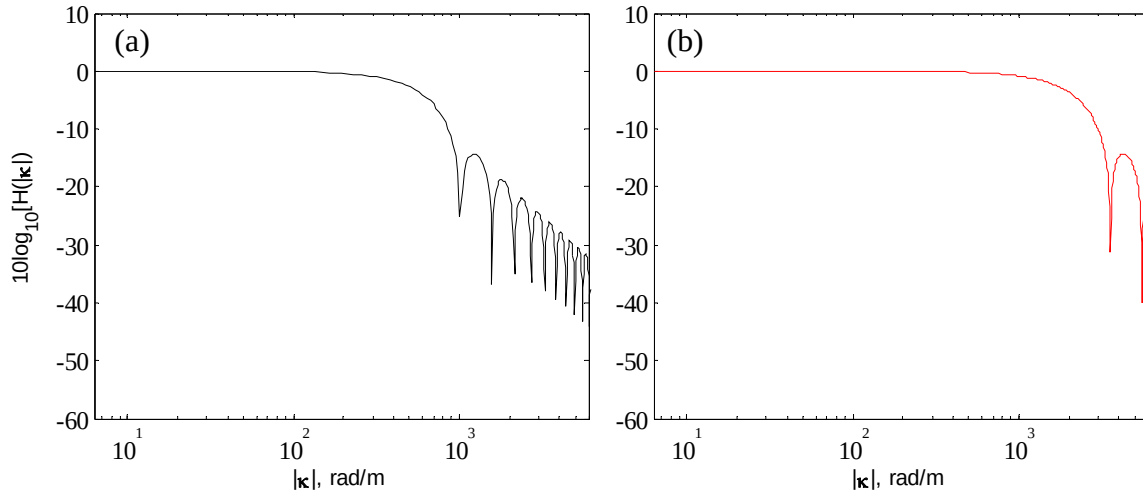


Figure 3-35 Wavenumber response function for the microphones used in the linear surface pressure array. (a) B&K 4190 microphone, 11.1 mm diameter and (b) B&K 4138, 3.2 mm diameter.

The wavenumber response function of a microphone essentially acts as a low pass filter on the wavenumber-frequency pressure spectrum of the flow. As evident in Figure 3-35 this filter function has a lower wavenumber cut-off for a larger diameter microphone. With the knowledge of the filter function for a microphone and a wavenumber-frequency spectrum of pressure the frequency spectra that would be measured by a microphone can be obtained using equation (3-15). However, the wavenumber-frequency spectrum is difficult to measure directly and in most cases it is the frequency spectra of pressure which is measured. There are nonetheless several models of undisturbed boundary layer wavenumber-frequency spectrum which have been proposed in the past. Some examples of these models include Corcos (1964), Efimtsov (1982), Chase (1980, 1987) and Smol'yakov and Tkachenko model (1991). Graham (1997)

provides an in depth review of these models. The wavenumber behavior of these models is different and largely depends on the assumptions made in deriving the model. The exact quantitative behavior of these models is not of primary interest here since the ultimate goal of using these models is to pass them through different sensor response function and compare their response to determine the limitations of those sensors. Let us then examine the qualitative behavior of some of these models. We will consider four different models and their qualitative behavior in the discussion to follow. The formulation of these models is based on the form provided by Graham (1997). These four models are Corcos (1964), Ffowcs Williams (1982), Chase (1987) and Smol'yakov and Tkachenko (1991).

Corcos (1964) model is given by

$$\Phi'(\kappa_x, \kappa_z, \omega) = \frac{4\alpha_1\alpha_3}{\left[\alpha_1^2 + \left(\frac{U_c\kappa_x}{\omega} - 1\right)^2\right]\left[\alpha_3^2 + \left(\frac{U_c\kappa_z}{\omega}\right)^2\right]} \quad (3-25)$$

This model contains two constants α_1 and α_3 whose values are chosen to be 0.1 and 0.77 respectively based on Graham's (1997) suggestion. In the above equation $\Phi'(\kappa_x, \kappa_z, \omega)$ is the wavenumber-frequency spectrum normalized on single point frequency spectrum of pressure $\phi(\omega)$, convection velocity U_c and frequency ω . The normalized form of the wavenumber-frequency spectrum is given by

$$\Phi'(\kappa_x, \kappa_z, \omega) = \frac{\Phi(\kappa_x, \kappa_z, \omega)\omega^2}{\phi(\omega)U_c^2} \quad (3-26)$$

The original Ffowcs Williams (1982) model is derived analytically and contains many unknowns which need to be determined experimentally. Here we use a version of this model derived by Hwang and Geib (1984) which is given by

$$\Phi'(\kappa_x, \kappa_z, \omega) = \left(\frac{U_c|\boldsymbol{\kappa}|}{\omega}\right)^2 \frac{4\alpha_1\alpha_3}{\left[\alpha_1^2 + \left(\frac{U_c\kappa_x}{\omega} - 1\right)^2\right]\left[\alpha_3^2 + \left(\frac{U_c\kappa_z}{\omega}\right)^2\right]} \quad (3-27)$$

With the values of constants the same as those for Corcos (1964) model. Note that as mentioned by Graham (1997) the applicability of this model is limited to lower wavenumbers since it does not satisfy the requirement that

$$\int_{-\infty}^{\infty} \int_{-\infty}^{\infty} \frac{\Phi(\kappa_x, \kappa_z, \omega)}{\phi(\omega)} d\kappa_x d\kappa_z = 1 \quad (3-28)$$

The Smol'yakov and Tkachenko (1991) model is given by

$$\Phi'(\kappa_x, \kappa_z, \omega) = 0.974A(\omega)h(\omega)[F(\boldsymbol{\kappa}, \omega) - \Delta F(\boldsymbol{\kappa}, \omega)] \quad (3-29)$$

where the functions $A(\omega)$ and $h(\omega)$ are given by

$$h(\omega) = \left[1 - \frac{m_1 A}{6.515\sqrt{G}}\right]^{-1}, \quad A(\omega) = 0.124 \left[1 - \frac{U_c}{4\omega\delta^*} + \left(\frac{U_c}{4\omega\delta^*}\right)^2\right]^{1/2} \quad (3-30)$$

where δ^* is the displacement thickness and,

$$m_1 = \frac{1 + A^2(\omega)}{1.025 + A^2(\omega)} \quad G = 1 + A^2(\omega) - 1.005m_1 \quad (3-31)$$

The functions F and ΔF are given by

$$F(\mathbf{\kappa}, \omega) = \left[A^2(\omega) + \left(1 - \frac{\kappa_x U_c}{\omega} \right)^2 + \left(\frac{\kappa_z U_c}{6.45\omega} \right)^2 \right]^{-3/2} \quad (3-32)$$

$$\Delta F(\mathbf{\kappa}, \omega) = 0.995 \left\{ 1 + A^2(\omega) + \frac{1.005}{m_1} \left[\left(m_1 - \frac{\kappa_x U_c}{\omega} \right)^2 + \left(\frac{\kappa_z U_c}{\omega} \right)^2 - m_1^2 \right] \right\}^{-3/2} \quad (3-33)$$

The Chase (1987) model which will be used in the present work is a slightly modified version of the model first proposed by him in Chase (1980) which did not account for the acoustic region of the wavenumber spectra and did not produce a wavenumber white shape at lower wavenumbers observed in experiments (Graham (1997)). This revised model is given by

$$\Phi'(\kappa_x, \kappa_z, \omega) = \frac{(2\pi)^3 \rho^2 u_\tau^2}{\phi(\omega) [K_+^2 + (b\delta)^{-2}]^{5/2}} \left[C_M \kappa_x^2 + C_T |\mathbf{\kappa}|^2 \frac{K_+^2 + (b\delta)^{-2}}{|\mathbf{\kappa}|^2 + (b\delta)^{-2}} \right] \quad (3-34)$$

where the term K_+ is given by

$$K_+^2 = \frac{(\omega - U_c \kappa_x)^2}{(h_o u_\tau)^2} + |\mathbf{\kappa}|^2 \quad (3-35)$$

b , C_T , C_M and h_o are all constants with values of 0.75, 0.0047, 0.1553 and 3 respectively. Even though these are adjustable constants, the values used in present work are the same as recommended by Chase (1987). The single point frequency spectrum of pressure $\phi(\omega)$ which is used in the Chase model and to normalize the rest of the wavenumber-frequency models is obtained using the model also suggested by Chase (1987). The frequency spectrum model is given by

$$\phi(\omega) = \frac{4\pi^2 h_o \rho^2 u_\tau^4}{3\omega(1 + \mu_o^2)} (C_M F_M + C_T F_T) \quad (3-36)$$

where F_M and F_T are given by

$$F_M = \frac{1 + \mu_o^2 \alpha_o^2 + \mu_o^4 (\alpha_o^2 - 1)}{[\alpha_o^2 + \mu_o^2 (\alpha_o^2 - 1)]^{3/2}}, \quad F_T = \frac{3(1 + \mu_o^2)(1 + \alpha_o^2)}{2\alpha_o^3}$$

With

$$\alpha_o^2 = 1 + \left(\frac{U_c}{b\omega\delta} \right)^2, \quad \mu_o = \frac{h_o u_\tau}{U_c}$$

Figure 3-36 (a)-(d) shows the non-dimensionalized wavenumber spectrum at $U_\infty = 30$ m/s calculated using the above mentioned models for four frequencies of 500 Hz and 5,000 Hz respectively. The wavenumber spectra are shown as a function of streamwise wavenumber normalized on frequency (in rad/s) and convection velocity U_c which is assumed to be 60% of the free stream velocity i.e. 18 m/s. The values of undisturbed boundary layer parameters δ and δ^* used in the computation are obtained from the measurements of Awasthi *et al.* (2014) and are 97 mm and 10.8 mm respectively. The friction velocity u_τ is calculated as

$$u_\tau = \sqrt{\frac{C_f}{2}} \quad (3-37)$$

where the skin friction coefficient's value is $C_f = 0.0025$ which is taken from Awasthi (2012) who obtained it by applying a law of the wall fit to measured boundary layer velocity profiles at $U_\infty = 30$ m/s. For each frequency the peak of all four models occurs at a streamwise wavenumber given by ω/U_c . This peak is also known as the convective ride of the wavenumber-frequency spectrum and at a given frequency is responsible for the most intense pressure fluctuations. The region of the spectra below the convective wavenumber which slopes down is known as the sub-convective region and the region at extremely low wavenumbers is known as the acoustic region. Both the sub-convective and the acoustic region have far less energy than the convective region and are usually difficult to measure due to the acoustic contamination of the measured spectra by facility noise.

Now consider the differences between the wavenumber-frequency spectra calculated using different models as shown in Figure 3-36. Looking at the sub-convective wavenumber range first we see that the Corcos (1964) model over-predicts the levels in this range compared to the rest of the models. The Ffowcs Williams (1982) and Chase (1987) models show similar levels at low wavenumbers but different slopes with the former model showing almost a linear behavior. The Smol'yakov & Tkachenko (1991) model shows the lowest levels in the sub-convective range and produces almost a wavenumber white shape which has been observed in experiments (Graham (1997)). In the convective region, each model displays similar peak levels with Chase (1987) model showing slightly higher levels than the rest. The Chase (1987) model also shows the broadest convective region while the Smol'yakov & Tkachenko (1991) models shows the narrowest region. The Ffowcs Williams (1982) model shows similar behavior as the Chase (1987) model when approaching from the low wavenumber side. However, on the higher wavenumber side the Ffowcs Williams (1982) model diverges and displays an almost wavenumber white shape at higher frequencies. The reason for this high wavenumber divergence is that the Ffowcs Williams (1982) model does not satisfy the criteria in equation (3-28) and therefore its validity is limited to lower wavenumbers. The Corcos (1964) model shows a slower decay at higher wavenumbers while the Chase (1987) and Smol'yakov and Tkachenko (1991) models have similar decay rates and lower levels than the Corcos (1964) model.

We will now consider the performance of each of these models by passing them through the wavenumber response functions for the two microphones used in the linear array to obtain a frequency spectrum of pressure fluctuations according to the integral shown in equation (3-15). This analysis was carried out for the undisturbed boundary layer flow at $U_\infty = 30$ m/s. The wavenumber-frequency models were un-normalized using the single point frequency spectrum model given by Chase (1987) and shown in equation (3-36). The integration in equation (3-15) was performed numerically and numerical convergence was ensured by changing the wavenumber spacing ($\Delta\kappa_x$ and $\Delta\kappa_z$) and the upper limit of the wavenumber range. The wavenumber range chosen was $0 \leq \kappa_x \leq 4000\pi$ rad/m and $-2000\pi \leq \kappa_z \leq 2000\pi$ rad/m and the wavenumber resolution in each case was $\Delta\kappa_x = \Delta\kappa_z = 2\pi$ rad/m.

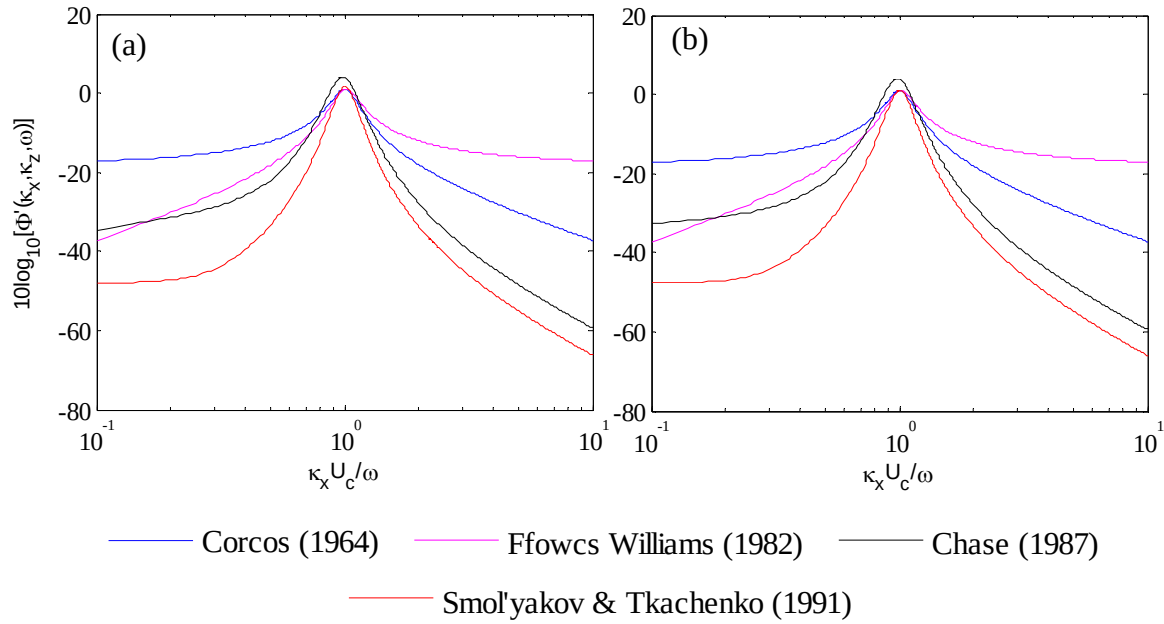


Figure 3-36 Wavenumber-frequency spectra using four different models as a function of non-dimensional streamwise wavenumber at $U_\infty = 30$ m/s and $\kappa_z = 0$ rad/m. (a) $f = 500$ Hz and (b) $f = 5,000$ Hz.

Figure 3-37 (a) shows the frequency spectrum of surface pressure calculated by passing each wavenumber-frequency model through the wavenumber filter function of B&K 4190 microphone (11.1 mm diameter). Also shown in the figure is the spectrum measured by the most upstream B&K 4190 microphone in the linear surface pressure array when the array was in streamwise orientation. It can be seen that each model except for Ffowcs and Williams (1982) predicts the measured spectra well (within 3 dB) in the frequency range between about 100 Hz to 2,000 Hz. At frequencies below 100 Hz the Chase (1987) model provides the best agreement with measured data while the rest of the models over predict the levels and do not show a flat region as in the measurements. The Smol'yakov and Tkachenko (1991) and Corcos (1964) model show similar behavior at low frequencies. The Ffowcs Williams (1982) model diverges and is orders of magnitude higher than the measurements because of its divergent behavior at high wavenumbers. At frequencies higher than 2,000 Hz the Corcos model has an excellent agreement with the measurements while the rest of the models under predict the levels. Consider now the Figure 3-37 (b) which shows the predicted and measured spectra for the smaller B&K 4138 microphone in the array. Here we see a similar trend as for the larger microphone with the Chase (1987) providing the best agreement at lower frequencies and Ffowcs Williams (1982) model again diverging away. The predictions provide a good estimate of the shape of the measured spectra at mid-frequencies but slightly under predict the levels compared to the larger microphone. At higher frequencies the Corcos (1964) model predicts the shape of the measured spectra up to frequencies greater than the rest of the models. However, it under-predicts the measured levels.

Based on the comparison of the models with measurements it seems that the Chase (1987) and the Corcos (1964) models are the two most suitable models to perform comparison with present measurements. Therefore, in further analysis only these two models will be considered.

The two plots in Figure 3-37 can be combined together to yield an estimate of the capability of different models to provide an assessment of the spatial attenuation due to finite size microphone. To do so we consider the ratio of the spectral densities measured by the two microphones used in present measurements as

$$R_\phi(f) = \frac{\phi_{B\&K4190}(f)}{\phi_{B\&K4138}(f)} \quad (3-38)$$

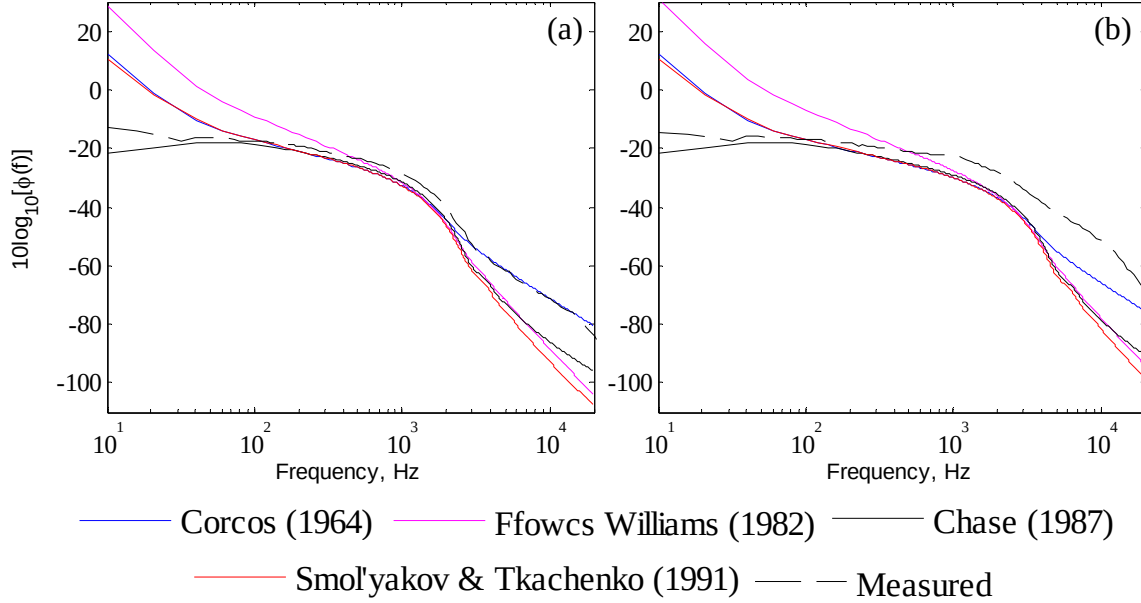


Figure 3-37 Single point frequency spectrum of surface pressure for undisturbed boundary layer flow at $U_\infty = 30$ m/s computed by passing different wavenumber-frequency pressure spectrum models through the wavenumber filter function for (a) B&K 4190 microphone and (b) B&K 4138 microphone. The frequency spectra measured by one of the B&K 4190 microphones in the array at $U_\infty = 30$ m/s is also shown.

The above ratio can provide an estimate of the cutoff frequency above which the spatial averaging effects become significant for the larger diameter microphone. This ratio can be calculated using both the measured data and the wave-number frequency models to compare which model is most effective in estimating the spatial attenuation as seen in the measurements. Figure 3-38 (a) and (b) show the comparison between the measured and computed $R_\phi(f)$ when the array was in spanwise and streamwise orientation respectively. Looking at Figure 3-38 (a) we see that the measured ratio is flat between 100 Hz to 1,000 Hz which signifies that the larger microphone does not spatially average the pressure fluctuations in this frequency range. We would expect the ratio to be also flat below 100 Hz because the pressure fluctuations at such low frequencies are dominated by small wavenumbers which the microphone does not filter. However, in the measured spectra of Figure 3-38 (a) this is not the case. The reason for this discrepancy is that the undisturbed boundary layer measurements with the spanwise array are dominated by the background acoustics at low frequencies. This contamination is believed to originate from the acoustic reflections from the aluminum panels which form the floor and ceiling adjacent to the false wall. This acoustic contamination of low frequency spectra in undisturbed boundary layer measurements is further discussed in §3.2.3. At higher frequencies the ratio begins to fall due to the spatial filtering by the larger microphone in the array. The frequency at which the spatial attenuation exceeds 3 dB is about 750 Hz. The spatial attenuation continues to fall linearly up to about 3,000 Hz after which the curve flattens. The reason for this flattening at high frequencies is that at these frequencies the response from both the microphones is severely attenuated. If we now compare the models with the measurement we see that both the models predict the attenuation curve quite well and provide a good estimate of frequency at which the spatial attenuation from larger microphone becomes significant. Note again that the discrepancy at lowest frequencies is due to background contamination of the measured spectra. At higher frequencies, the Corcos

(1964) model shows much better quantitative agreement with the measurements than the Chase (1987) model.

Consider now Figure 3-38 (b) which shows the comparison between models and measurements made with the array in streamwise orientation. First we note that in this case the measured ratio is flat and is approximately equal to the ideal 0 dB limit to much lower frequencies than in Figure 3-38 (a). This shows that, at least for the undisturbed boundary layer flow, the measurements made with the array in streamwise orientation are less contaminated than those made with the array in spanwise orientation. The models again predict the measured attenuation well with Corcos (1964) model providing good quantitative and qualitative agreement to higher frequencies than the Chase (1967) model.

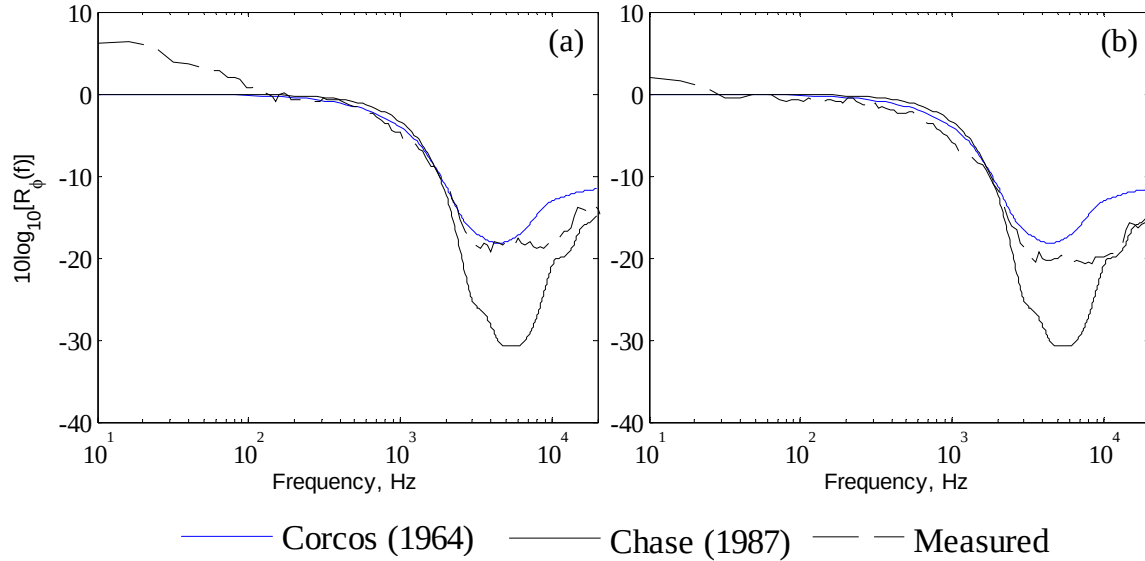


Figure 3-38 Ratio of response of B&K 4190 microphone and B&K 4138 microphone to undisturbed boundary layer flow at $U_{\infty} = 30$ m/s. (a) Measured ratio with array in spanwise orientation and (b) Measured ratio with array in streamwise orientation.

The results shown in Figure 3-38 suggest that when comparing the low frequency (frequency below which spatial attenuation is insignificant) response of two microphones exposed to the same flow field the choice of the wavenumber-frequency spectrum model is more or less irrelevant. Both the Chase (1987) and Corcos (1964) models provide good assessment of the frequency range up to which the larger sensor does not spatially filter the pressure fluctuations over its sensitive area. In terms of predictions of the measured spectra themselves the Chase (1987) model provides a good quantitative and qualitative estimate at low and mid frequencies while the Corcos (1964) model works best for mid to high frequencies. We will perform an analysis similar to the one presented here on the entire microphone array in §3.2.4 in order to estimate the limitations of the microphone array when it is used as an unsteady force sensor. The analysis presented here helps to narrow the choices of the available wavenumber-frequency spectrum models for further analysis. Based on the results of this section, only the Corcos (1964) models will be considered in the array response analysis.

3.1.6 Far Field Sound Measurement using a 117 Microphone Phased Array

The phased array system (hardware and software) used to measure the far field sound is provided by AVEC Inc. This array consists of 117 microphones arranged in a spiral fashion with 9 arms consisting of 13 microphones. The phased array is shown in Figure 3-39 (a). The array is composed of Panasonic WM-64PNT electret microphones which have a flat frequency response between 20 Hz and 16 kHz and these microphones are mounted flush with the surface on a circular carbon fiber disk. The nominal sensitivity of

these microphones is -44 ± 3 dB Re 1V/Pa at 1 kHz. The distance between the microphones at the end of the opposite arms is approximately 1.1 m. The coordinates of each of these microphones in meters are shown in Figure 3-39 (b). The distance of each microphone is defined from the center of the array where a positioning laser beam is present (shown in Figure 3-39 (a)) which helps to position the array with respect to the sound source of interest. The microphones were calibrated and were selected to be within $\pm 5^\circ$ phase and ± 0.4 dB amplitude between 500 Hz and 16 kHz. The signal from the microphones is acquired simultaneously using two 64 channel PCI based data acquisition cards. The signal from the array microphones is conditioned using conditioner designed by AVEC Inc. which applies an anti-aliasing filter at 20 kHz to the signals and also powers the microphones. The signal from each microphone is acquired with a sampling rate of 51200 Hz and Fourier transformed to yield the auto and cross spectral densities. The record length used for ensemble averaging is 8192, a Hanning window is applied to each record and a 50% overlap between the records is used in averaging.

To measure far field sound from forward steps the phased array was positioned outside the flow region in the port side anechoic chamber with its face parallel to the false wall. The phased array installed in the anechoic chamber has been shown on the left in Figure 3-40. The anechoic chamber is acoustically treated with foam wedges along its walls to minimize the acoustic reflections from the test section. The phased array is positioned such that its center is 805 mm away from the Kevlar wall. The height of the center of the array above the test section floor is 873 mm which is also shown in the figure. The foam wedges lining the anechoic chamber and the support structure of the array built from 80/20 aluminum beams with slotted cross section profile have also been shown in the figure. After the array was installed the floor of the anechoic chamber was also covered with acoustic grade foam wedges (not shown in the figure). The sound measurements from the array are all referenced to the center of the array making it the observer location in present measurements. The location of the array center with respect to the forward step geometry is shown on the top right of the figure. The source to observer radial distance and the directivity of the observer with respect to the step are shown at the bottom right of the figure.

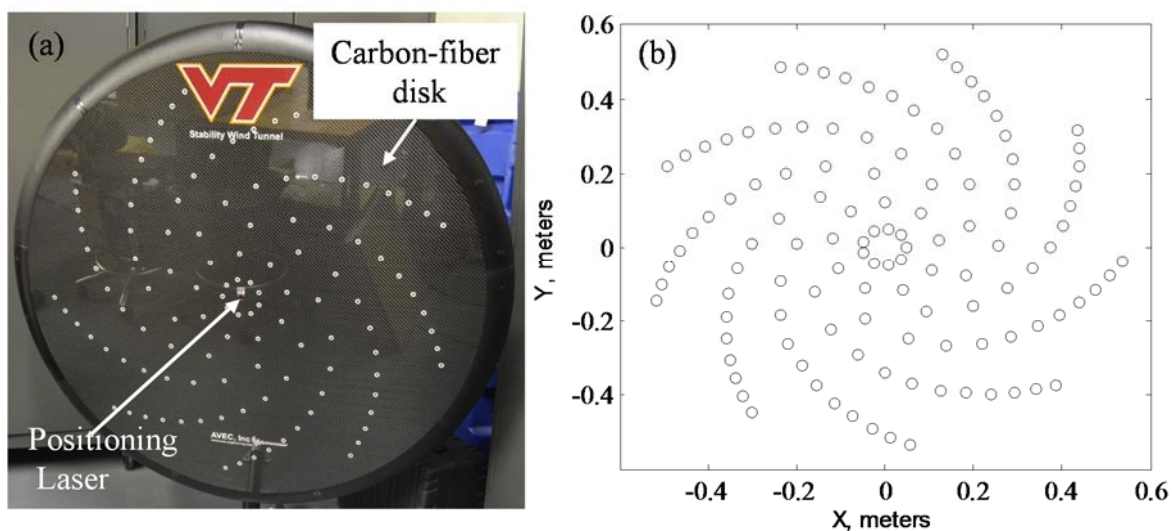


Figure 3-39 117 microphone phased array. (a) Panasonic WM-64PNT microphones installed flush with the surface of a carbon fiber disk. (b) Coordinates of each microphone.

3.1.6.1 Beamforming Procedure and Array Performance

The output of the phased array is beamformed using software provided by AVEC Inc. The beamforming procedure followed by this software is the conventional sum and delay beamforming procedure as described by Mueller (2002). This procedure assumes that the acoustic waves at the array

arrive as spherical wave and knowing the distance between each microphone the array can be steered in a manner such that it experiences constructive interference in a particular direction. In the frequency domain the mean square value of pressure due to a N microphone array with the source at distance r_s it is given by

$$P_A = \frac{1}{N^2} \vec{R}_S^+ [C] \vec{R}_S \quad (3-39)$$

In the equation above, $[C]$ is the cross spectral matrix (or CSM) of the array output, $\vec{R}_S$ is what is known as the steering vector or weight and $\vec{R}_S^+$ represents its conjugate transpose. The steering vector is given by

$$\vec{R}_S = \frac{1}{r_{s,0}} [r_{s,1} e^{ikr_{s,1}} \dots r_{s,M} e^{ikr_{s,M}}]^T \quad (3-40)$$

where $r_{s,0}$ is the distance from the source location to the center of the array, $r_{s,m}$ is the distance from the source to the m^{th} microphone in the array, superscript T refers to the transpose of a matrix and k is the acoustic wavenumber at a particular frequency. Equations (3-39) and (3-40) can be used to obtain the acoustic power being radiated from sources at different points by changing the distance $r_{s,m}$. For the present work the entire false wall was discretized into a scanning grid with a resolution of 20 mm in both streamwise and spanwise direction and the beamformed output at the center of the array was then calculated using the equations above. Note that the diagonal of the CSM is removed before carrying out beamforming to eliminate incoherent or uncorrelated noise in the output.

At a particular frequency the beamforming output from a N sensor discrete array suffers from spatial aliasing which is similar to the aliasing experienced in digitization of continuous time signals. The beamformed output of the array at a particular frequency consists of a main lobe (maximum power) due to constructive interference and side lobes due to spatial aliasing. This side lobe behavior which is a function of frequency can be estimated by considering the response of the array to a monopole point source located at some known distance. This theoretical estimate also known as Point Spread Function (PSF) of the array provides a theoretical estimate of the spatial bandwidth and SNR at a given frequency. The PSF of the array used in present work at four different frequencies has been shown in Figure 3-41 (a)-(d). The PSF is shown for a monopole source response of the array with observer located at the center of the array. The scale of the PSF maps is shown in dB with the maximum level of 0 dB indicating the main lobe power and falling levels indicating the side lobe levels relative to those for the main lobe. In practice the spot size of the array is defined as the region where the SPL is down 3 dB from the maximum. Therefore, the contour levels shown in the figure are restricted between 0 and -3 dB. Looking at the figure it can be seen that the spatial resolution of the array improves with increasing frequency. At the lowest frequency of 1 kHz shown in Figure 3-41 (a) the spot size is close to 1m but is reduced to approximately 0.5m, 0.2m and 0.09 m at frequencies of 5 kHz, 8 kHz and 10 kHz respectively. This means that the spatial resolution i.e. the ability of array to distinguish between sound sources which are distributed over an area significantly improves as the frequency rises. The spot size of the array was calculated at several other frequencies as well and has been shown in Figure 3-42 (a) as a function of frequency. As shown in the figure the spot size of the array reduces drastically as the frequency increases and for frequencies above 5 kHz it is reduced to 0.2 m or less. Another quantity of interest in assessment of array performance which is related to the PSF is the theoretical array SNR. This is defined as the ratio between the main lobe power and the side lobe power. The array SNR is useful in determining the sound sources present in a beamformed sound map which might be due to the side lobes as opposed to the presence of actual noise source. This theoretical array SNR as a function of frequency for the present array is shown in Figure 3-42 (b). As seen in the figure the SNR is also a function of frequency and decreases with increasing frequency. The lowest values of SNR occur above frequency of about 12 kHz but these are still more than 12 dB. This indicates that any sound levels depicted

in the beamformed map within approximately 12 dB down from the maximum in that map is not due to the side lobes but is an actual noise source.

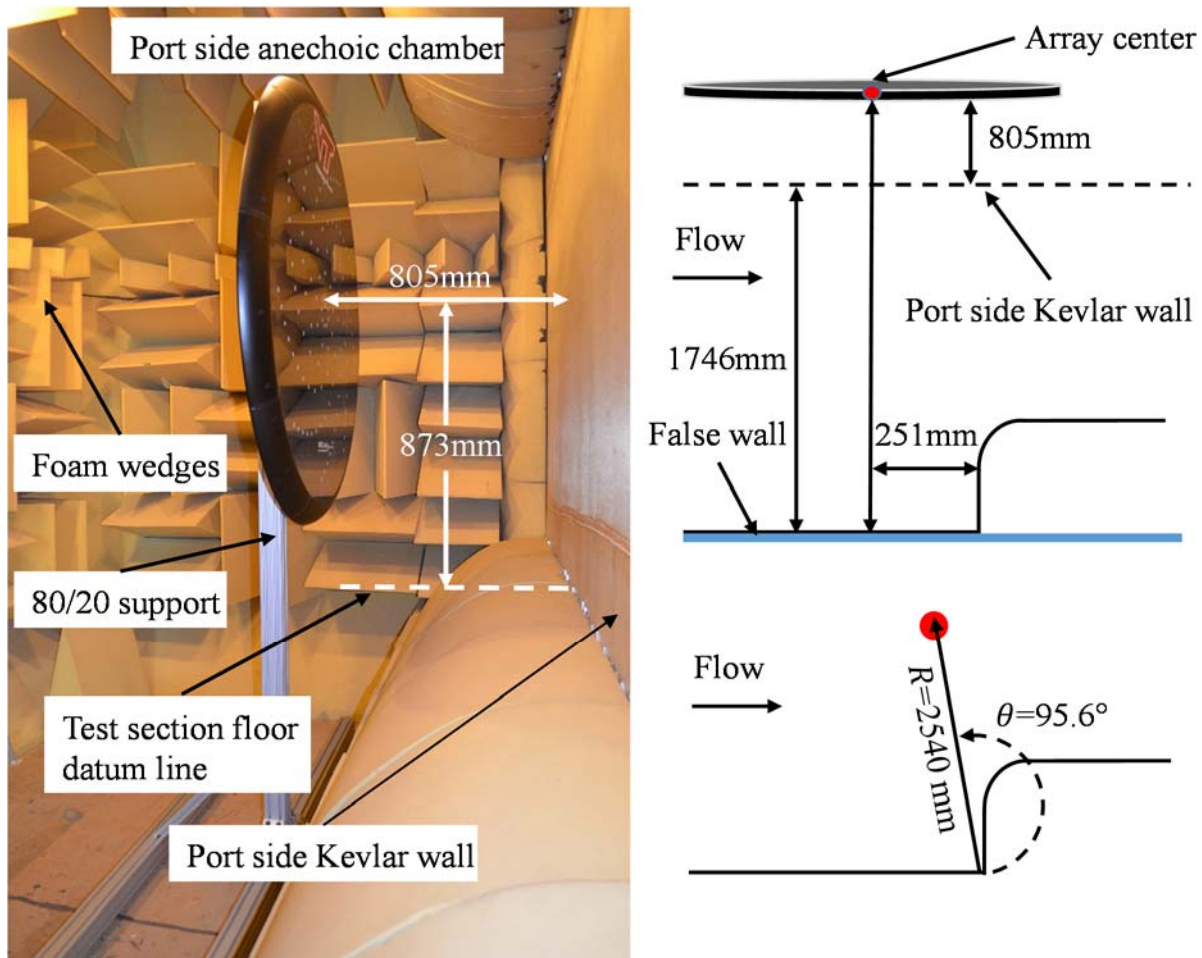


Figure 3-40 Phased array position installed in the port side anechoic chamber with its face parallel to the false wall (left). Location of the phased array center with respect to the inside corner of the forward step (top right). Directivity of the phased array measurement (bottom right).

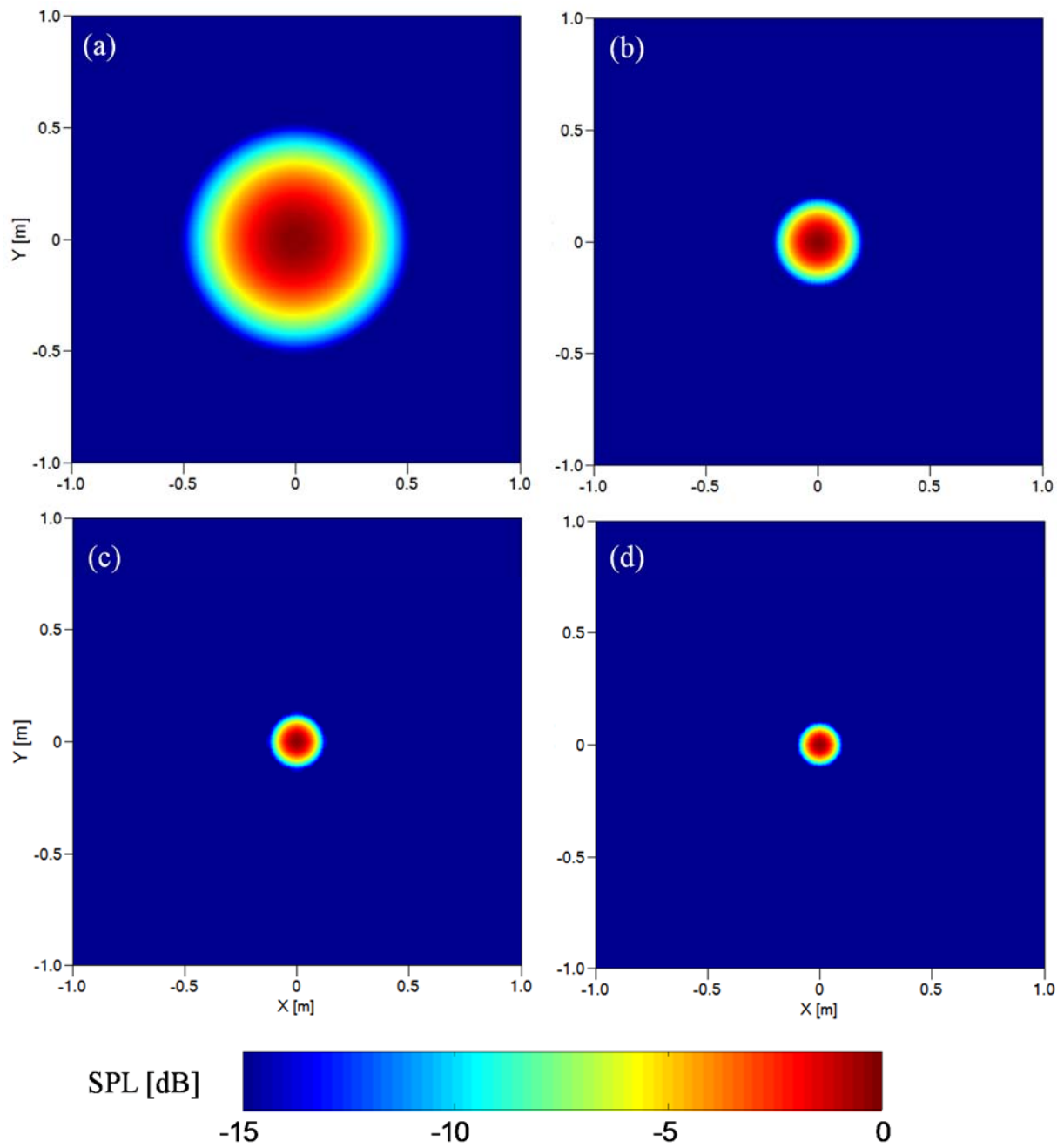


Figure 3-41 Point spread function of phased array at four different frequencies. (a) $f = 1$ kHz, (b) $f = 2$ kHz, (c) $f = 5$ kHz and (d) $f = 10$ kHz.

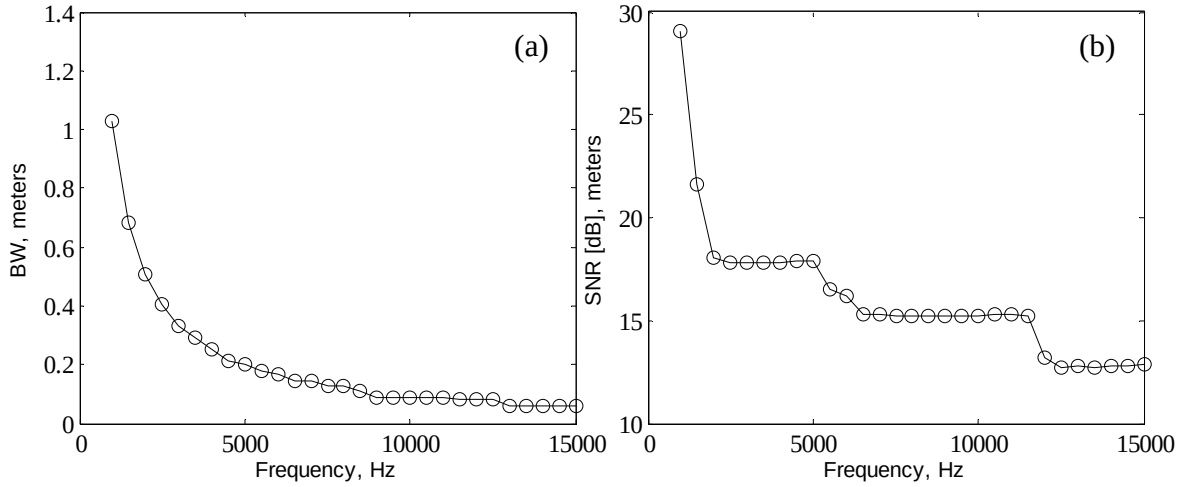


Figure 3-42 Phased array spot size and SNR as a function of frequency. (a) Bandwidth, (b) SNR.

3.1.6.2 Flow/Kevlar Refraction and Hard Surface Correction

The apparent sound levels received by the phased array suffers from losses due to the refraction from the boundary layer on the Kevlar wall and the porous surface of the Kevlar itself. These effects must be accounted for in beamforming results in order to reveal the true boundary layer noise coming from the surface discontinuities mounted on the false wall. These losses have previously been modeled through a series of experiments on roughness noise by Ravetta *et al.* (2012). This work was performed in the same facility as the present study and therefore their results are assumed to apply to present work as well. To assess the losses through the Kevlar they performed a test where the array response to a white noise speaker was measured with and without the Kevlar window present. The measured attenuation was curve fitted and a quadratic expression for attenuation (in dB) was found which is given by Ravetta *et al.* (2012) as

$$\Delta_{Kevlar} = 5.9(10^{-3}) \left(\frac{f}{1000} \right)^2 + 1.45(10^{-2}) \left(\frac{f}{1000} \right) \quad (3-41)$$

For the losses due to refraction from the boundary layer on the port side Kevlar wall Ravetta *et al.* (2012) measured the array response to a white noise speaker which was placed on the starboard side anechoic chamber. They then measured the array response to this white noise speaker with and without the flow on. Since in this case there are two boundary layers (one on each Kevlar wall) as opposed to the present case where there is only one the attenuation is simply one half of what was measured by Ravetta *et al.* (2012). The curve fit for the boundary layer losses (in dB) as a function of frequency and Mach number (M) given by Ravetta *et al.* (2012) is

$$\Delta_{BL} = (1 - e^{-\beta f})(\alpha_1 M + \alpha_2 M^2) \quad (3-42)$$

where $\alpha_1 = 5.4316$, $\alpha_2 = 88.95$ and $\beta = 0.001057$. The total attenuation due to Kevlar and boundary layer refraction using equations (3-40) and (3-41) is plotted in Figure 3-43 for five different free stream velocities. The velocity range for which these attenuation have been shown corresponds to the range for which measurements were made in the present work. It can be seen that the losses increase with frequency and flow velocity and can be 5-6 dB for highest velocity and frequency of interest.

Since the microphones are mounted on a hard surface a doubling of pressure sensed by microphones occurs due to the reflection of sound waves. This doubling of pressure corresponds to a 6 dB increase and was also accounted for in the beamforming results.

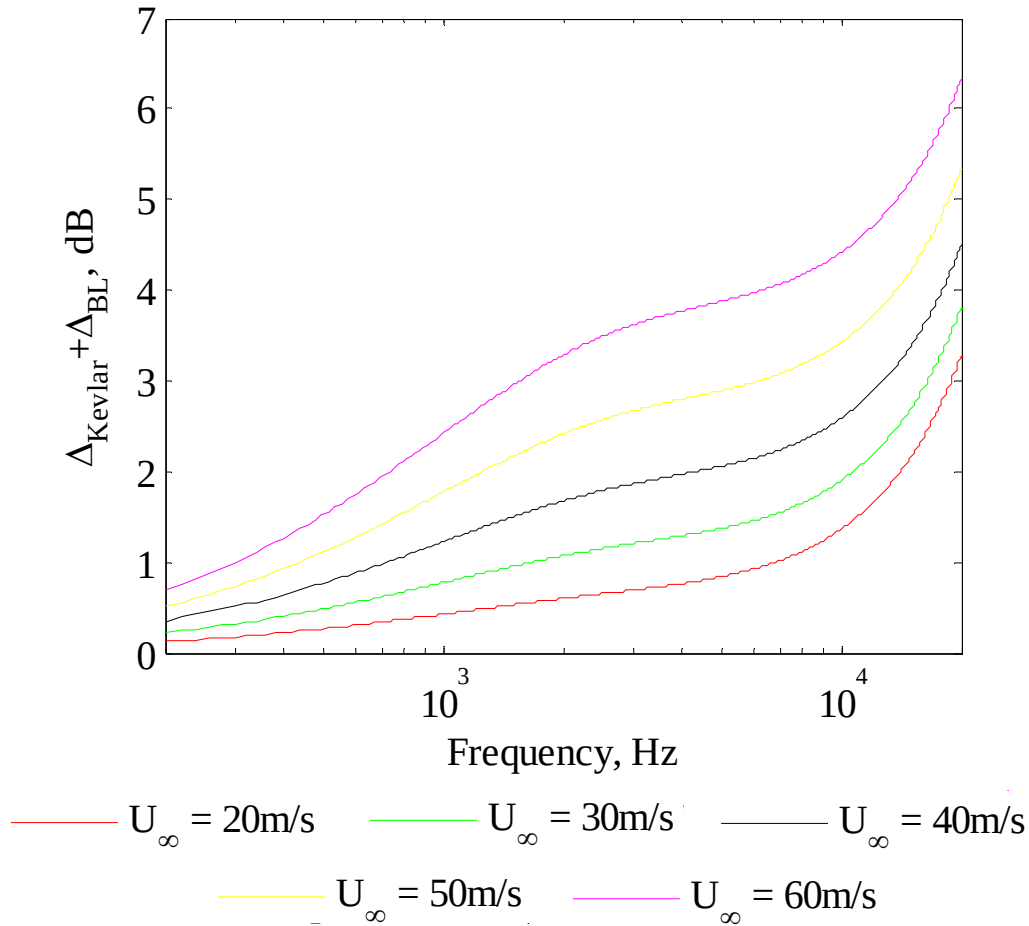


Figure 3-43 Sound attenuation due to losses from boundary layer and Kevlar wall for different free stream velocities.

Figure 3-44 shows a typical sound source map obtained using the beamforming procedure outlined above for $r/h = 0\%$ forward step at a frequency of 2,000 Hz. Also shown in the figure is dashed line which corresponds to the streamwise location of the vertical face of the step. The streamwise and spanwise coordinates which will be used to present the beamformed source maps are X and Y respectively as shown in the figure. Note that this figure shows the sound emanating from the entire false wall region ($-3 \text{ m} \leq X \leq 3 \text{ m}$). It can be seen from this figure that the beamforming procedure is clearly able to isolate the step noise from the acoustic reflections of facility noise which are seen upstream and downstream of the step. The source maps for all discontinuities in present work (rounded steps and circular embossment) at other frequencies are further presented and discussed in detail in §3.2.6.

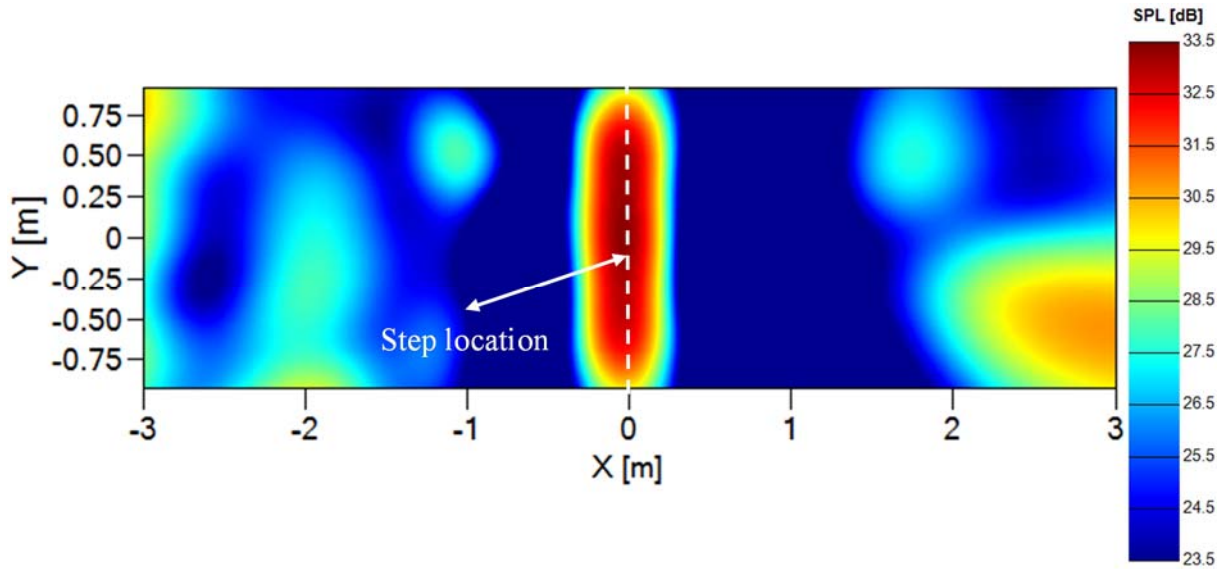


Figure 3-44 Typical beamformed sound source map for an unrounded forward facing step at $f=2,000$ Hz and $U_{\infty} = 30$ m/s.

3.1.6.3 Background Noise Removal from Beamformed Sound Source Maps

The approach used to remove undesired noise sources from the acoustic maps is part of unpublished work by Dr. Ravetta of AVEC Inc. This approach, currently referred to as CLEAN-er, is based on the concept of CLEAN-SC and other potential applications mentioned in the paper by Sijsma (2007). The application of this procedure to remove noise from the beamformed source maps is discussed in detail in §3.2.6.2.

3.1.7 Far Field Sound Measurement using In-Flow Microphones

Four B&K 4190 microphones mounted on aerodynamic stands inside the test section were also used to measure far field sound from rounded forward steps. The purpose of using these microphones in addition to the phased array was to attempt to capture directivity of forward step noise. The in-flow microphone setup has been shown in Figure 3-45. The in-flow microphone stands used to mount the microphones have a NACA 0012 profile to reduce self-noise from the stands. These stands are mounted on panels which are of the same dimension as the Kevlar panels that form the floor and ceiling of the test section. Thus to install the microphone stands selected floor/ceiling panels were replaced by these microphone stand panels. The microphone stands are installed either by replacing a floor or a ceiling Kevlar panel. Two of these microphone stands are installed towards the upstream end of the test section while two are installed towards the downstream end. A close up view of one of the in-flow microphone stands with the B&K 4190 microphone installed on it has been shown in Figure 3-45 on the right. The microphone consists of a UA 0052 bullet nose wind screen in front of the diaphragm (inset) to minimize the self-noise of the microphone. The connection wires from the microphone to the DAQ are run through a slot that exists on the microphone stands and are then routed from underneath (or overhead) the floor/ceiling panels to the starboard side anechoic chamber where the DAQ are placed. This slot on the microphone stand is covered using a foil tape as shown on the right in Figure 3-45. The junction between the microphone and the microphone stand is faired smoothly using foil tape.

The observer locations of in-flow microphone measurements with respect to the forward step geometry have been shown in Figure 3-46. It must be noted that the in-flow microphones are not all in the same spanwise plane. This is because two of the microphones ($\theta = 155.1^\circ$ and $\theta = 15.2^\circ$) are installed on the microphones stands which are mounted on the floor of the test section while the other two ($\theta = 141.2^\circ$ and $\theta = 22^\circ$) are mounted on stand that are attached to the ceiling of the test section. Further note that the

radial distance of each of the microphone from the inside corner of the forward steps is approximately $R = 1.9 \pm 0.01$ m

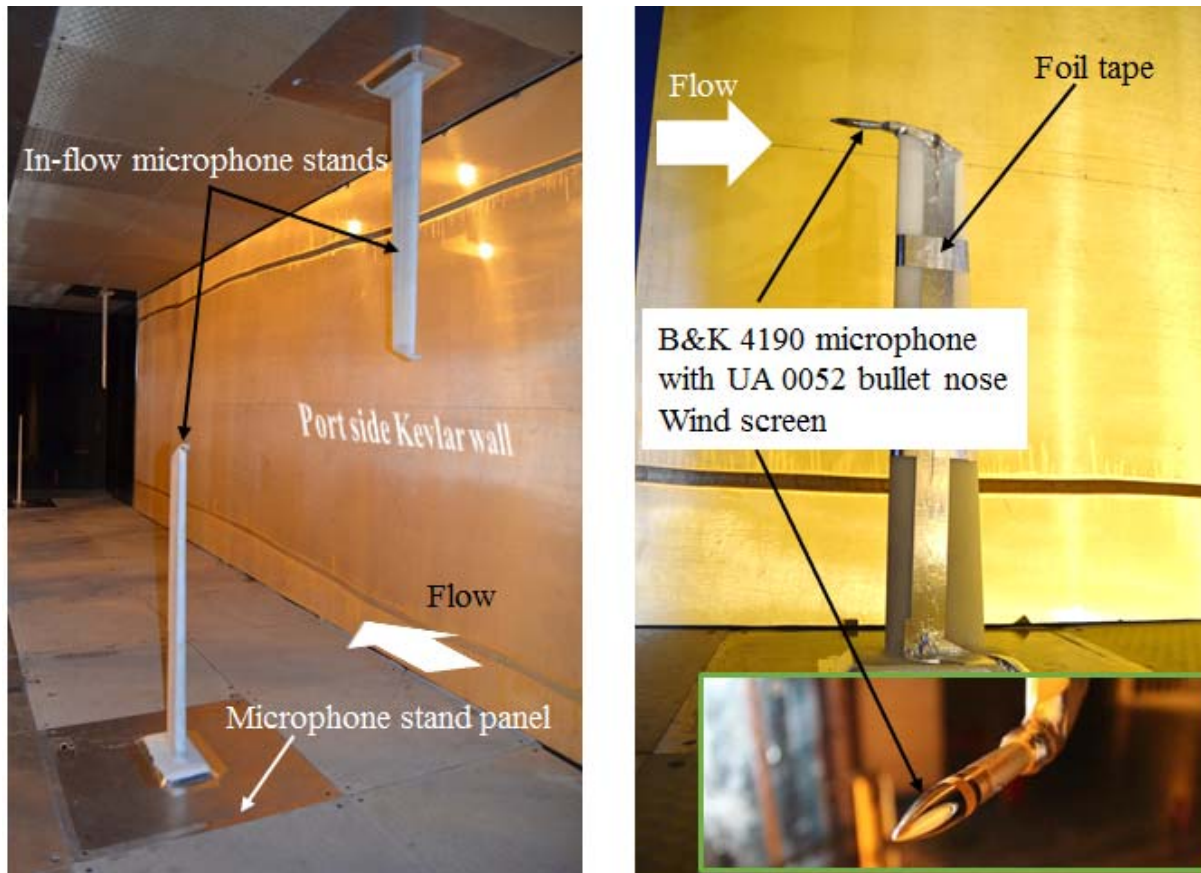


Figure 3-45 In-flow microphone setup to measure far field sound.

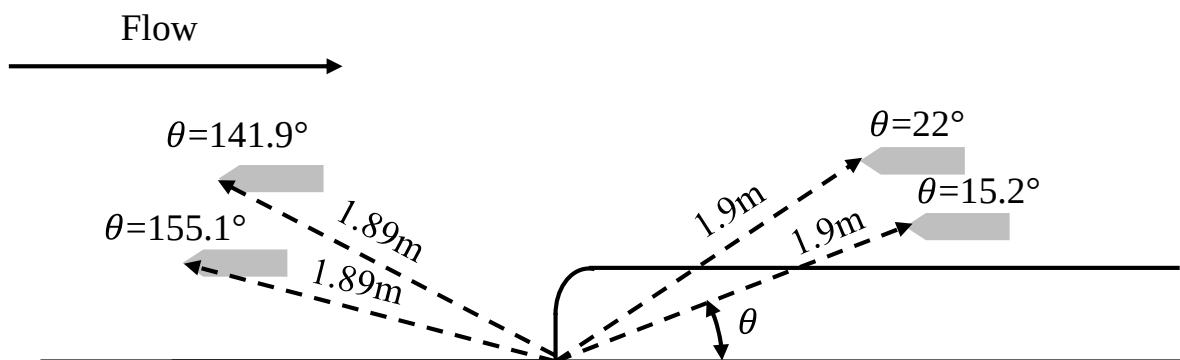


Figure 3-46 Directivity of in-flow microphones.

The pressure signal from the in-flow microphone is acquired using B&K LAN-XI DAQ which is the same hardware used for acquisition of the signal from 24 microphone unsteady force array (see §3.1.5). This enabled simultaneous acquisition of the unsteady force and pressure fluctuation signal with the far field sound signal from in-flow microphones. The far field sound pressure was sampled at 65536 Hz for 32 seconds and the auto and cross spectral densities (between all in-flow microphones as well as between in-

flow and 24 microphone linear array/probe microphone) were obtained using Fourier transform of the time series signal.

3.1.8 Wall Mounted Circular Embossment Configuration

The wall mounted embossment used in the present work to explore the characteristics of sound from three-dimensional discontinuities had a height of $h_D = 25.4$ mm and a radius of $r_D = 457.2$ mm. To fabricate this embossment the circular geometry was split into four side arcs and a rectangle as shown in Figure 3-47 which shows the flow side view of the embossment. The side arcs were machined precisely out of aluminum and had a height equal to the embossment height, $h_D = 25.4$ mm while the central rectangular shape was machined out of an aluminum plate that had a height of 6.4 mm. Note that in this assembly Arc 1 is the same dimension as Arc 3 and Arc 2 is the same dimension as Arc 4.

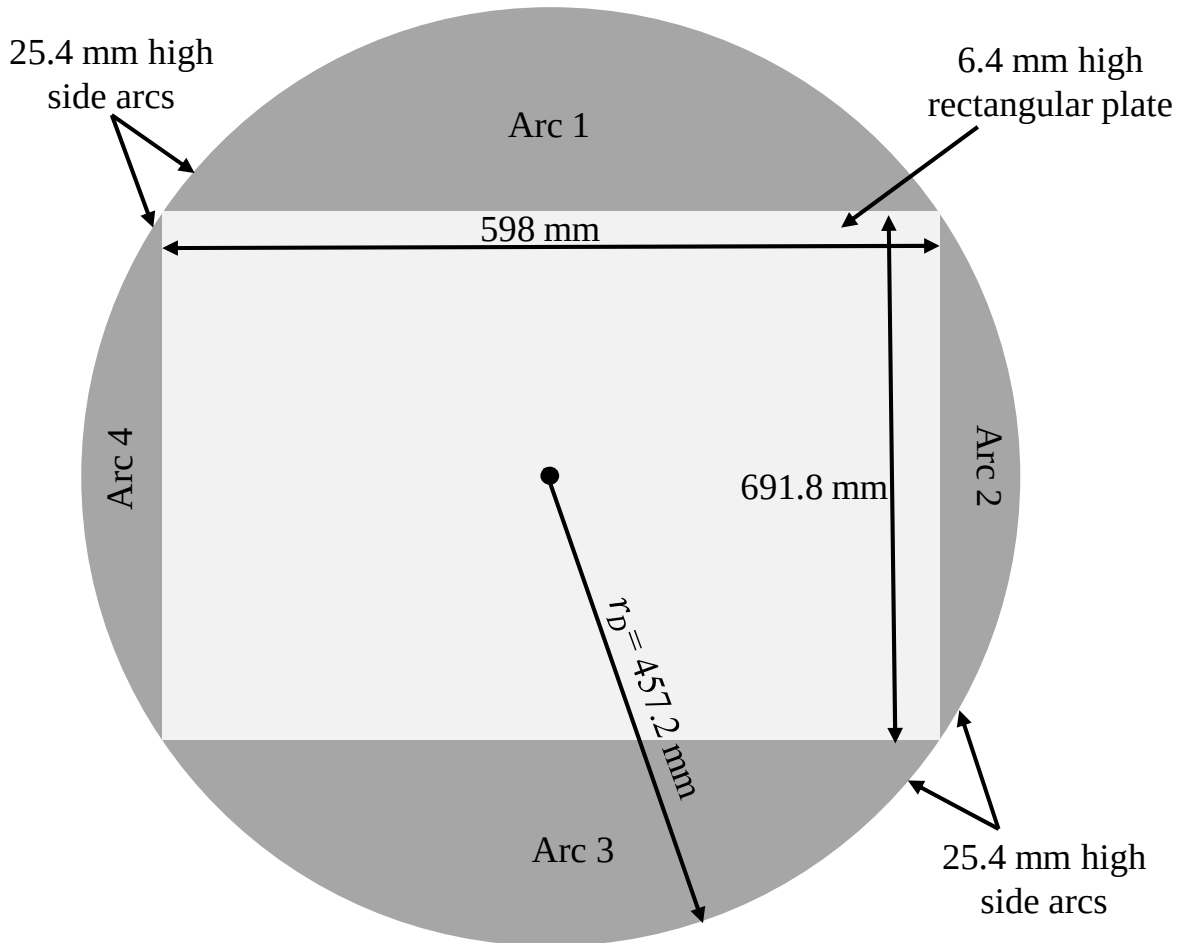


Figure 3-47 Circular embossment assembly (flow side).

The underside of the circular embossment assembly is shown in Figure 3-48. This view shows five 19 mm high support blocks which elevate the rectangular aluminum plate at the center of the embossment in Figure 3-47 to the height of the side arc. Each of these blocks are machined out of aluminum and have a square cross section of 50.8 mm. The rectangular plate is attached to these support blocks via 12.7 mm diameter through bolts that utilize countersunk holes on the flow side. The figure also shows four aluminum bars, each with a thickness and a height of 19 mm which support the side edges of the rectangular aluminum plate. Two of these bars (shown in blue towards the top and bottom of the rectangular plate) have the same width as Arc 1 and Arc 3 while the other two towards the sides of the rectangular plate (shown in green)

are slightly shorter than the width of Arc 2 and Arc 4. These aluminum bars are also used to connect the side arcs and the rectangular plate together to form the whole circular embossment as each component is bolted down to these bars. The circular arcs are bolted to these bars from the underside of the embossment while the rectangular plate is bolted from the flow side using 12.7 mm diameter bolts that utilize countersunk holes.

The seams between each of the circular arcs and between the arcs and the rectangular plate were sealed by applying automotive body filler and then sanding it down to form a smooth joint between adjacent features. The countersunk holes on the flow surface used to connect the rectangular plate to the support bars on the underside of the embossment and the countersunk holes to attach the rectangular plate to the square support blocks were all taped using a 60 micron thin clear tape.

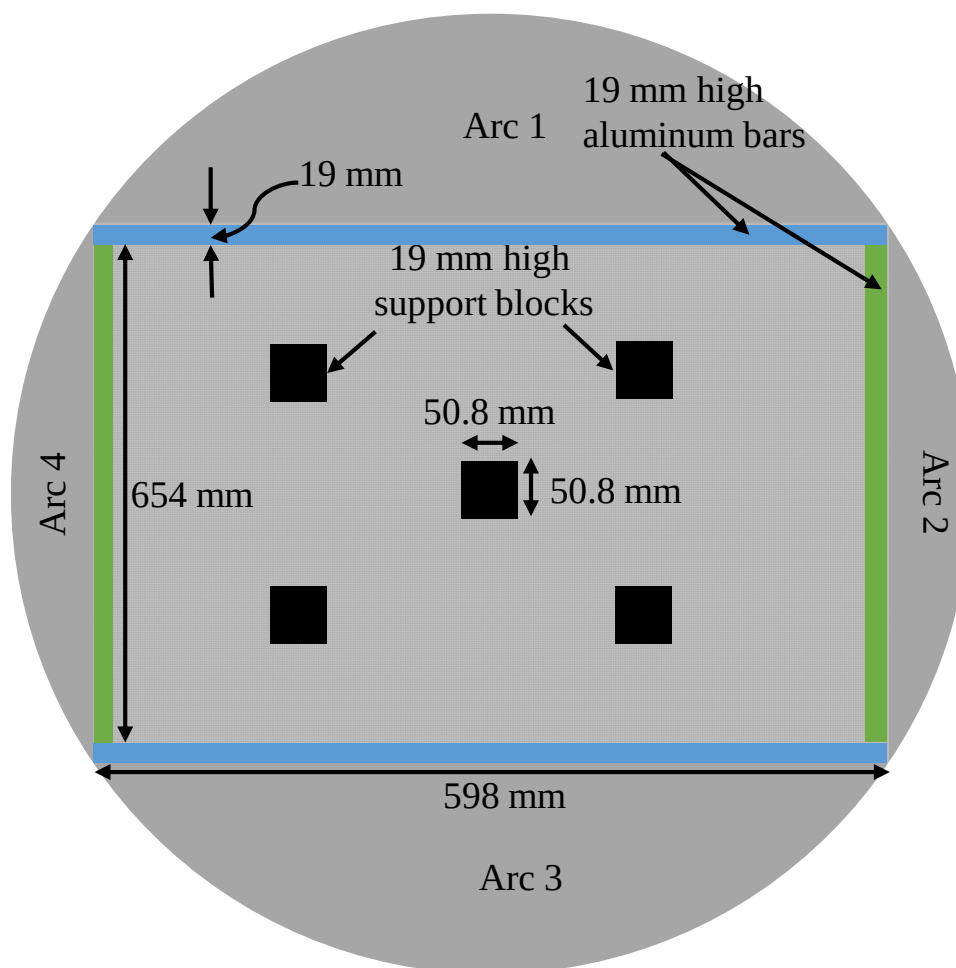


Figure 3-48 Underside of circular embossment assembly showing the five 19 mm high blocks that support the 6.4 mm high rectangular aluminum plate in the previous figure and the 19 mm high aluminum bars that connect the side arcs to the rectangular plate.

Once the entire circular embossment is assembled together it is bolted down to the false wall using 12.7 mm diameter bolts that use countersunk holes located on each of the side arcs as well on the rectangular plate. Note that the bolts used on the rectangular plate are through bolts that connect the plate, the support blocks and the false wall together. As was mentioned in §3.1.2, an access hatch was created in the false wall for instrumentation access from the starboard anechoic chamber (see Figure 3-5). While for forward facing steps this entire access hatch is covered by the step configuration (since it spans almost the entire height of the test section), for circular embossment this was not the case. Therefore, the polycarbonate

window created to seal this hatch during undisturbed boundary layer measurements was kept intact and the circular embossment was mounted on top of it. The inside corner of the circular embossment (embossment/false wall junction) was sealed by applying thick motor oil grease.

The circular embossment was mounted on the false wall such that its leading edge (at spanwise center of the embossment) was at the same location as the leading edge of the forward steps. Figure 3-49 shows the embossment mounted on the false wall. The location of the vertical face of the step is shown on the figure with a dashed line. Looking at the figure it can be seen that the embossment is mounted such that its spanwise center is about 933.5 mm from the floor of the test section which is about 18.5 mm above the spanwise center of the test section. The coordinate system which will be followed for the circular embossment is the same as that for the forward step in that its origin is located on the false wall at the leading edge of the embossment and at the spanwise center of the test section. The coordinate X represents the streamwise coordinate, Y is the wall-normal coordinate and Z is the spanwise coordinate which points towards the floor of the test section in accordance with the right hand rule. This coordinate system means that the spanwise center of the embossment is located approximately at $Z = -18.5$ mm.

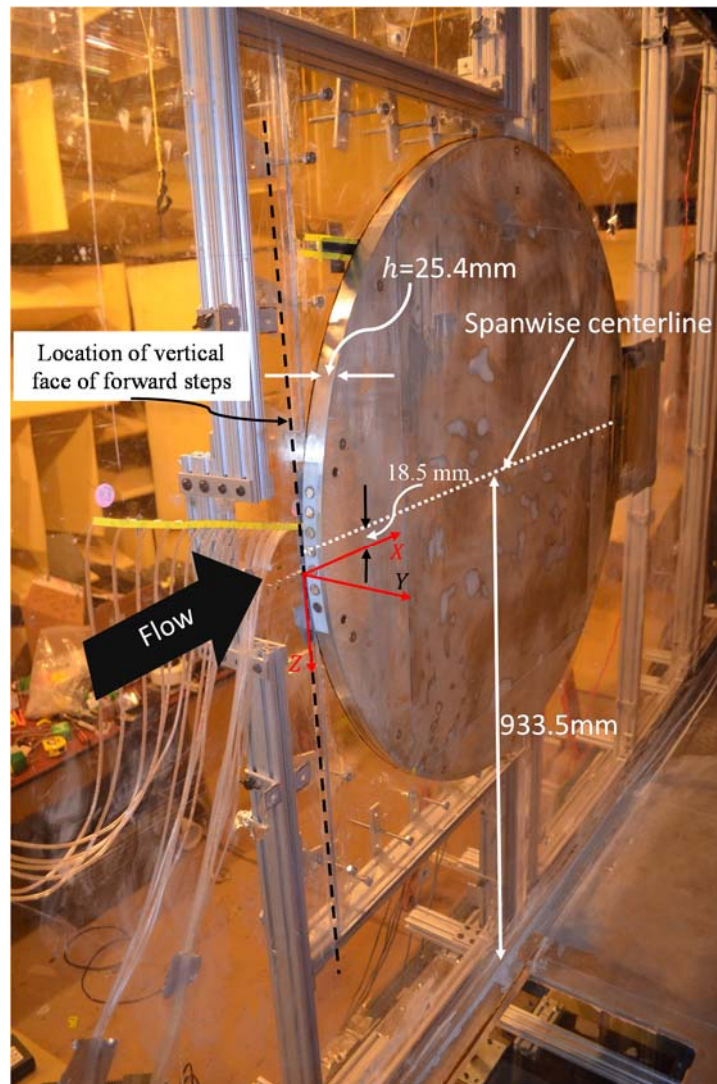


Figure 3-49 Circular embossment mounted on the false wall of Virginia Tech Stability Wind Tunnel. Also shown is the coordinate system which will be used for circular embossment.

3.1.9 Unsteady Surface Force Measurement on Wall Mounted Circular Embossment

Similar to the forward facing step measurement, a linear array of B&K 4190 microphones (11.1 mm diameter) was used to measure an area averaged unsteady surface pressure which is equivalent to unsteady surface force. However, in the case of circular embossment 7 microphones (instead of 24 for forward steps) were used to make this measurement (also note that as opposed to forward step microphone array which has a single B&K 4138 microphone at its center, this array only uses B&K 4190 microphones). Two separate microphone array configurations were created to perform measurements on the face of the embossment and on the downstream surface. Since the circular embossment configuration is three-dimensional the array was also three-dimensional in the sense that it was designed so that the radial distance of the center of each microphone from the embossment center was the same (instead of the constant spanwise or streamwise separation used for the forward steps).

Figure 3-50 shows the 7 microphone array configuration used to measure the average unsteady pressure on the downstream surface of the circular embossment. As seen in the figure the microphones are arranged in an arc whose distance from the center of the embossment is $r_D - h$ with r_D being the embossment radius and h being the embossment height. This arrangement ensures that the distance of each microphone's center from the upper corner of the embossment is exactly one step height, the angular separation measured from embossment center is $\psi = 1.8^\circ$ and the separation between each microphone along the arc is 14.9 mm. The array is arranged such that the axis representing the spanwise center of the embossment passes through the center of the microphone in the middle of the array. On either side of this microphone there are three microphones separated by a constant angular separation.

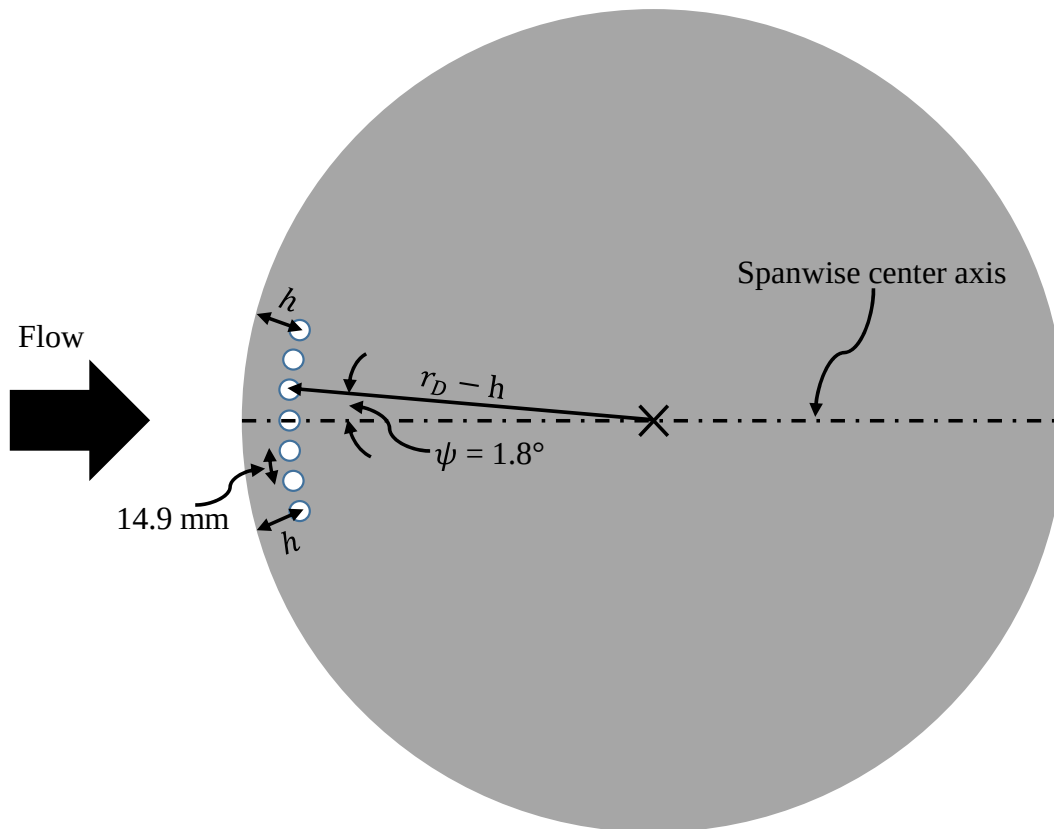


Figure 3-50 Microphone array configuration used to measure spatially averaged unsteady pressure on the downstream surface of the circular embossment.

Figure 3-51 (a) and (b) show the microphone array configuration used on the vertical face of the embossment in top and frontal view respectively. As seen in the top view the array on the face also has 7 microphones with the axis representing the spanwise center of the embossment passing through the center of the microphone in the middle of the array. On either side of the microphone in the middle there are three microphones which are separated by a constant angular separation of $\psi = 6.4^\circ$. This angular separation is wider than that used for the array on the downstream surface because the installation of the microphones in the face is limited by the length of the preamplifiers attached to the microphones. Reducing the angular separation any further than 6.4° would have caused microphone/preamplifier combination to overlap each other. Since the microphones are all mounted flush with the vertical face of the embossment their radial distance from the center of the embossment is equal to the embossment radius. The angular separation used in this array configuration yields a center to center separation (along the face of the embossment) of 50.8 mm between the microphones as shown in the figure. The distance to the center of each microphone in the array from the inside corner of the embossment is 9.5 mm as shown in the frontal view of Figure 3-51 (b).

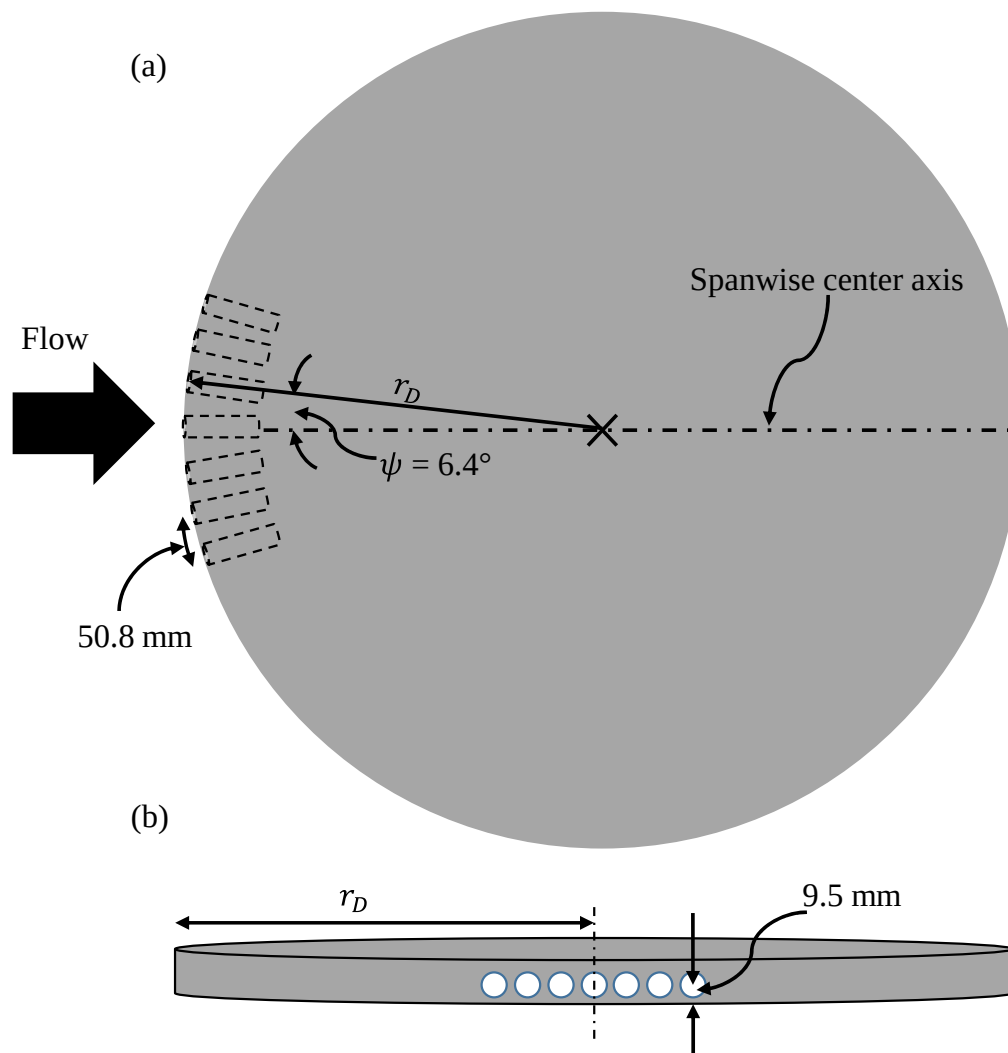


Figure 3-51 Microphone array configuration used to measure spatially averaged unsteady pressure on the vertical face of the circular embossment. (a) Top view showing the radial and angular position of the microphones and (b) Frontal view showing the distance of each microphone in the array from the inside corner of the embossment.

To arrange the microphones in a circular arc the array holder was designed using rapid prototyping. The two side arcs (*Arc 1* and *Arc 3* in Figure 3-47) which are part of the embossment assembly were used

to create accommodations for these array holders. Figure 3-52 (a) and (c) show slots which were created in two different arcs for installation of array holders on the downstream surface and on the face of the embossment respectively. As shown in Figure 3-52 (a) the slot for downstream array is simply a 9.5 mm wide opening with circular edges. The microphone array holder which is installed in this opening is shown in Figure 3-52 (b). This array holder which is rapid prototyped has the same width as the slot in the circular embossment and contains seven 12.7 mm diameter holes to accommodate the B&K 4190 microphones. The depth of this array holder is 57 mm and supports the preamplifiers which are attached to the microphones. For measurement on the embossment face a 190 mm x 140 mm x 9.5 mm slot is created on the second arc (which is the same dimension as the arc shown in Figure 3-52 (a) and is installed on the opposite side). The array holder which is installed in this slot is shown in Figure 3-52 (d) and has the same height (9.5 mm) and the same curvature at its face as the slot in Figure 3-52 (c).

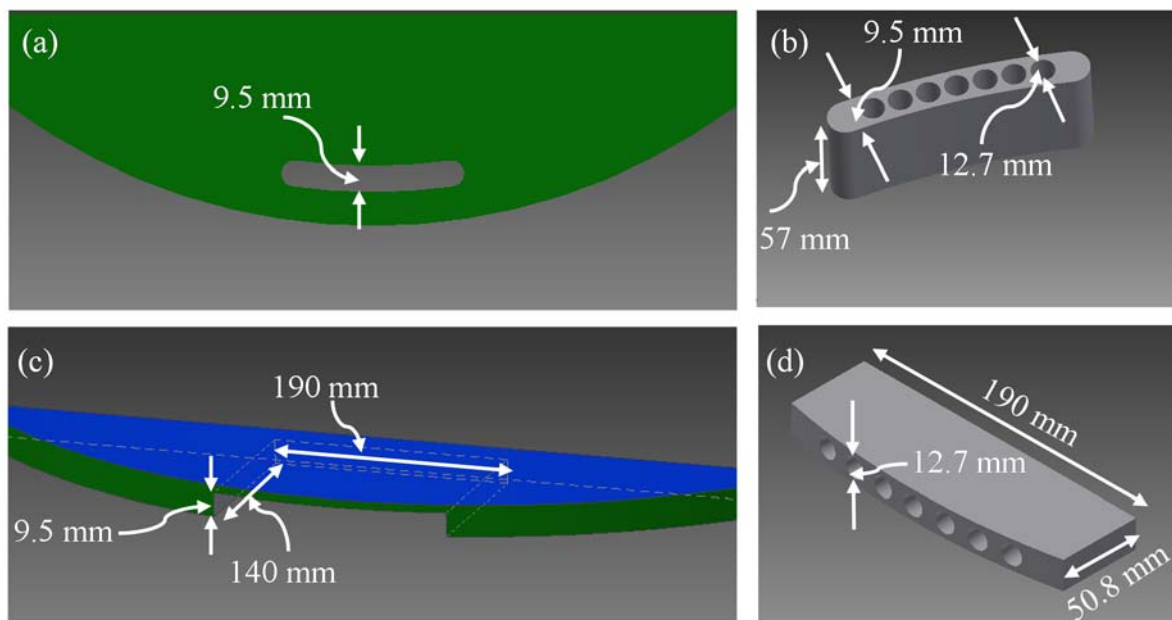


Figure 3-52 Microphone array installation setup for measurement on circular embossment. (a) 9.5 mm wide slot with circular edges created on the downstream surface of the embossment to accommodate the array holder shown in (b). (c) 190 mm x 140 mm x 9.5 mm slot created on the face of the circular embossment to accommodate the microphone array holder shown in (d). Note that the slots shown in (a) and (c) are created on two different arcs which are part of the circular embossment assembly shown in Figure 3-47.

Since the two array holders are installed on the opposite arcs, the measurements were first made with the array installed on the downstream surface of the embossment and then the embossment was turned around 180 degrees to perform measurements on the face. This procedure has been illustrated in Figure 3-53 (a) and (b) which shows the embossment configuration for measurements on the downstream surface and the face respectively. As seen in the figure, first the embossment was installed on the false wall such that the arc with the slot for the downstream surface array was on the upstream side. After this measurement was complete, the embossment was rotated 180 degrees as shown in Figure 3-53 (b) such that the arc with the slot for the face array was on the upstream side. Since the circular embossment is a symmetric geometry both the configurations depicted in Figure 3-53 are equivalent. The two microphone array configurations installed on the circular embossment during measurements have been pictured in Figure 3-54.

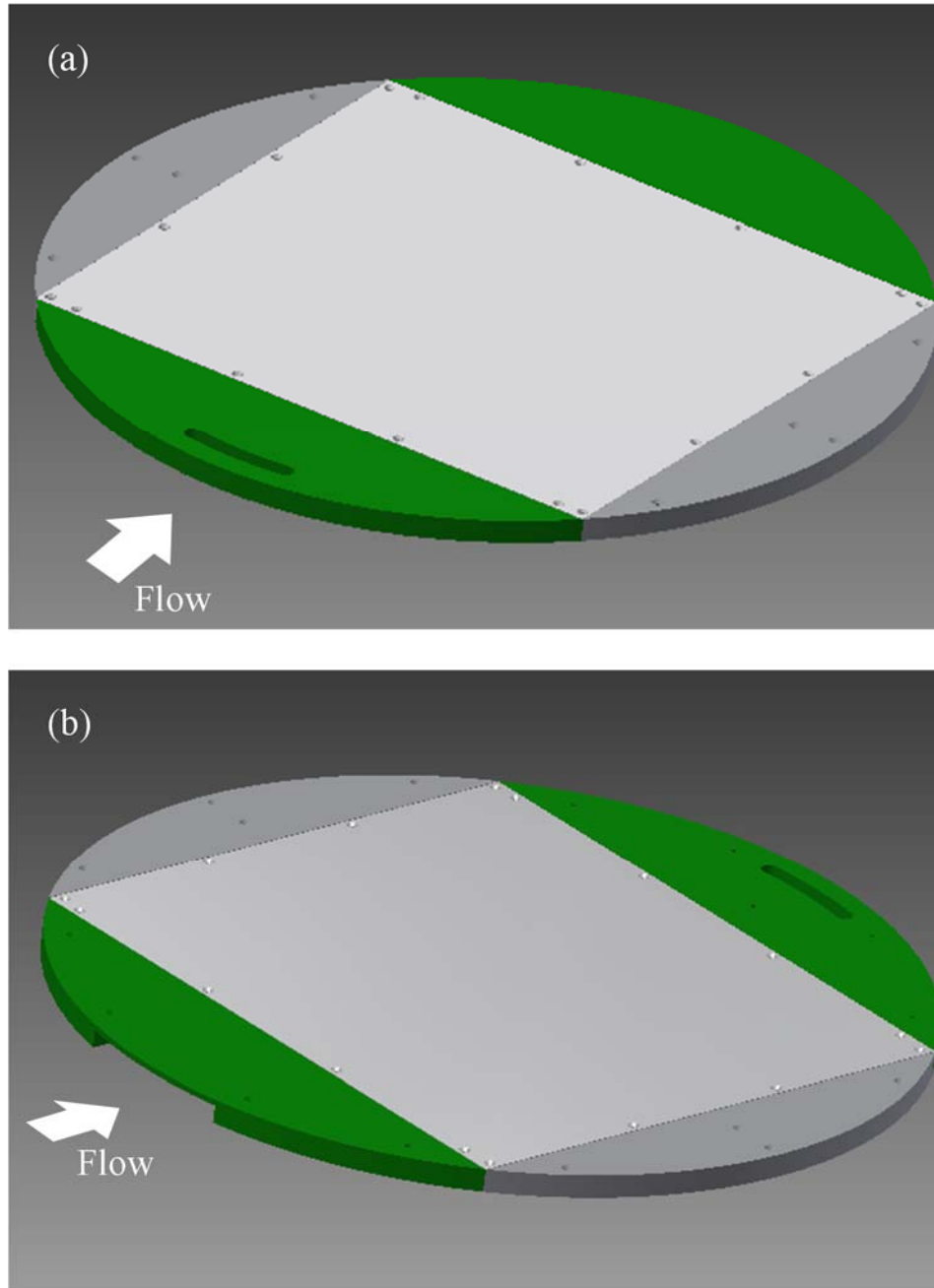


Figure 3-53 Illustration of setup used for measurement of spatially averaged unsteady pressure on circular embossment. (a) Measurement of pressure on the downstream surface of the embossment. (b) Embossment turned around 180 degrees to perform measurements on the face of the embossment.

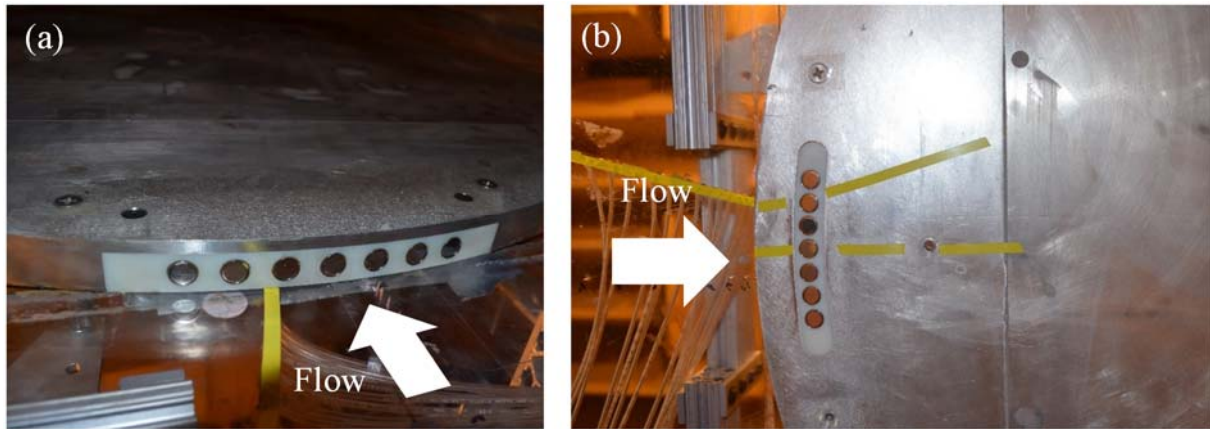


Figure 3-54 Seven microphone surface pressure array installed on the circular embossment during a typical measurement run. (a) Array on vertical face of the embossment and (b) Array on the downstream surface of the embossment.

3.1.10 Far Field Sound Measurement from Flow over Wall Mounted Circular Embossment

The far field sound from the circular embossment was measured using the same 117 microphone phased array used to measure sound from rounded forward steps. This array and the associated data processing method described in detail in §3.1.6 to obtain sound measurements from forward steps are also applicable for circular embossment measurements. The phased array was kept in the same position relative to the false wall for far field sound measurement. Figure 3-55 shows the location of the phased array center (which is the observer in present work with respect to the projection of the embossment on the false wall. As seen in the figure the shortest distance from the inside corner of the embossment to the observer is $R = 2.54$ m and it lies in a plane which is approximately 3° offset from the spanwise centerline of the circular embossment. The angle θ shown in the figure lies in this 3° offset plane.

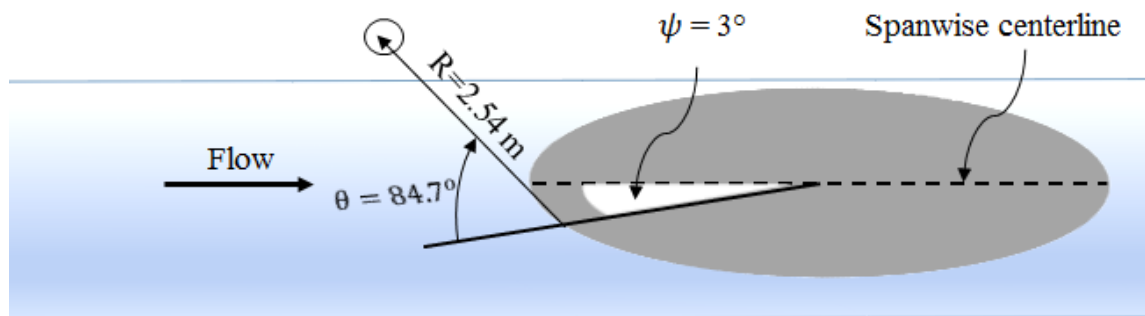


Figure 3-55 Observer location (phased array center) with respect to the wall mounted circular embossment. Note that the image shows the projection of the embossment on the false wall and all the distances and angles are defined with respect to this projection.

3.2 Results and Discussion

In the following sections we will discuss the results from measurements made for flow over two-dimensional forward steps followed by three-dimensional wall mounted circular embossment. We will first consider the near field measurements on flow over rounded forward steps which will be followed by a discussion on the far field sound radiated by these discontinuities. The presentation and discussion of near field results from measurements on the circular embossment will be discussed next followed by a discussion on the far field sound measurements for this geometry.

3.2.1 Rounded Forward Facing Steps Flow Structure

3.2.1.1 Separation and Reattachment Lengths for Rounded Forward Steps

The separation and reattachment lengths were obtained using the image analysis of oil flow visualization results described in §3.1.3 for all four rounded forward steps and two free stream velocities have been shown in Table 3-3. At the top of each table the flow conditions (undisturbed free stream velocity (U_∞), Reynolds number based on momentum thickness (Re_θ) and step height to boundary layer thickness (h/δ)) have been shown for each case. The separation (x_s) and reattachment lengths (x_r) have been shown in terms of step heights. Also shown are the uncertainty bounds (in step heights) for each case calculated using method described in §3.1.3. Note that the results for $r/h = 6.25\%$ at $U_\infty = 30$ m/s are not shown due to inconclusive flow pattern reflected in flow visualization images. At the bottom left of the table the separation and reattachment lengths measured in the wall-jet flow (see §2.2.1 for details) for the same four rounding radii to step height ratio as in the present work are shown along with the uncertainties in those measurements. The bottom right of the table shows the reattachment length for three comparable rounding of step corner calculated by Hao and Wang (2014) using LES. Note that their step height to boundary layer ratio is very similar to the present work, however, their Reynolds number is much lower.

Looking at the table on the left we see that while the separation upstream of the step is weakly dependent on the corner rounding the downstream reattachment has a strong dependence on it. Doubling the Reynolds number leads to a slight increase in both the separation and reattachment lengths with the latter again showing strong reduction with increasing rounding. Note that for the highest rounding of $r/h = 25\%$ the absolute uncertainty ($\Delta x_r/h$) is smaller but when compared to the overall length of the separation bubble this uncertainty is quite large. This is because for this particular rounding the separation bubble was found to be weak which resulted in slightly ambiguous flow visualization results. Comparing the present results to the wall-jet measurements we see that for the unrounded step the separation length in wall-jet is slightly larger which could be due to a much higher step height to boundary layer ratio in those measurements. Rounding the step corner in wall-jet leads to a slight reduction in separation length which is not seen the present measurements. The reattachment lengths for each rounded forward step are slightly smaller in the wall-jet measurements (except for $r/h = 25\%$ where the uncertainties in both measurements are high) which is possibly due to a much higher Reynolds number in the present flow. Overall, the variation of separation and reattachment length with rounding of step corner is consistent in both the facilities in that the separation length is a weak function of rounding while the reattachment length depends strongly on it.

Compared to the Hao and Wang's (2014) simulation the present lengths for $r/h = 0\%$ and 12.5% are slightly higher which is likely due to the present Reynolds number being 3 and 6 times higher than the computation. For the highest rounding ($r/h = 25\%$) considered in the present work the reattachment lengths are much smaller than the simulations. It is believed that this discrepancy is due to high uncertainty in measurements for $r/h = 25\%$ step which arises because of the ambiguity in the flow visualization results caused by the weak separation bubble for this step.

Table 3-3 Separation and reattachment lengths for rounded forward steps at two free stream velocities.

$U_\infty=30\text{m/s}, h/\delta=26.2\%, Re_\theta=15,500$					$U_\infty=60\text{m/s}, h/\delta=26.8\%, Re_\theta=26,600$				
r/h	x_s/h	$\Delta x_s/h$	x_r/h	$\Delta x_r/h$	r/h	x_s/h	$\Delta x_s/h$	x_r/h	$\Delta x_r/h$
0%	1.05	0.4	3.0	0.45	0%	1.5	0.34	3.2	0.43
6.25%	-	-	2.1	0.64	6.25%	1.7	0.3	2.2	0.48
12.5%	1.3	0.54	1.6	0.54	12.5%	1.31	0.45	1.6	0.47
25%	1.09	0.55	0.4	0.15	25%	1.6	0.39	0.4	0.13

Wall-jet, $h/\delta=84\%, Re_\theta=926$					Hao and Wang (2014), $h/\delta=26\%, Re_\theta=4755$	
r/h	x_s/h	$\Delta x_s/h$	x_r/h	$\Delta x_r/h$	r/h	x_r/h
0%	2	0.4	2.7	0.2	0%	2.44
6.25%	1.6	0.4	1.6	0.2	12.5%	1.41
12.5%	1.6	0.4	1.2	0.2	25%	1.05
25%	1.7	0.4	1	0.5		

3.2.1.2 Mean Wall Pressure

To further understand the variation of the flow structure with corner rounding we now consider the mean pressure variation. Figure 3-56 (a) and (b) shows the mean wall pressure coefficient (C_p) as a function of streamwise distance in step heights (x/h) for $r/h = 0\%$ step at velocities of 30 m/s and 60 m/s respectively. The Reynolds number based on step height for these velocities are approximately 46,350 and 92,700. The vertical solid line on each plot represents the streamwise location of the vertical face of the step. Two vertical dashed lines on each plot show the mean separation and reattachment lengths (x_r and x_s respectively) found using oil flow visualization (Table 3-3). Looking at Figure 3-56 (a) we see that as the flow approaches the step an adverse pressure gradient (positive C_p) begins to develop and the pressure rises rapidly until flow separates after which a plateau very close to the step face is seen. Downstream of the step a strong negative value of pressure coefficient is observed due to rapid acceleration of the shear layer over the step. The pressure then begins to increase due to shear layer reattachment reaching a near constant value around 10 step heights downstream of the step. At the last measurement station $x/h = 16.4$ the value of pressure coefficient is about -0.08 which shows indicates that the flow has not quite recovered to its undisturbed form. This result is not surprising given the far downstream influence of effects of forward steps shown by Awasthi *et al.*'s (2014) measurements. Increasing the velocity to 60 m/s (Figure 3-56 (b)) has no qualitative effect on the mean pressure and it shows the same effects of separation/reattachment process as for the lower velocity.

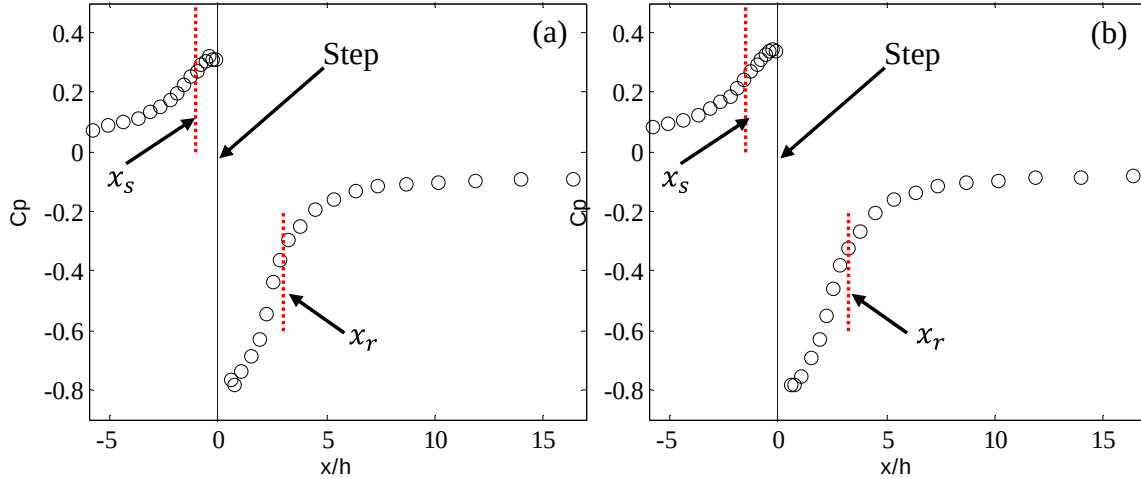
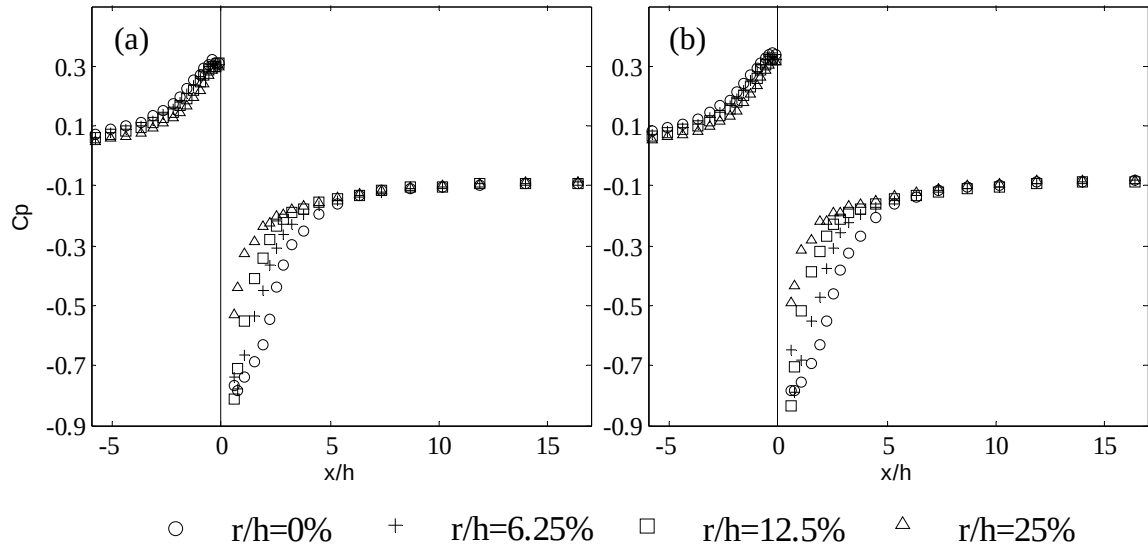
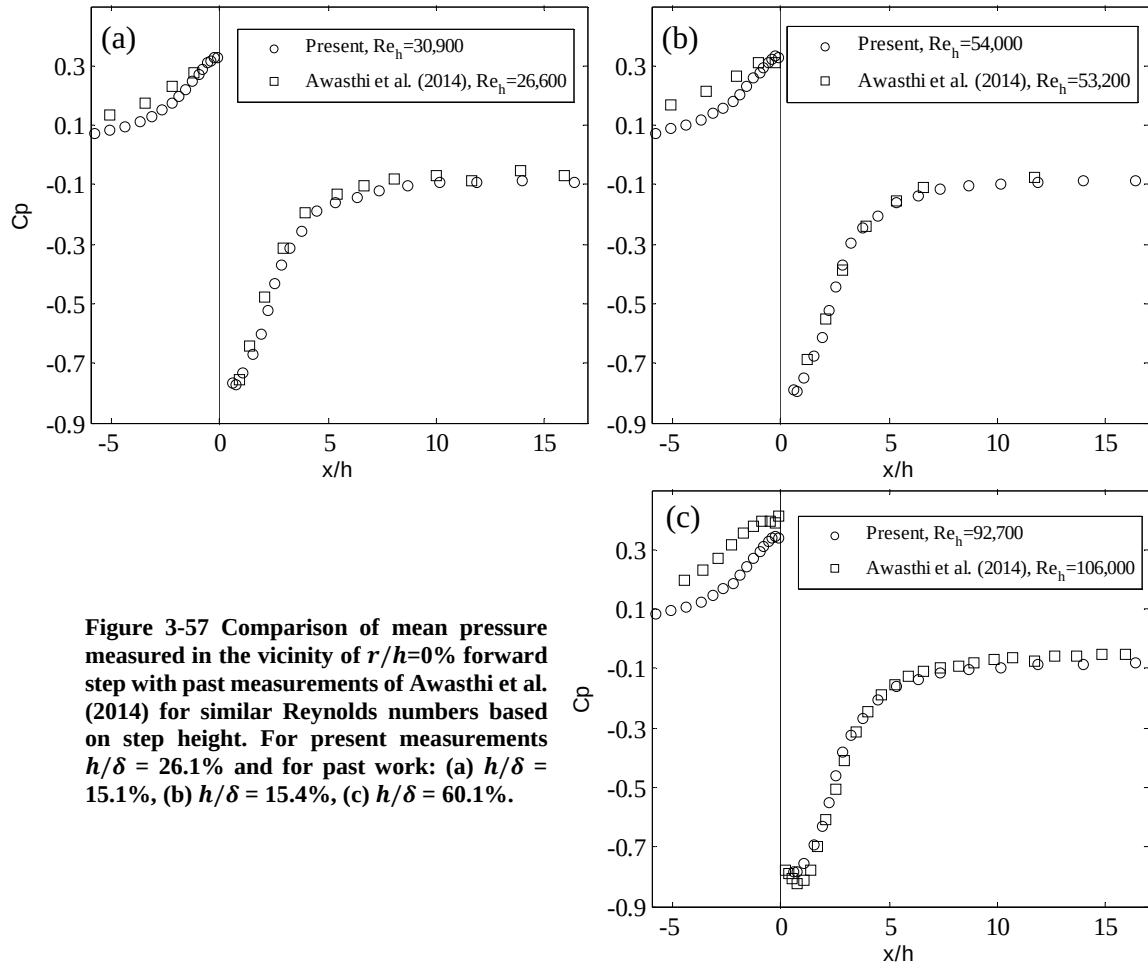


Figure 3-56 Mean wall pressure in front and behind an unrounded forward step. (a) $U_\infty = 30$ m/s, (b) $U_\infty = 60$ m/s. Also shown on the figure is the location of step face by solid vertical line and upstream separation/reattachment lengths found using oil flow visualization by dashed vertical line.

Awasthi *et al.* (2014) had measured the mean pressure in the vicinity of an unrounded step in the same facility for different step heights and Reynolds numbers. Figure 3-57 (a)-(c) shows a comparison of present measurements with their results at three comparable Reynolds numbers (based on step height, Re_h) but with different step height to undisturbed boundary layer ratios (h/δ). The location of the step is shown with a vertical line at $x/h = 0$. Looking at each of the plots in the figure it can be seen that downstream of the step the pressure distribution is independent of h/δ and for similar Re_h is approximately the same. This result is consistent with Awasthi *et al.*'s (2014) conclusion that the Re_h is a more influential parameter in governing the downstream behavior than h/δ . Upstream of the step however, there is a noticeable difference between the present and past measurements with the present measurements being consistently lower in each case. This discrepancy between Awasthi *et al.*'s (2014) largest step height (Figure 3-57 (c)) and present measurements could be explained by the difference in step height. However, the fact that their pressure coefficient for lower step heights (Figure 3-57 (a) and (b)) is also higher than the present measurements suggests some deeper issue with either the present or the past measurements.

Figure 3-58 (a) and (b) shows the effect of rounding the step corner on the mean pressure distribution at freestream velocities of 30 m/s and 60 m/s respectively. Looking at Figure 3-58 (a) it can be seen that rounding the step corner results in a slight decrease in pressure levels immediately upstream of the step. However, this reduction is small and is consistent with the oil flow visualization results which showed that the upstream separation lengths are a weak function of corner rounding. On the other hand the rounding effects the downstream pressure distribution more severely. The pressure immediately downstream of the step is lower for small r/h and its recovery is slow. This is because the downstream separation bubble extends much farther for lower rounding which leads to slower recovery of wall pressure. The results of the flow visualization in the previous section and the mean pressure results presented here clearly show that the flow immediately downstream of the forward step is very sensitive to the rounding of the corner.



The downstream behavior of mean pressure is largely consistent with the flow structure observed using oil flow visualization results and this can be demonstrated by a closer examination of the values of pressure coefficients around the mean reattachment point for each rounded step. To perform this comparison we apply a simple polynomial fit to the mean pressure data downstream of this step. This curve fit applied to the mean pressure data at $U_\infty = 30$ m/s and 60 m/s has been shown in Figure 3-59 (a) and (b) respectively. Looking at the figures it can be seen that the polynomial fit approximates the shape of the pressure curve quite well within the region of interest. The general form of this polynomial fit is given by

$$Cp_{fit} = az^8 + bz^7 + cz^6 + dz^5 + ez^4 + fz^3 + gz^2 + hz + i \quad (3-43)$$

where $z = (x/h-5.5)/4.7$ and the values of coefficients a through i are a function of rounding of step corner. The values of these coefficients for each rounding at two velocities are shown in Table 3-4.

Table 3-4 Coefficients for the polynomial fit to the mean surface pressure downstream of rounded forward steps.

	$U_\infty = 30$ m/s				$U_\infty = 60$ m/s			
r/h	0%	6.25%	12.5%	25%	0%	6.25%	12.5%	25%
a	0.048	0.060	-0.011	-0.040	0.038	0.114	-0.039	-0.025
b	-0.257	-0.279	0.069	0.196	-0.207	-0.532	0.201	0.129
c	0.388	0.261	-0.152	-0.247	0.323	0.514	-0.294	-0.188
d	0.074	0.415	0.140	-0.110	0.031	0.714	0.004	-0.022
e	-0.597	-0.694	-0.048	0.302	-0.476	-1.166	0.206	0.187
f	0.419	0.102	0.014	0.009	0.390	0.045	0.014	-0.021
g	-0.127	0.109	-0.041	-0.144	-0.168	0.285	-0.122	-0.095
h	0.101	0.071	0.069	0.074	0.132	0.069	0.073	0.074
i	-0.152	-0.155	-0.139	-0.130	-0.158	-0.161	-0.138	-0.128

We can now evaluate equation (3-43) at the values of reattachment lengths ($x = x_r$) found using oil flow visualization to obtain an estimate of the pressure coefficient in the reattachment region. This evaluation yields a Cp of -0.35, -0.37, -0.38 and -0.52 for $r/h = 0\%$, 6.25%, 12.5% and 25% respectively at $U_\infty = 30$ m/s. And, for $U_\infty = 60$ m/s the calculated values of Cp are -0.33, -0.37, -0.35 and -0.58 for $r/h = 0\%$, 6.25%, 12.5% and 25% respectively. We see that except for the highest rounding the pressure coefficient has approximately the same value at the reattachment lengths computed using oil flow visualization for both velocities. The discrepancy for the highest rounding is likely due to insufficient resolution of the flow visualization images owing to the weak separation bubble associated with this rounding. This particular rounding is also the case where Hao and Wang's (2014) LES simulation results (also shown in Table 3-3) show largest discrepancy with the present measurements which cannot be explained on the basis of Reynolds number difference. In fact, if we apply their reattachment length result ($x_r=1.05$) to the polynomial curve fit result to the present data we find a Cp value of -0.35 which is approximately the same as those for lower rounding.

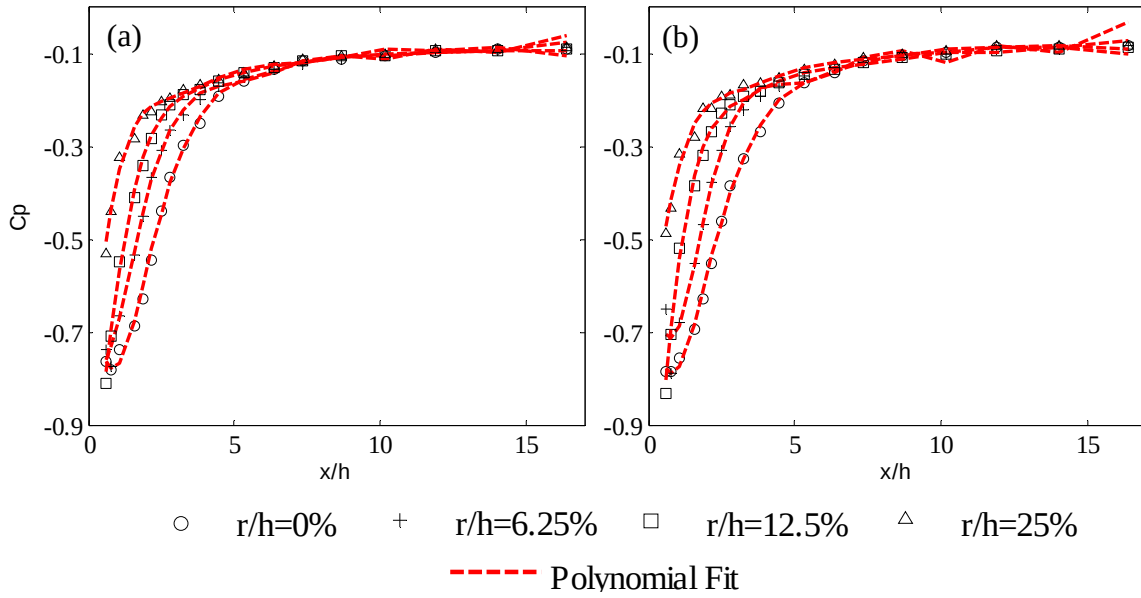


Figure 3-59 Polynomial fit to the mean wall pressure data measured downstream of rounded forward steps. (a) $U_\infty = 30$ m/s, (b) $U_\infty = 60$ m/s.

The correlation between the reattachment lengths and the mean pressure distribution can further be demonstrated by plotting the mean pressure coefficient as a function of downstream distance normalized on reattachment length for each rounding. This normalization is shown in Figure 3-60 (a) and (b) for free stream velocities of 30 m/s and 60 m/s respectively. We can see that the scaling on reattachment length collapses the mean pressure well for rounding up to $r/h = 12.5\%$ and $r/h = 25\%$ is a clear outlier. This observation is consistent with the previous discussion on the values of pressure coefficients found in the reattachment region using curve fits. We can then also scale the streamwise distance for $r/h = 25\%$ step on the reattachment value found by Hao and Wang (2014). This normalization is shown in Figure 3-61 (a) and (b) for $U_\infty = 30$ m/s and 60 m/s respectively. Looking at the figure we can see that using their value for the reattachment length in case of $r/h = 25\%$ step provides a better collapse of the data. This normalization along with the values of C_p at reattachment location discussed earlier seem to suggest that the flow visualization is either under predicting the reattachment length for $r/h = 25\%$ or the flow structure undergoes a major change as the step is rounded beyond $r/h = 12.5\%$.

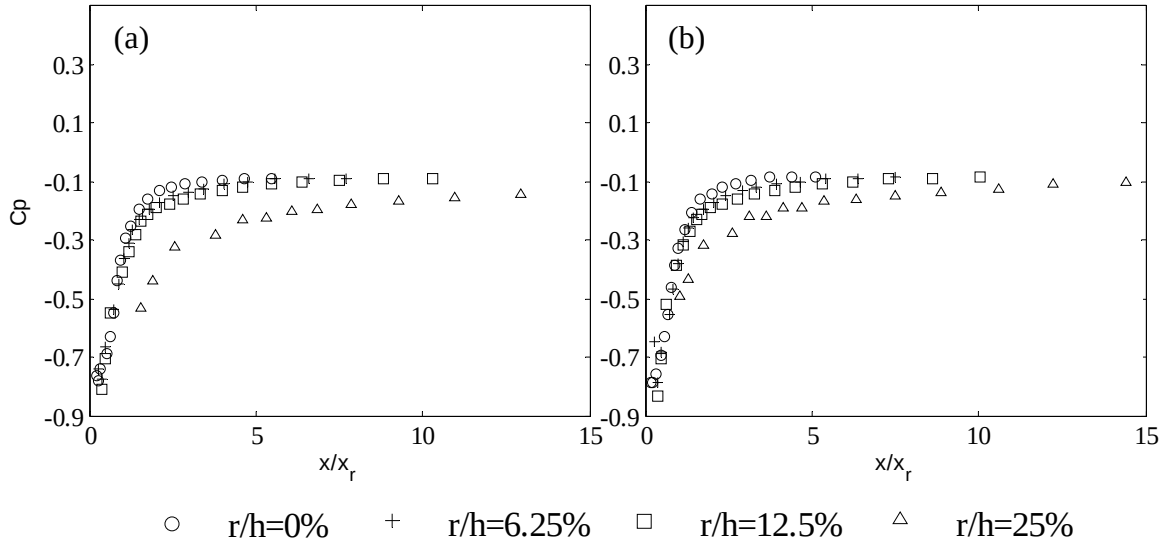


Figure 3-60 Mean wall pressure coefficient as a function of streamwise distance normalized on the reattachment length for each corner rounding. (a) $U_\infty = 30$ m/s, (b) $U_\infty = 60$ m/s.

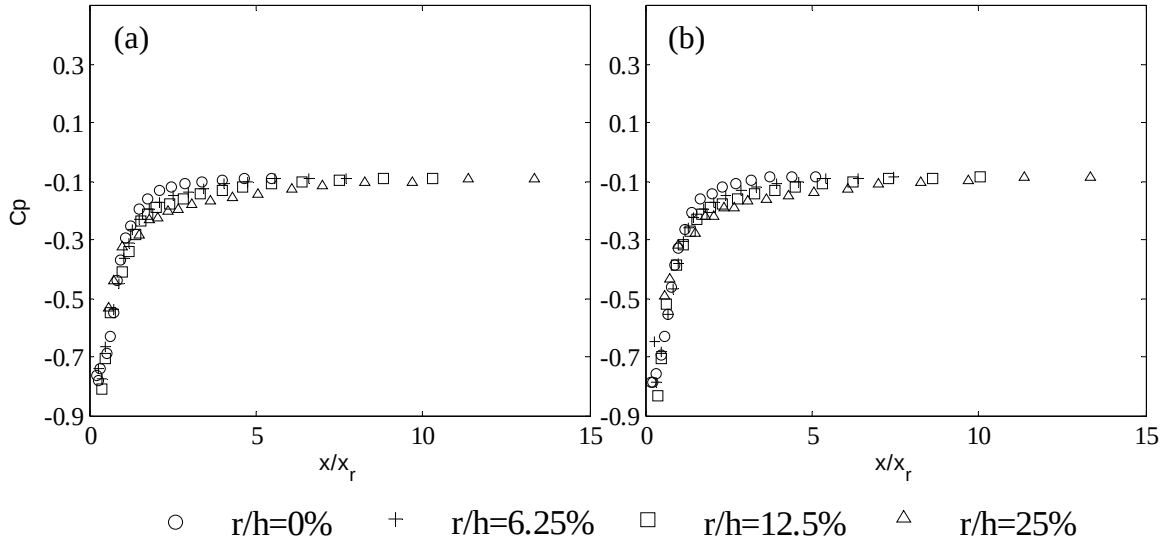


Figure 3-61 Mean wall pressure coefficient as a function of streamwise distance normalized on measured reattachment lengths for $r/h \leq 12.5\%$ and computed reattachment length (by Hao and Wang (2014) for $r/h = 25\%$. (a) $U_\infty = 30$ m/s, (b) $U_\infty = 60$ m/s.

3.2.2 Single Point Surface Pressure Fluctuations on Flow over Rounded Forward Steps

We now consider the pointwise unsteady surface pressure in the vicinity of rounded forward steps measured using the probe microphone. The results in this section will only be presented for measurements made at $U_\infty = 30$ m/s since the results for $U_\infty = 45$ m/s are qualitatively the same. The spectra have been background subtracted through the use of a reference microphone as described in §3.1.4 and have been bin combined using 12 bins per octave. It is well known that a finite diameter microphone will attenuate the boundary layer pressure due to spatial averaging across its diaphragm. This issue of spatial averaging was earlier discussed in detail in §3.1.5.1 but for a different set of microphones. For the probe microphone we estimate the frequencies above which the attenuation due to spatial averaging will become significant by

using the criteria defined by Corcos (1963). According to this criteria the attenuation will become significant ($> 3\text{dB}$) for frequencies greater than those associated with the half wavelength based on convection velocity and the diameter of the microphone i.e.

$$f_{cutoff} = \frac{U_c}{2d} \quad (3-44)$$

where U_c is the convection velocity within the boundary layer and d is the diameter of the sensing area which, in this case, is the diameter of the probe microphone of 0.75 mm. Since the convection velocity for the step flows is not known *a priori* we assume the conventional $U_c = 0.6U_\infty$ used for smooth wall boundary layers. Then based on the probe microphone diameter the frequencies at which the attenuation is expected to become significant is about 12 kHz. Therefore, we will show results only up to this frequency.

Figure 3-62 shows the spectral density of surface pressure fluctuations (in dB, $p_{ref} = 20 \mu\text{Pa}$) at $U_\infty = 30 \text{ m/s}$ for all four step corner rounding measured at four different locations upstream of the step. Since no measurements were made for undisturbed boundary layer in the present work, each plot also shows the undisturbed boundary layer spectra measured by Awasthi *et al.* (2014) under similar flow conditions. Looking at Figure 3-62 (a) which shows the spectra measured at 5.9 step heights upstream of the step we first note that the rounding of the step corner has no influence on the spectra at this location. Compared to the undisturbed boundary layer, the spectra for each step shows suppressed higher frequencies due to the adverse pressure gradient which begins to lift the smaller, faster moving scales away from the surface. As we move further downstream towards the step to $x/h = -2.9$ (Figure 3-62 (b)) the higher frequencies are suppressed further while the lower frequencies now are elevated compared to the undisturbed boundary layer. This measurement location is immediately upstream of the mean separation position and the spectra are therefore likely influenced by larger, slower moving structures associated with flow separation, resulting in the elevated low frequencies. As we move to $x/h = -1.4$ (Figure 3-62 (c)) which is within the separation region (once the uncertainties in the oil flow visualization are taken into account) we see the lower frequencies that are higher by about 18-20 dB compared to the undisturbed flow while the higher frequencies are orders of magnitude lower. The last measurement station shown in this figure ($x/h = -0.9$ Figure 3-62 (d)) is within the separation bubble for each step and shows lower frequencies that can exceed the undisturbed flow by more than 20 dB due to the large scale structures associated with the separation bubble dominating the spectra at this location. The spectra within the separation bubble show negligible influence of rounding the step corner and this is consistent with the observation made during discussion of mean pressure and flow visualization results that the flow immediately upstream of the step is nearly independent of the corner rounding.

The spectra measured at two locations on the face of the rounded forward steps are shown in Figure 3-63. Looking first at $y = 0.56h$ (Figure 3-63 (a)) we see that the overall shape of each spectrum is similar to that seen upstream of the step in that the lower frequencies are elevated and higher frequencies are suppressed due to the effects of flow separation. However, unlike the upstream spectra we see a slight effect of rounding on the spectra measured here with the pressure levels rising as rounding increases throughout the frequency range. For $r/h = 25\%$ step the pressure levels exceed those for an unrounded step by about 3 dB at low to mid frequencies.

This rise in fluctuating pressure on the step face with increasing rounding can be due to two possible reasons. Firstly, it is likely that at $y = 0.56h$, the probe microphone is exposed at least intermittently to flow passing over the separation region (Leclercq *et al.* (2001) showed the mean flow reattachment on the face of an unrounded step to occur between 0.5-0.6 step heights from the inside corner). This flow is being accelerated around the exterior corner of the step. The overall acceleration likely decreases as the step is rounded, but the local acceleration felt at the fixed measurement point will increase with step rounding, since rounding brings the corner closer to the measurement point. As pointed out by McGrath and Simpson (1987) a faster accelerating flow tends to have larger spanwise and streamwise coherent length scales which

would then explain the enhanced pressure fluctuations for higher corner rounding seen in Figure 3-63 (a). The second explanation is that pressures on the step face may be influenced by those in the separated flow downstream and, as shown in Hao and Wang's (2014) LES simulations, those pressure fluctuations can locally increase as the corner is rounded.

As we now move closer to the step corner along the step face ($y = 0.69h$, Figure 3-63 (b)) the spectrum takes on a different form with higher frequencies also rising above the undisturbed boundary layer levels. The effect of rounding the step corner on pressure fluctuations at this location is more severe than that of Figure 3-63 (a) with the levels for $r/h = 25\%$ exceeding the levels for $r/h = 0\%$ step by approximately 4 dB and 8 dB at mid and high frequencies respectively. We note that the measurement location of Figure 3-63 (b) is now completely outside expected reattachment region of the upstream separation and is also closer to the salient corner of the step. Since an accelerating flow would generally tend to elevate the higher frequencies, this may explain the behavior of the spectra seen in both Figure 3-63 (a) and (b).

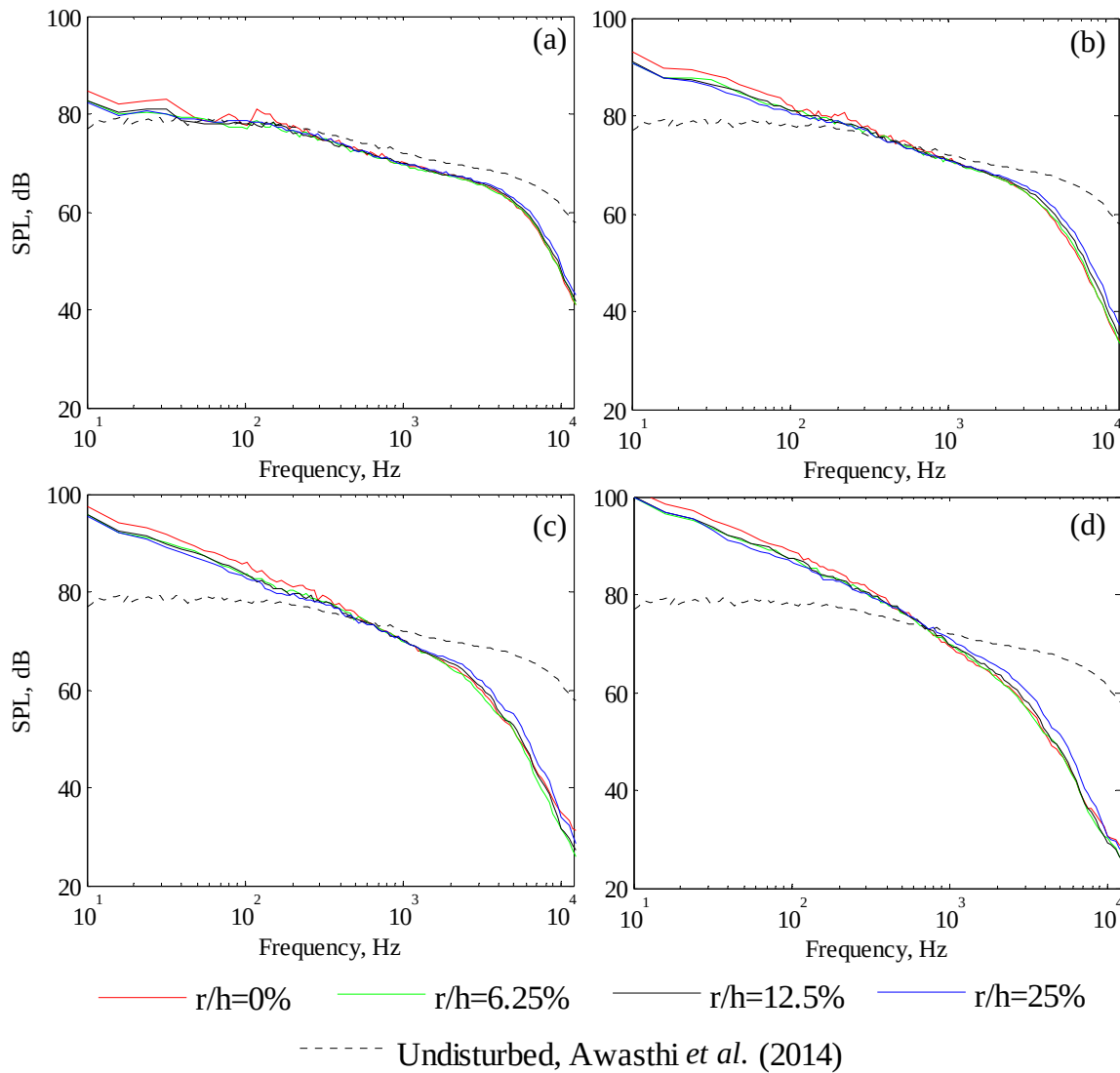


Figure 3-62 Single point surface pressure fluctuations spectra for rounded forward steps at four locations upstream of the step at $U_\infty = 30$ m/s. (a) $x/h = -5.9$, (b) $x/h = -2.9$, (c) $x/h = -1.4$ and (d) $x/h = -0.9$.

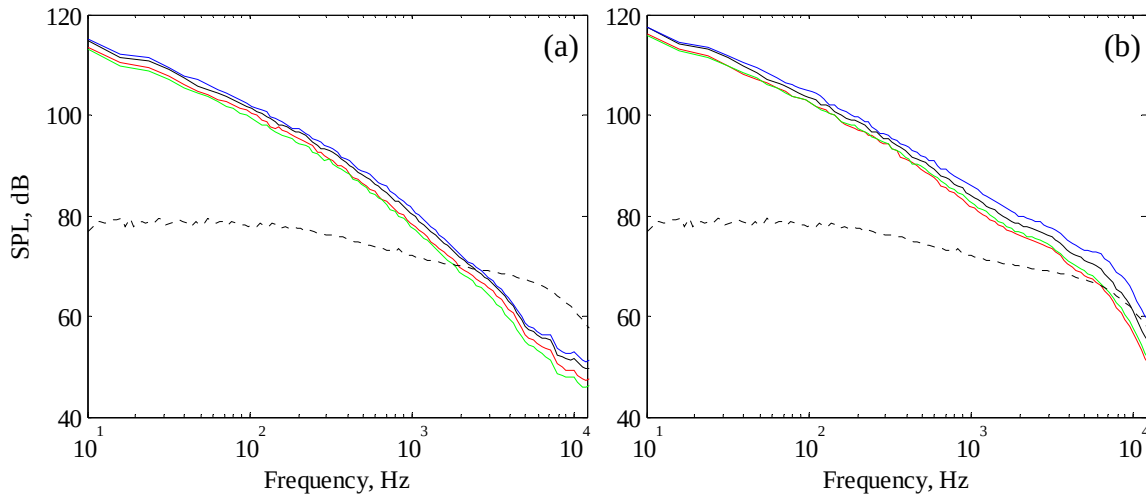


Figure 3-63 Surface pressure fluctuations measured on the face of rounded forward steps at $U_\infty = 30$ m/s. (a) $y = 0.56h$ and (b) $y = 0.69h$. Legend is the same as that for Figure 3-62.

Spectra measured downstream of the rounded forward steps at $U_\infty = 30$ m/s are shown in Figure 3-64 for four different measurement stations. Looking first at the station immediately downstream of the step ($x/h = 0.8$, Figure 3-64 (a)) we first note that unlike the spectra measured upstream of the step, these show a strong dependence on the corner rounding. For $r/h \leq 12.5\%$ the spectra show levels that exceed the undisturbed boundary layer flow by 30 dB at the lowest frequencies and that, below about 200 Hz are independent of rounding. At higher frequencies the spectral levels roll off, the roll-off frequency, and thus the high frequency spectral levels, increasing monotonically with rounding. Hao and Wang's (2014) LES simulations show that the region of peak turbulence kinetic energy, which they cite as directly responsible for the most intense pressure fluctuations, moves closer to the surface as the rounding of the step corner increases. The increase in pressure fluctuation energy at high frequencies may therefore be directly related to the separation bubble becoming smaller and more focused near the surface. Moving from $r/h = 12.5\%$ to 25% low frequency spectral levels (below about 1 kHz) and high-frequency levels increase. The reason for this change is that for $r/h = 25\%$ this measurement location falls outside the separation bubble and thus does not experience the full intensity of low frequency motion associated with the separated flows. Moving further downstream to $x/h = 1.6$ (Figure 3-64 (b)) we see that for $r/h \leq 12.5\%$ the spectra are still elevated throughout the frequency range while the spectrum for $r/h = 25\%$ drops further and is about 15 dB lower than the rest at the lowest frequencies. Moving further downstream to $x/h = 2.1$ (Figure 3-64 (c)) we now see the lower frequency content of the spectra dropping with increasing rounding and the spectra for $r/h = 12.5\%$ is about 5 dB lower than the unrounded step due to this location being within the reattachment zone for this step while for the two lower rounded steps it is still well within the separation bubble. At the last measurement station shown in Figure 3-64 (d) a significant variation of the low frequency content of the spectra with rounding is seen. This measurement station is downstream of the separation bubble for each rounding except $r/h = 0\%$ which is the reason for this low frequency variation.

Awasthi *et al.* (2014) measured surface pressure fluctuations downstream of unrounded forward steps of three different step heights in the same facility as used in the present work. Though their work mostly focused on studying the extent to which the forward steps disturb the boundary layer downstream of the step, they did measure pressure spectra within the separation bubble for their largest step height of $h/\delta = 60.1\%$. Their reattachment length which they measured using oil flow visualization was $x_r = 4.1h$ and their step height based Reynolds number was $Re_h = 106,000$. Both their step height and Reynolds number are more than twice in present work at $U_\infty = 30$ m/s. We can compare the present measurements made within the separation bubble downstream of unrounded step to their measurements made at similar positions relative to their reattachment length. Figure 3-65 (a) and (b) shows a comparison of the present

spectra with those of Awasthi *et al.*'s (2014) for comparable downstream locations in terms of reattachment lengths. For the measurements made within the first half of the separation bubble (Figure 3-65 (a)) we see that both the past and the present spectra have similar shapes but the present spectra are slightly lower (~ 4 dB) at lowest frequencies. In the measurements made in the latter half of the separation bubble (Figure 3-65 (b)) a similar discrepancy is seen between the two measurements. It seems that the present spectra are all shifted slightly to the right on the frequency axis which might be due to the difference in step height between the two measurements which as shown by Awasthi *et al.* (2014) dominates the behavior of surface pressure fluctuations downstream of the step.

The pressure fluctuations in the vicinity of forward steps are of interest to us because they are directly related to the unsteady loading on the surface which is believed to be the dominant sound source in low Mach number flows. The variation of the pressure fluctuations seen here indicates that, while on the face of the step the unsteady pressure does not vary much with rounding, the story on the downstream surface of the step is quite different. Here the streamwise extent over which large pressure fluctuations are seen reduces as the separation bubble length is reduced by the rounding. This could explain the far field sound reduction with rounding if we accept the hypothesis of Slomski (2011) who argued that the surface downstream of the step is the dominant noise source. However, one also has to consider that changing the rounding of the step corner also leads to change in the scattering efficiency of the step geometry. So, it might be that step face is still responsible for the sound being produced but the reduction in sound with rounding is due to reduced scattering efficiency of rounded steps. A third hypothesis put forth recently by Glegg *et al.* (2014) is that for an unrounded forward step the unsteady loading on the face and the top surface is one and the same and it is the resultant of these forces centered on the step corner which is the source of sound. Since we have measurements of pressure fluctuations (which are related to the unsteady loading) both on the step face and on the downstream surface we can compare the two in order to qualitatively test the hypothesis of Glegg *et al.* (2014). This has been done in Figure 3-66 which compares the pressure spectrum measured immediately downstream of the step ($x/h = 0.8$) to that measured on the face of the step at $y = 0.69h$. This figure does show some similarity in the form of the pressure spectra on the two sides of the corner, tending to support Glegg *et al.*'s (2014) idea. Of course, one has to remember that even though the pressure fluctuations are related to the unsteady loading the results here are not a unique indication of the latter because the correlation area of the pressure is not considered. This point will be further discussed in a later section where we will consider spatially averaged unsteady pressure measured by the surface microphone array which can be directly related to the unsteady loading.

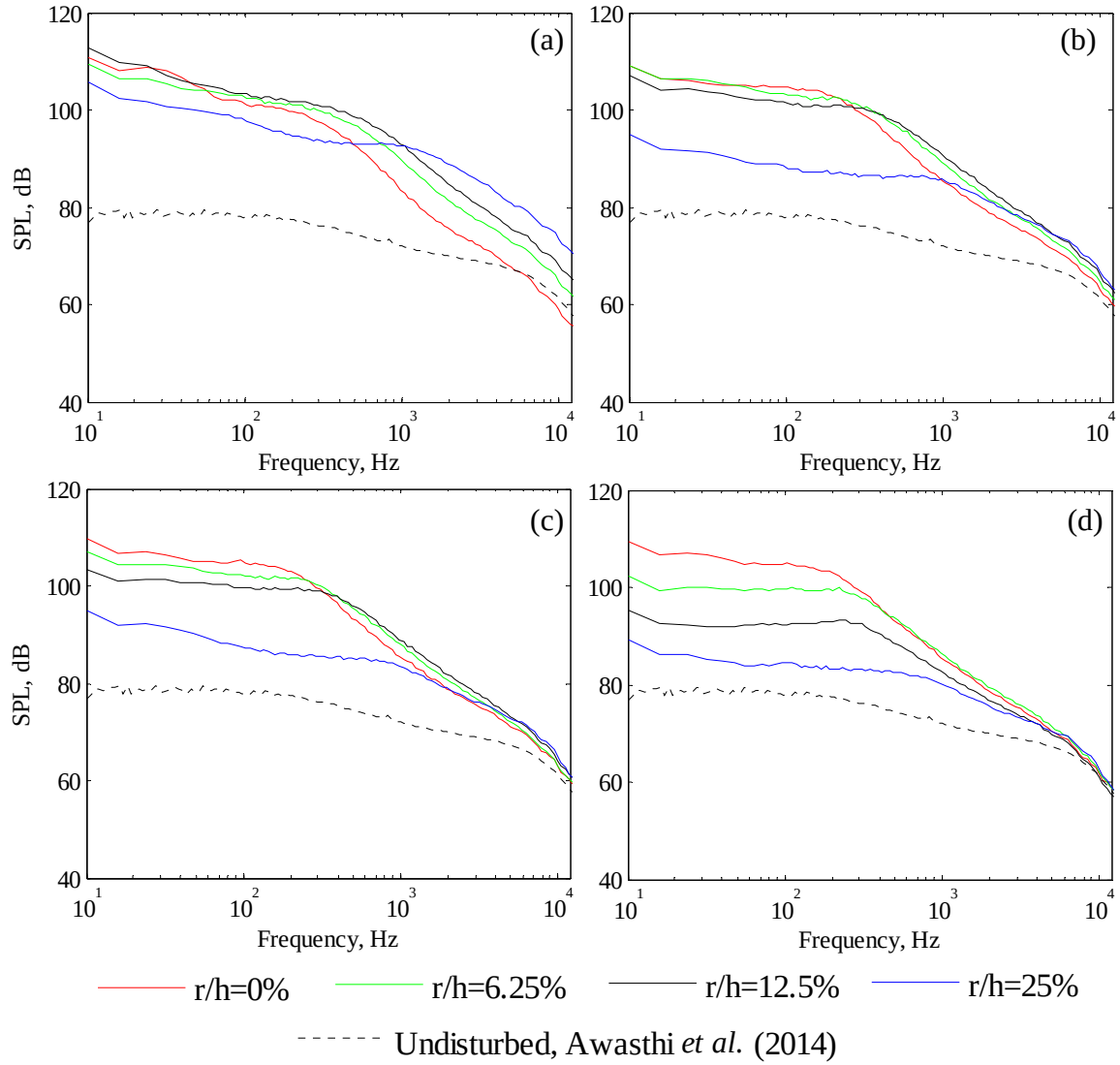


Figure 3-64 Single point surface pressure fluctuations spectra for rounded forward steps at four locations downstream of the step at $U_\infty = 30$ m/s. (a) $x/h = 0.8$, (b) $x/h = 1.6$, (c) $x/h = 2.1$ and (d) $x/h = 2.9$.

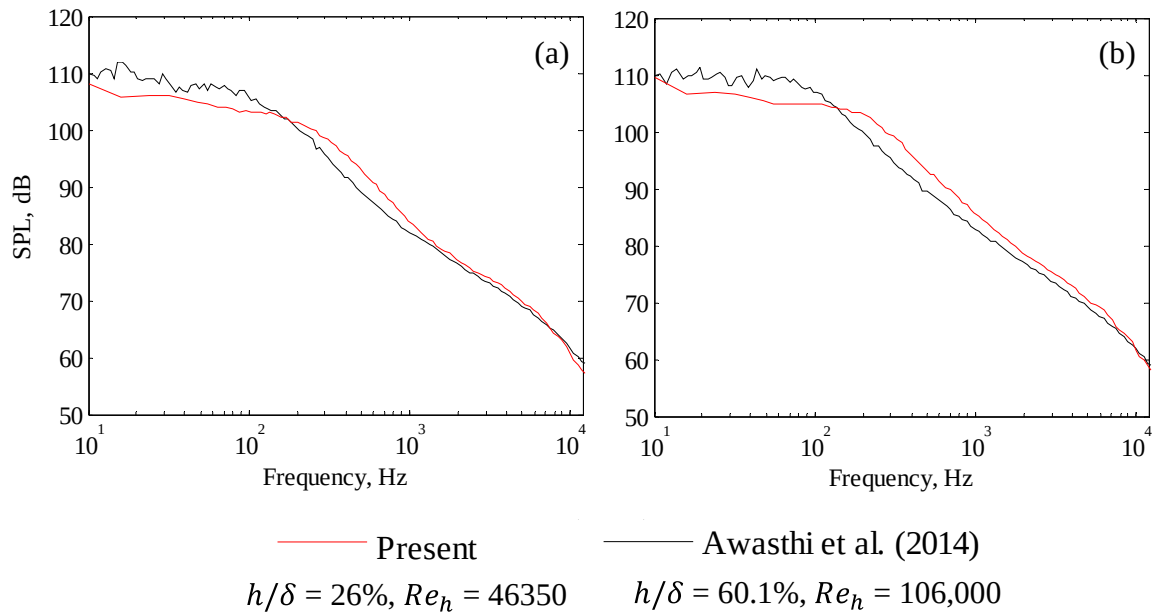


Figure 3-65 Comparison of spectra measured downstream of $r/h = 0\%$ forward step with those measured by Awasthi *et al.* (2014). (a) Present: $x/x_r = 0.38$, Awasthi *et al.* (2014): $x/x_r = 0.35$ and (b) Present: $x/x_r = 0.96$, Awasthi *et al.* (2014): $x/x_r = 0.9$

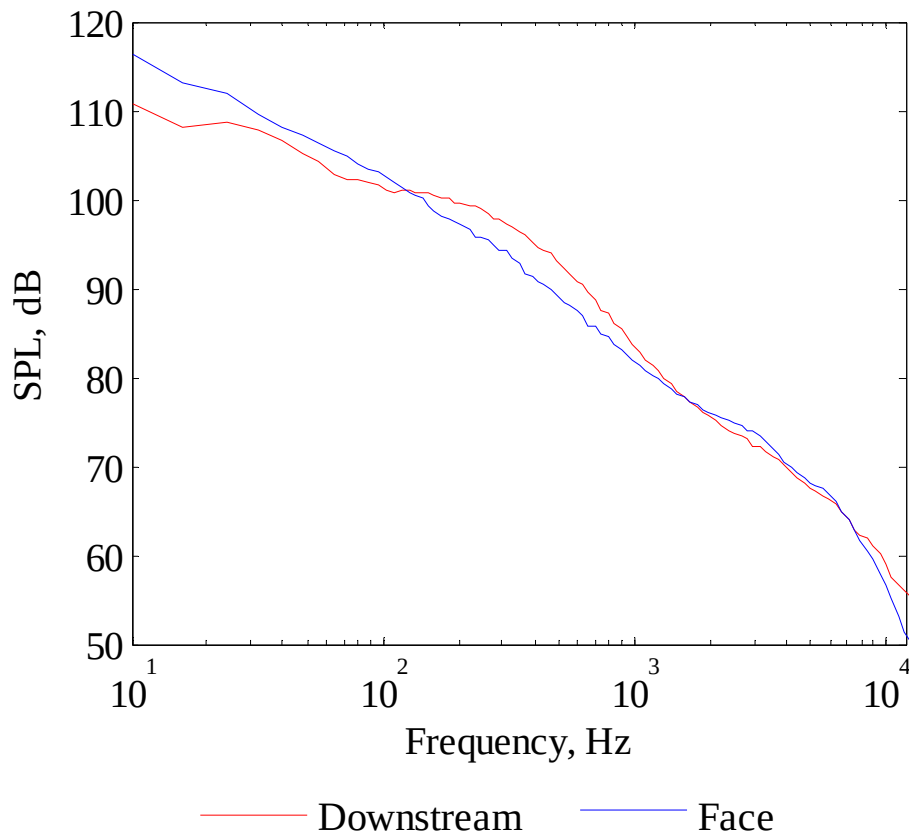


Figure 3-66 Comparison of the pressure spectra measured immediately downstream of $r/h = 0\%$ forward step at $x/h = 0.8$ for a flow velocity of $U_\infty = 30$ m/s with that measured on the step face at $y = 0.69h$.

To further understand the evolution of pressure fluctuations in presence of the step and to explore the effect of corner rounding on it we now look at the root mean square pressure (p_{rms}) calculated from the probe microphone measurements. The p_{rms} can be calculated in the frequency domain using Parseval's theorem and is given by

$$p_{rms} = \sqrt{\int_{f_{min}}^{f_{max}} \phi_{pp}(f) df} \quad (3-45)$$

where $\phi_{pp}(f)$ is the spectral density of surface pressure fluctuations, f_{min} and f_{max} are the lowest and the highest frequency bounds respectively within which the integration is performed. For the present calculation the minimum and maximum frequencies were chosen to be 10 Hz and 20,000 Hz respectively. It should be noted that one may expect the integration to be affected by the upper limit of the frequency range due to the spatial filtering of the pressure fluctuations by the finite size of the probe microphone. However, as we saw in Figure 3-62 and Figure 3-64, the pressure spectra in the vicinity of the step are dominated by lower frequencies which can be orders of magnitude higher than the higher frequencies. We would therefore expect the integration in equation (3-45) to be dominated by the lower frequencies which are not affected by the spatial filtering action of the microphone. In fact, this expectation was put to test by changing the upper limit of the integration and it was found that increasing f_{max} beyond 10,000 Hz had negligible impact on the outcome of the integration. Despite of this, the r.m.s pressure was obtained by integrating the spectra up to a frequency of 12 kHz which is the upper limit of the spectra shown here.

Figure 3-67 shows the p_{rms} normalized on the free stream dynamic pressure ($q_{\infty} = \rho U_{\infty}^2$) as a function of streamwise distance (in step heights) for all four rounded forward steps. The undisturbed boundary layer pressure is shown using a horizontal dashed line and was calculated using the data from Awasthi *et al.* (2014). The figure also shows the location of the step ($x/h = 0$) via a vertical solid line. Looking at the figure we see that far upstream of the step (about 6 step heights) the fluctuating pressure is at about the same level as the undisturbed boundary layer. As the flow approaches the step the pressure begins to rise due to flow separation and reaches its maximum at the measurement location closest to step. As seen in the pressure spectra of Figure 3-62, the fluctuating pressure upstream of the step is independent of the step corner rounding. As we move downstream of the step however, the story is quite different with the fluctuating pressure being much higher than the upstream values and also varying with the corner rounding. The fluctuating pressure for an unrounded step displays a broad peak with near constant values between 2-3 step heights after which it starts to recover. As the step is rounded the peak values of p_{rms} increase but the peaks become narrow (due to faster recovery) and shift upstream with increasing rounding. The peak values of p_{rms} for $r/h = 0\%$, 6.25% and 12.5% occur at $x/h = 2.1$, 1.3 and 0.94 respectively. These streamwise distances correspond to approximately 70%, 62% and 59% of the reattachment lengths for $r/h = 0\%$, 6.25% and 12.5% respectively. This suggests that the fluctuating pressure peaks in the latter half of the separation bubble for each step and then begins to fall as the flow reattaches. The fluctuating pressure for $r/h = 25\%$ step has the fastest recovery of all the steps considered here and shows pressure levels which are much lower than any rounded steps considered here. This is because the separation bubble for $r/h = 25\%$ is much smaller (as evident in the flow visualization results) and measurements are not available within the separation bubble for this step. The variation of p_{rms} along with the mean pressure and flow visualization results show a drastic reduction in the strength and extent of the separation bubble as the step rounding is increased from $r/h = 12.5\%$ to 25%. This change in flow structure bears a strong correlation with the far field from rounded steps measured in the wall-jet facility in that the sound spectra also showed a sudden drop throughout the frequency range as the step rounding was increased from $r/h = 12.5\%$ to 25%. Since the pressure fluctuations on solid surfaces in turbulent flows are representative of the dipole sound sources responsible for the emission of far field sound, the change in behavior of the flow immediately downstream of the step might be directly related to the far field sound behavior seen for rounded forward steps.

The trend in variation of fluctuating pressure downstream of the step with rounding of the step corner is consistent with Hao and Wang's (2014) LES simulations which also show the peak value of p_{rms} rising and shifting upstream with increasing rounding. Figure 3-68 (a)-(c) shows a comparison of the present measurements with LES simulations of Hao and Wang (2014) for $r/h = 0\%$, 12.5% and 25% respectively. The levels of p_{rms} in both cases have been referenced to the free stream dynamic pressure and have been plotted as a function of streamwise distance in step heights. Note that the Reynolds number based on step height in present work is more than 3 times the Reynolds number in computations of Hao and Wang (2014) but their step height to boundary layer ratio is approximately the same as measurements ($h/\delta = 26\%$). As seen in the figure, both measurements and the computations show similar qualitative behavior as far as the decay of fluctuating pressure with downstream distance is concerned. There are however some quantitative differences. For $r/h = 0\%$ (Figure 3-68 (a)) the measurements show a more gradual decay or p_{rms} compared to computations. This might be because the Reynolds number in measurements is much higher and as shown by Awasthi *et al.* (2014) the behavior of fluctuating pressure downstream of an unrounded forward step is strongly dependent on the step height Reynolds number. For $r/h = 12.5\%$ the quantitative agreement is much better closer to the step ($x/h < 1.5$) but further downstream the measured pressure again shows a slower decay. It must be noted that for this step the reattachment lengths for measurement and computations are similar as well (see Table 3-3) and so within the separation bubble the measurements and computations show good agreement. For the highest rounding of $r/h = 25\%$ (Figure 3-68 (c)) the measured spectra show much lower levels and a faster decay. The measured reattachment length for this step is $x/x_r = 0.4$ while the computed length is $x/x_r = 1.05$ and so the faster decay of measurements is consistent with a smaller separation bubble size.

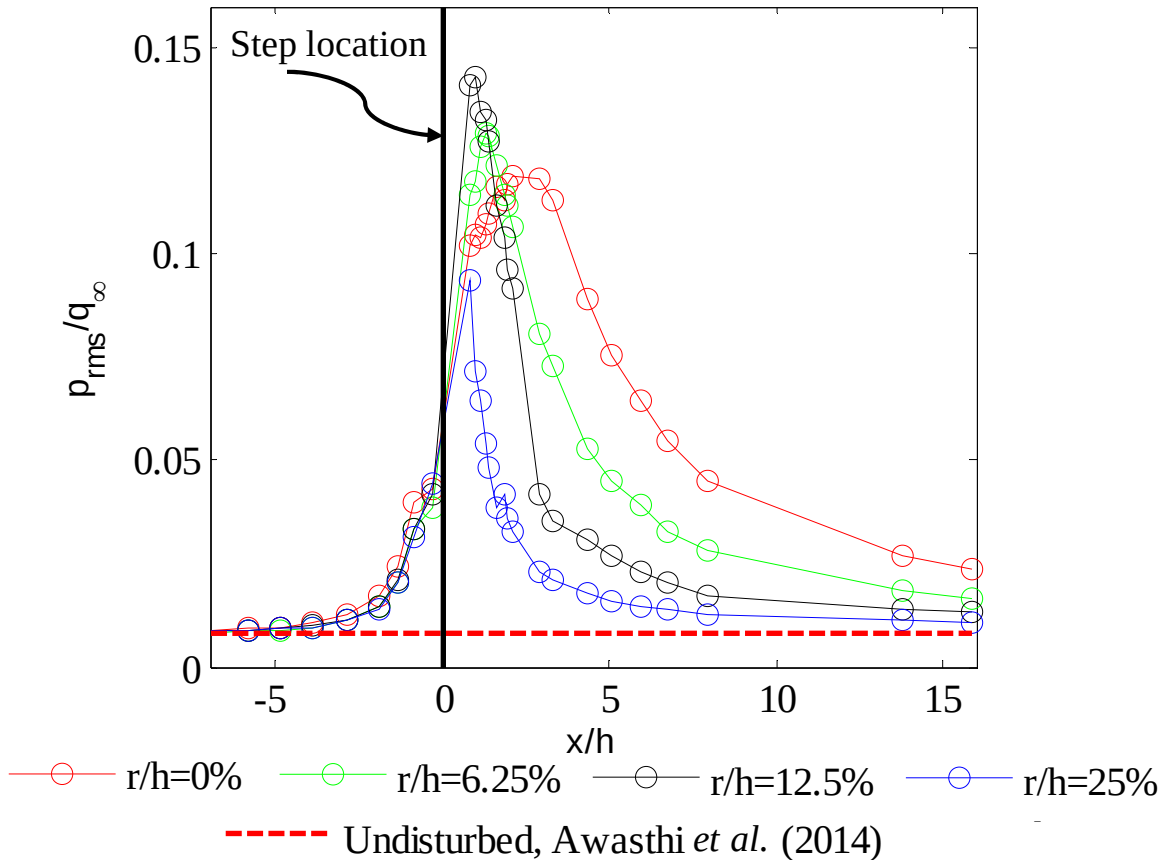
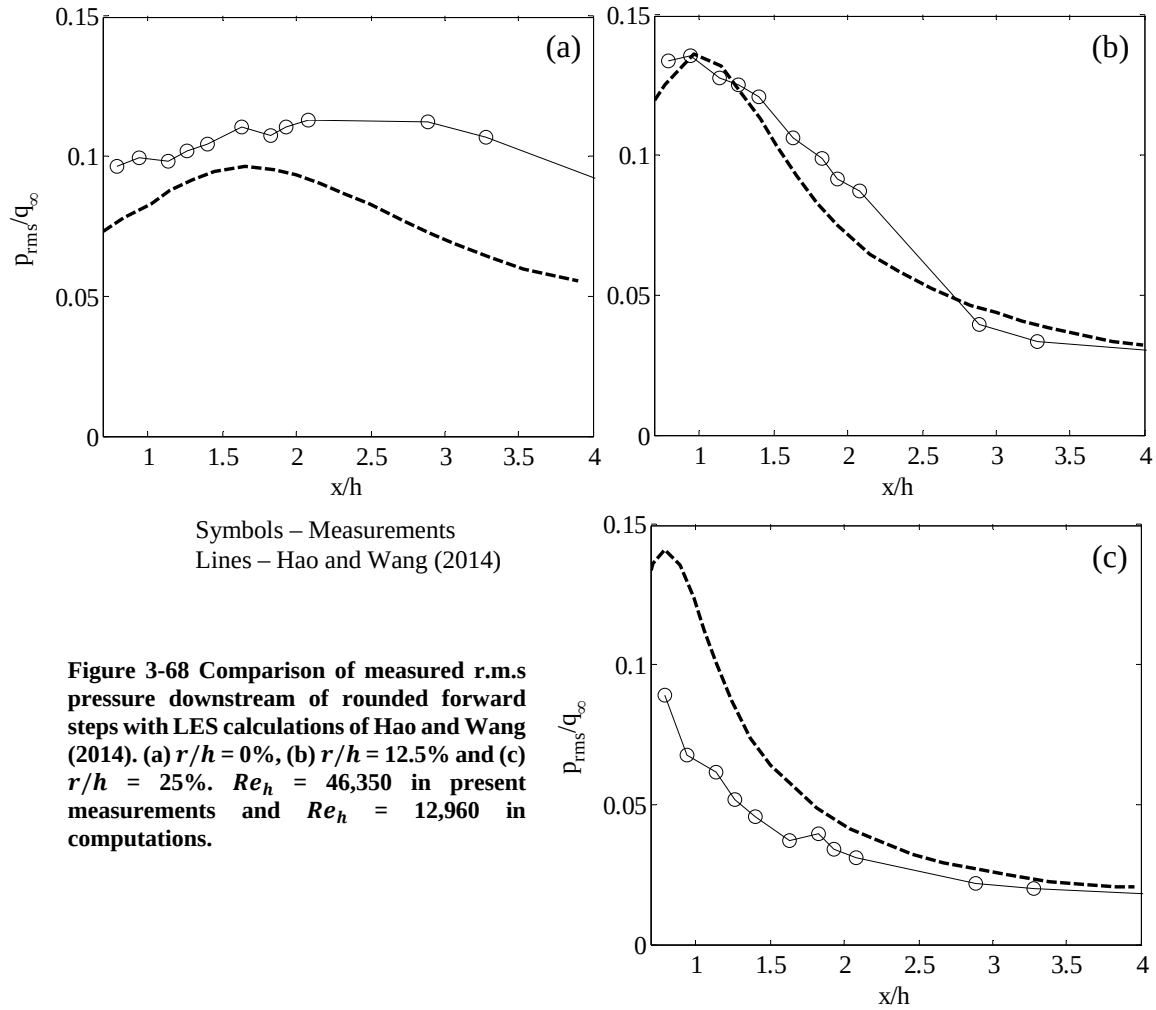


Figure 3-67 Root mean square surface pressure fluctuations as a function of streamwise distance for rounded forward steps at $U_\infty = 30$ m/s. Undisturbed boundary layer p_{rms} taken from the data of Awasthi *et al.* (2014). Lines for visual aid only.



Let us now consider the p_{rms} on the vertical face of the step for rounded forward steps as shown in Figure 3-69. This figure shows the fluctuating pressure as a function of step corner rounding for the two measurement locations used in the present work. Looking at the figure it can be seen that for both locations the r.m.s pressure is higher for the two highest rounding while it is about the same for the two lowest ones. The pressure for the location which is more exposed to the oncoming shear layer ($y = 0.69h$) is consistently higher than the location which is expected to be within the separation bubble ($y = 0.56h$). As explained in the discussion of the pressure spectra on the face of the step this increase in fluctuating pressure is possibly related to the flow acceleration around the step corner.

Figure 3-70 shows the p_{rms} on the downstream surface of the step scaled on p_{rms} at $y = 0.69h$ plotted against the streamwise distance normalized on reattachment length for each rounded forward step. The plot in the inset is the same as the main plot but with a reduced abscissa so that it only shows values for $\leq x/x_r \leq 2$. This figure essentially is an attempt to explore the similarity between the unsteady pressures within the downstream separation bubble to that on the step face. Looking at the figure it can be seen scaling the p_{rms} in such manner provides a reasonable collapse of data for $x/x_r < 2$ (see plot in the inset) for $r/h \leq 12.5\%$. The pressure on the downstream side close to the rounded corner ($x/x_r < 1$) for these steps is between 80-100% of the pressure on the face of the step. Further downstream, the pressure consistently drops compared to that on the step face and at two reattachment lengths downstream it is about 60% lower. This result suggests that the unsteady pressure within the downstream separation bubble close

to the step corner for $r/h \leq 12.5\%$ is comparable to the pressure on the step face at a location which is outside of the mean separation bubble ahead of the step.

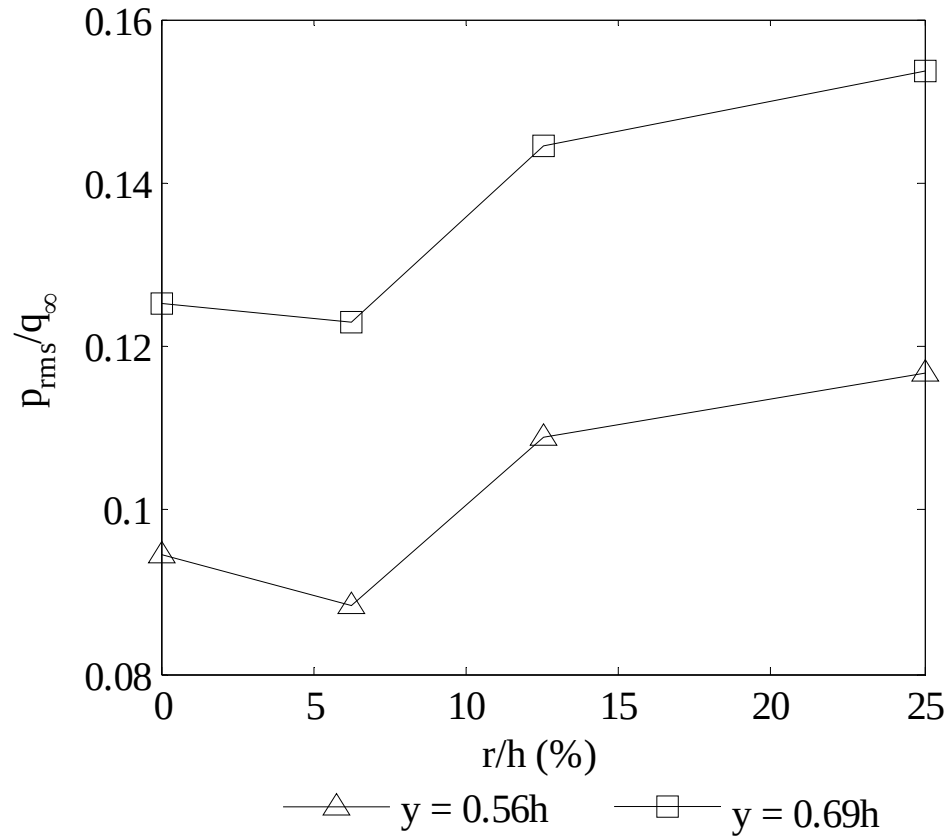


Figure 3-69 Root mean square pressure on the face of rounded forward steps at $U_{\infty} = 30$ m/s. Lines for visual aid only.

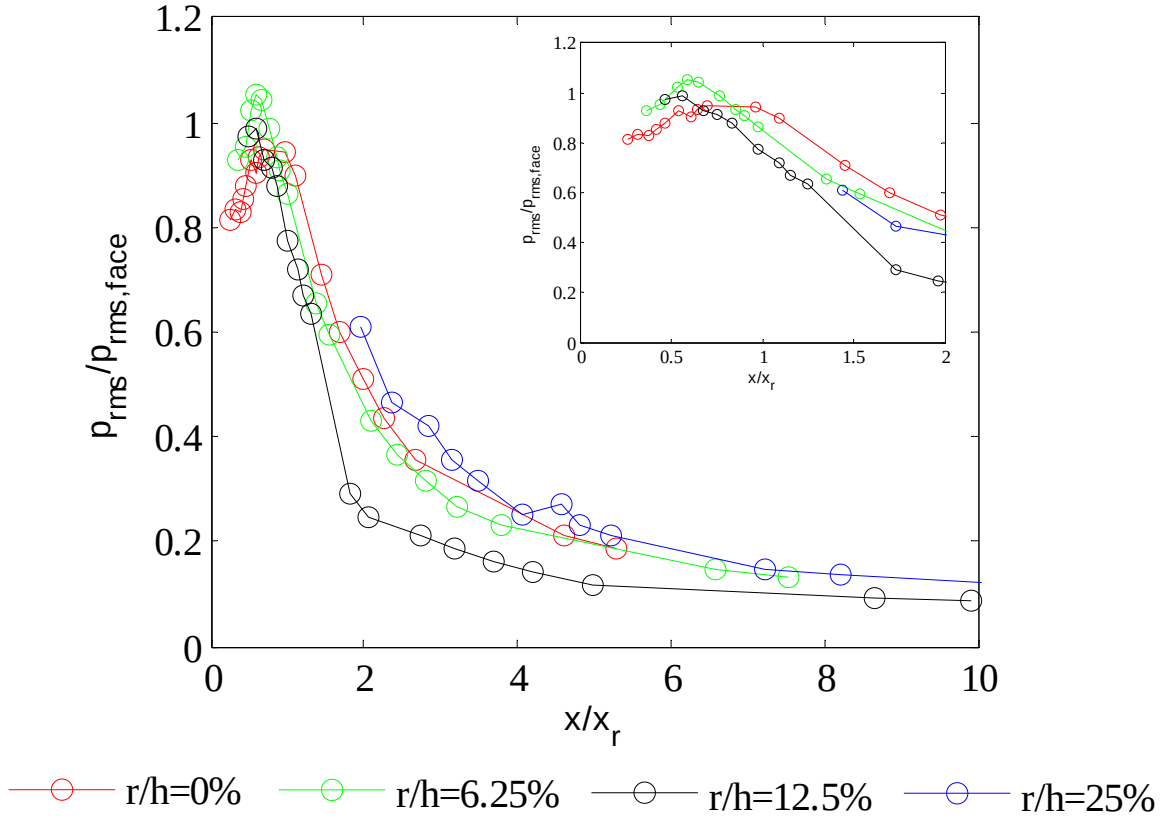


Figure 3-70 Root mean square pressure downstream of rounded forward steps scaled on root mean square pressure on step face at $y = 0.69h$ as a function of streamwise distance normalized on reattachment length for each step, $U_\infty = 30$ m/s. The plot in the inset is the same but with reduced abscissa.

3.2.3 Pressure Fluctuation Statistics from Unsteady Force Sensor

Though the purpose of using the 24 microphone linear surface pressure array describe in §3.1.5 is to extract the area averaged unsteady pressure in the vicinity of forward steps, the surface pressure fluctuation data from it can be used to provide some useful insight into the flow structure. The purpose of this section therefore is to consider some statistical results from the measurements made using the linear surface pressure array. It must be understood that these are not point pressure statistics but are rather pressure fluctuations averaged over an area equal to the diaphragm area of the microphones used in the array. The effects of this spatial filtering of the pressure field on the frequency response of individual microphones used in the array was previously discussed in §3.1.5.1. As shown there, the spatial filtering effects of large diameter microphones results in the frequency response being biased towards the larger scales of the flow. In low Mach number separated flows we expect that it is these large scales in the near field that govern the far field behavior. Therefore, analyzing these area averaged two point pressure statistics can reveal useful information about the sound source mechanism involved.

In the following sections we will consider some two point statistics of pressure fluctuations measured by the linear microphone array. We will denote the pressure measured by this array as $\tilde{p}(t)$, where the superscript $\sim$ refers to the area averaged pressure. Similarly, we will denote the spectra density of the surface pressure fluctuations measured by the array microphones as $\phi_{\tilde{p}\tilde{p}}(f)$.

3.2.3.1 Time Mean Surface Pressure Correlation

To probe into the flow structure modification by rounded forward steps we will now consider the two point correlations obtained from the measurements made using linear surface pressure array. To this end let us consider the time mean spanwise correlation coefficient which for two microphones separated by distance Δs is given by

$$\rho(\Delta s) = \frac{E[p^\sim(s, t)p^\sim(s + \Delta s, t)]}{\sqrt{E[p^\sim(s, t)^2]E[p^\sim(s + \Delta s, t)^2]}} \quad (3-46)$$

where ρ is correlation coefficient, $E[\]$ is the expected value operator, and s denotes the streamwise or spanwise position. Note that $s = x$ and $s = z$ for streamwise and spanwise separations respectively. Figure 3-71 (a)-(c) shows correlation coefficient as a function of spanwise separation Δz (in step heights) measured at three streamwise locations of $x/h = 3.13$, $x/h = 1.3$ and $x/h = 0$ respectively at $U_\infty = 30$ m/s. The correlation shown is calculated between the microphone located at $z = -166$ mm (see §3.1.1 for the definition of this coordinate system) and the rest of the microphones in the array. We have excluded the single 1/8th inch microphone from the calculations in order to be consistent with the definition of $p^\sim$.

At $x/h = 3.1$ (Figure 3-71 (a)) it can be seen that rounding the step corner leads to reduction in spanwise correlation length at this location. This reduction can be quantified in terms of the spanwise correlation lengthscale $\mathcal{L}_z$, defined as the distance over which the correlation coefficient falls to 5%. This cutoff value of 5% was chosen because unlike in an ideal case the correlation values over large spanwise distance in measurements does not decay to zero. Based on an observation of the data it was found that in certain cases the value of correlation coefficient at large distances could be as high as 0.04. Therefore, to avoid misinterpretation of these small values the cutoff of 5% was defined. We find that $\mathcal{L}_z$ decreases monotonically with corner rounding from 2.95 to 2.36, 2.77 and 1.18 step heights for $r/h = 0\%$, 6.25%, 12.5% and 25% respectively. Because of the variations in reattachment position x_r with rounding these results correspond to different normalized locations within the separation bubble of $x/x_r = 1, 1.5, 1.9$ and 7.8, respectively. One could therefore argue that the decrease in the spanwise correlation length scale is partly the result of examining the flow further downstream of reattachment where shear layer breakdown likely causes the production of small scale turbulence.

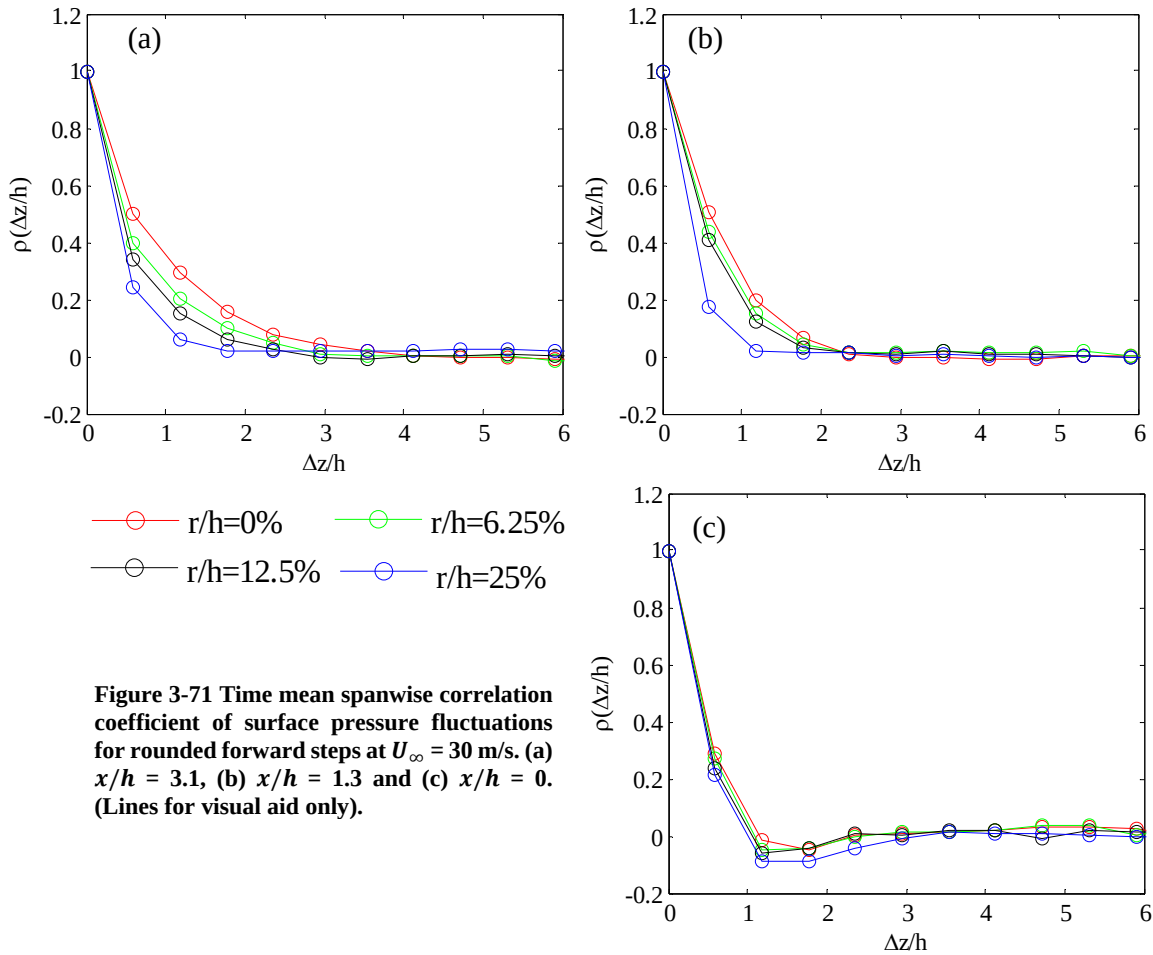
Further upstream the story is quite different however. Figure 3-71 (b) shows the time mean spanwise correlation functions measured at $x/h = 1.3$ corresponding to $x/x_r = 0.4, 0.6, 0.8$ and 3.3 for $r/h = 0\%$, 6.25%, 12.5% and 25% respectively. This location is within the separation bubble for all steps except $r/h = 25\%$. Here the correlation coefficient function is approximately the same for the 3 lowest rounding radii with $\mathcal{L}_z =$ perhaps suggesting the presence of similar large scale turbulence within the separation bubble. On the other hand for $r/h = 25\%$ the spanwise length scale is smaller consistent with breakdown at reattachment.

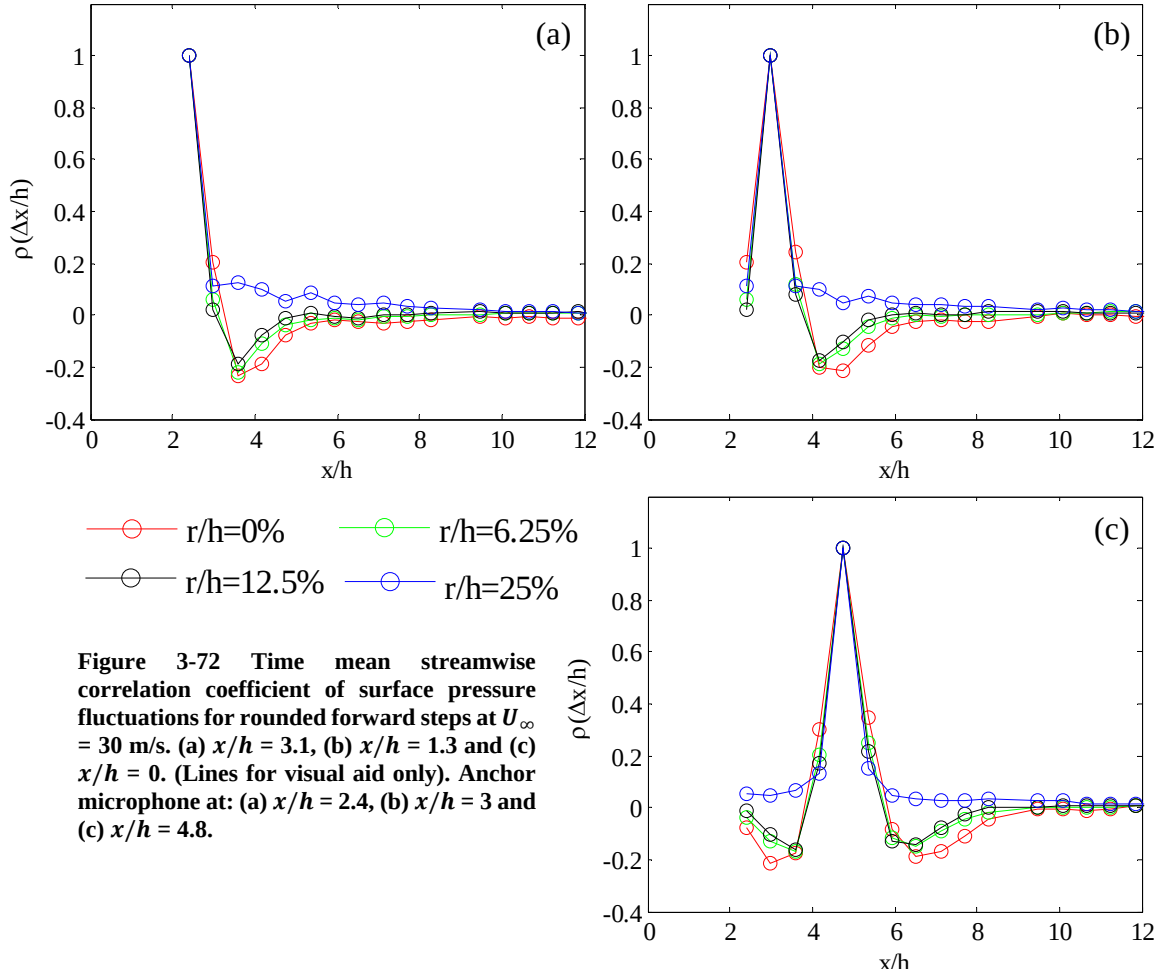
Figure 3-71 (c) shows the spanwise correlation coefficient at the step face for each rounding. Here there is no variation in correlations due to rounding and the correlation curve takes a different form than that seen downstream. For the two adjacent microphones ($\Delta z/h = 0.6$) there is a significant positive correlation for each rounding. However, as the spanwise separation increases each rounding shows a slightly negative correlation which suggest that a complex flow structure develops along the step face, perhaps due to three-dimensionality of the upstream separation bubble as observed by Pearson *et al.* (2013) in their PIV measurements of flow over forward steps.

Figure 3-72 shows the time mean correlation coefficient calculated from the measurements made with the array in its streamwise orientation downstream of the step. The correlation coefficient was calculated by selecting an anchor microphone and then computing its correlation with the rest of the microphones in the array (except the smaller B&K 4138 microphone). The correlations shown in Figure 3-72 (a), (b) and (c) are for the anchor microphone located at $x/h = 2.4, 3$ and 4.8 respectively, hence the

shifting correlation peak. Looking at Figure 3-72 (a) which shows correlation coefficient calculated with the most upstream microphone in the array (at $x/h = 2.4$) we see that for rounding up to $r/h = 12.5\%$ there is a positive correlation between the two most upstream microphones beyond which we see a negative correlation that lasts up to 8 step heights downstream of the step. This negative correlation is likely due to the large structures which originate within the separation bubble being convected downstream. The streamwise extent of the correlation function decreases slightly with increasing rounding possibly because of the reduced intensity and extent of the separation bubble. The streamwise correlation for $r/h = 25\%$ shows a completely different form than the rest of the rounded steps with the correlation showing only positive values and decaying faster.

As the position of the anchor microphone moves downstream (Figure 3-72 (b) and Figure 3-72 (c)) we further see that for $r/h \leq 12.5\%$ there is only quantitative change in the flow structure with the streamwise extent of the correlation function dropping as rounding increases. For the last anchor microphone position considered $x/h = 4.8$ (Figure 3-72 (c)) the highest rounding shows barely any correlation with the downstream microphones which demonstrates the limited streamwise influence of the separation bubble associated with this rounding.





3.2.3.2 Coherence of Surface Pressure Fluctuations

Coherence (squared) between signals from two microphones located at points s and $s + \Delta s$ (where Δs represents the separation between the two microphones) is given by

$$\gamma^2(\Delta s, f) = \frac{|\phi_{pp}^{\sim}(\Delta s, f)|^2}{\phi_{pp}^{\sim}(s, f)\phi_{pp}^{\sim}(s + \Delta s, f)} \quad (3-47)$$

where $\phi_{pp}^{\sim}(\Delta s, f)$ is the cross-spectral density of area averaged unsteady pressure between the microphones separated by distance Δs for which the coherence is required and, $\phi_{pp}^{\sim}(s, f)$ and $\phi_{pp}^{\sim}(s + \Delta s, f)$ are the area averaged autospectral densities of measured by microphones located at points s and $s + \Delta s$ respectively. Once again note that $s = x$ and $s = z$ for streamwise and spanwise separations respectively.

Since we are interested in coherence as a function of both spatial separation and frequency it is useful to present it on contour maps. To illustrate this representation of coherence consider Figure 3-73 (a) and (b) which show the spanwise coherence for undisturbed boundary layer at $U_\infty = 30$ m/s and 40 m/s respectively. In this figure the abscissa represents the frequency on a log scale, the ordinate represents the spanwise separation between microphones in meters and the contours show the value of $\gamma^2(\Delta z/h, f)$. To generate these plots the coherence between each microphone (except B&K 4138 microphone) was calculated as a function of frequency using equation (3-47) and the results were ordered according to the

separation between the centers of each microphones. Of these results, the coherence is plotted only for unique separations. This procedure can be adopted here for representation of coherence since the flow is homogeneous along the spanwise dimension. Also note that the spanwise scale of these plots does not represent the maximum spanwise separation between the microphones (which is $\Delta z = 0.34$ m) in the array but has been limited to ± 0.1 m since there is no discernible coherence observed beyond this range in any of the flow cases.

The contour levels in Figure 3-73 which represent coherence squared values are shown in increments of 0.01 and the color corresponding to each level has been indicated at the bottom of the figure. The spanwise separations for which coherence is actually measured have been indicated by short dotted lines to the right of the plot. The color contours between these separations have been obtained through interpolation. Also note that for the case shown in Figure 3-73 (a) ($U_\infty = 30$ m/s) coherence below 50 Hz is not shown since it is dominated by spurious background noise.

First let us consider the coherence at $U_\infty = 30$ m/s in Figure 3-73 (a). We see here that at frequencies lower than about 400 Hz the pressure fluctuations are coherent over a larger spanwise scale due to presence of slow moving, large structures in the boundary layer. For frequencies higher than 400 Hz and less than about 3,000 Hz the spanwise coherent length scale ($\mathcal{L}_{Cz}$) is limited to about $\Delta z = \pm 0.02$ m. Note that we have used the subscript C in the designation of coherent length scale to distinguish it from the length scale $\mathcal{L}_z$ defined for correlation coefficient earlier. At frequencies greater than 3,000 Hz $\mathcal{L}_{Cz}$ increases again but this increase is believed to be due to electrical noise since at these frequencies the microphone's response to boundary layer pressure is severely attenuated. If we recall our discussion in §3.1.5.1 on the spatial filtering by these microphones both the theoretical analysis and the measurements showed about a 20 dB attenuation at these same frequencies. As the velocity is increased to 40 m/s (Figure 3-73 (b)), the coherence map maintains the same appearance but the spatial extent of coherence decreases slightly and the high frequency noise shifts to higher frequencies. Due to the presence of this extraneous noise in coherence maps, further data will only be shown up to a frequency of 3,000 Hz.

Figure 3-74 shows the spanwise coherence shown of Figure 3-73 but with frequency normalized on boundary layer thickness and free stream velocity and spanwise separation normalized on boundary layer thickness. The undisturbed boundary layer thickness for $U_\infty = 40$ m/s was obtained using linear interpolation of Awasthi *et al.*'s (2014) data (see Table 3-2). In both of these figures the maximum spanwise coherent length scale of pressure fluctuations is limited to about a boundary layer thickness below a frequency given approximately by $f = 1.5U_\infty/\delta$. Based on a convection velocity estimate of $U_c = 0.6U_\infty$ this frequency is approximately 0.9. These results suggest that the pressure fluctuations in undisturbed boundary layer are being influenced by structures which are of the order of the boundary layer thickness in spanwise direction and are being convected with velocities lower than the free stream velocity.

Figure 3-75 (a) and (b) shows the streamwise coherence of pressure fluctuations in undisturbed boundary layer flow at $U_\infty = 30$ m/s and 40 m/s respectively. The streamwise separation in these figures has also been ordered like the spanwise case and only unique separations are considered. This can be done for undisturbed boundary layer flows because the flow is homogeneous in streamwise direction as well. The most striking feature of these plots is the large streamwise coherent length scale compared to spanwise coherent length scale seen in Figure 3-74. The streamwise coherent length scale $\mathcal{L}_{Cx}$ extends throughout the entire measurable length of the array which is about 3.5 times the boundary layer thickness at frequencies below about $f\delta/U_\infty = 0.5$. The reason for this extremely large coherent length scale is that the convective length scales in boundary layer flows tend to be much larger than the spanwise length scales. Similar behavior of streamwise coherence in undisturbed boundary layer flows has been observed in the past (see Pröbsting *et al.* (2012) and Forest (2012) for example). As the frequency increases $\mathcal{L}_{Cx}$ decreases in an exponential manner due to the smaller scales being convected at faster velocities than the larger scales.

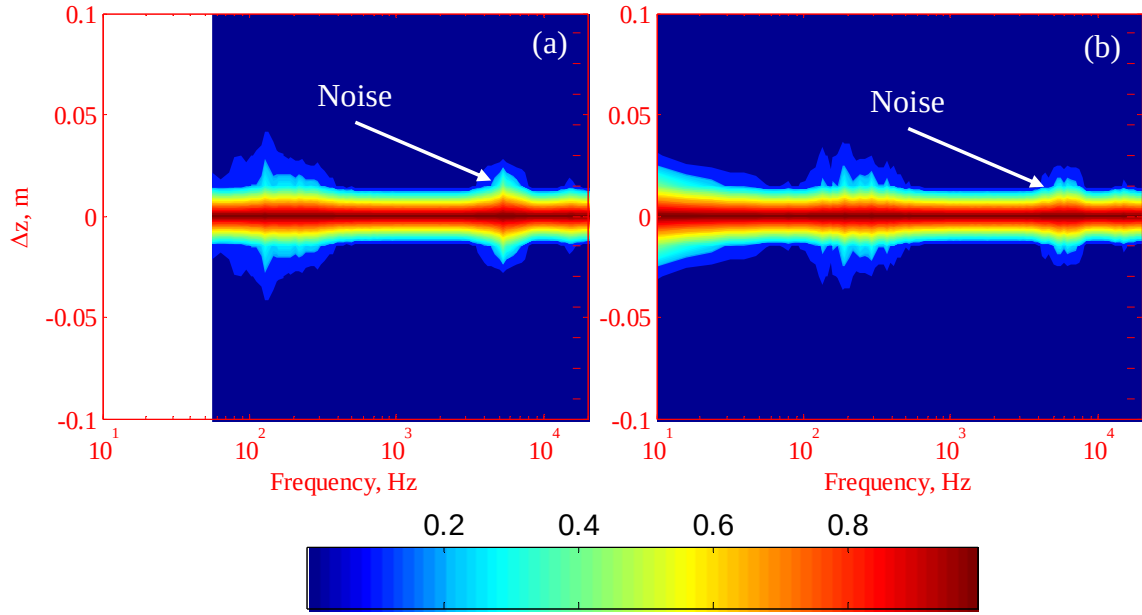


Figure 3-73 Contours of spanwise coherence of surface pressure fluctuations for undisturbed boundary layer flow at (a) $U_{\infty} = 30$ m/s and (b) $U_{\infty} = 40$ m/s. The spanwise separations have been ordered and only unique separations between microphones have been used. The separations for which coherence is plotted are indicated by the short dashed lines to the right on each plot. The contour levels are plotted in increments of $\gamma^2(\Delta z, f) = 0.01$ and levels between microphone separations have been obtained via interpolation.

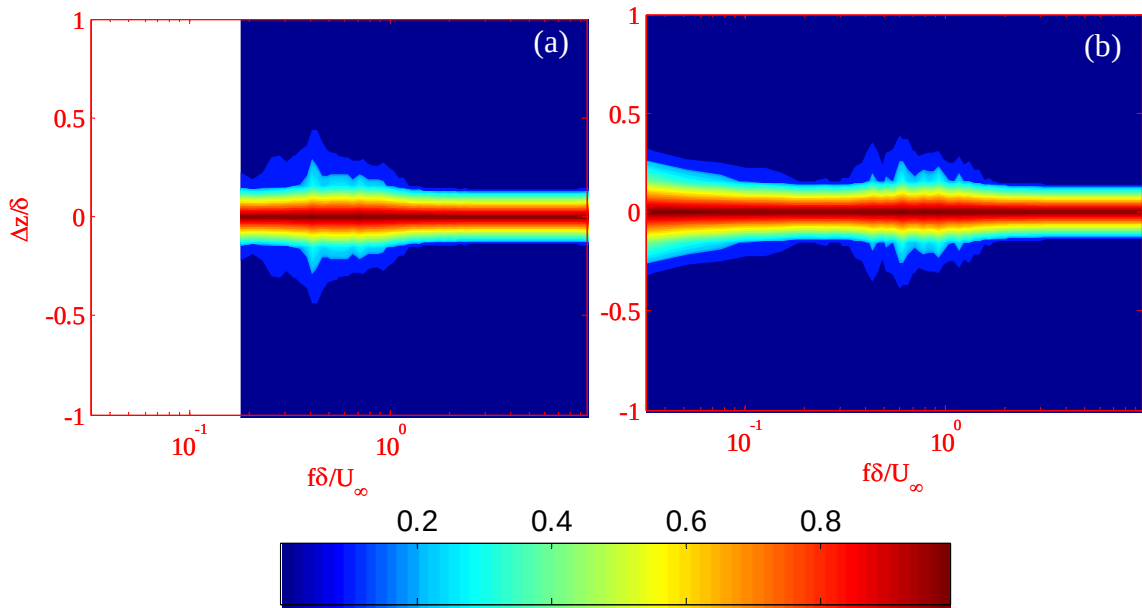


Figure 3-74 Contours of spanwise coherence of pressure fluctuations for undisturbed boundary layer flow with frequency normalized on boundary layer thickness and edge velocity and spanwise separation normalized on boundary layer thickness. (a) $U_{\infty} = 30$ m/s and (b) $U_{\infty} = 40$ m/s.

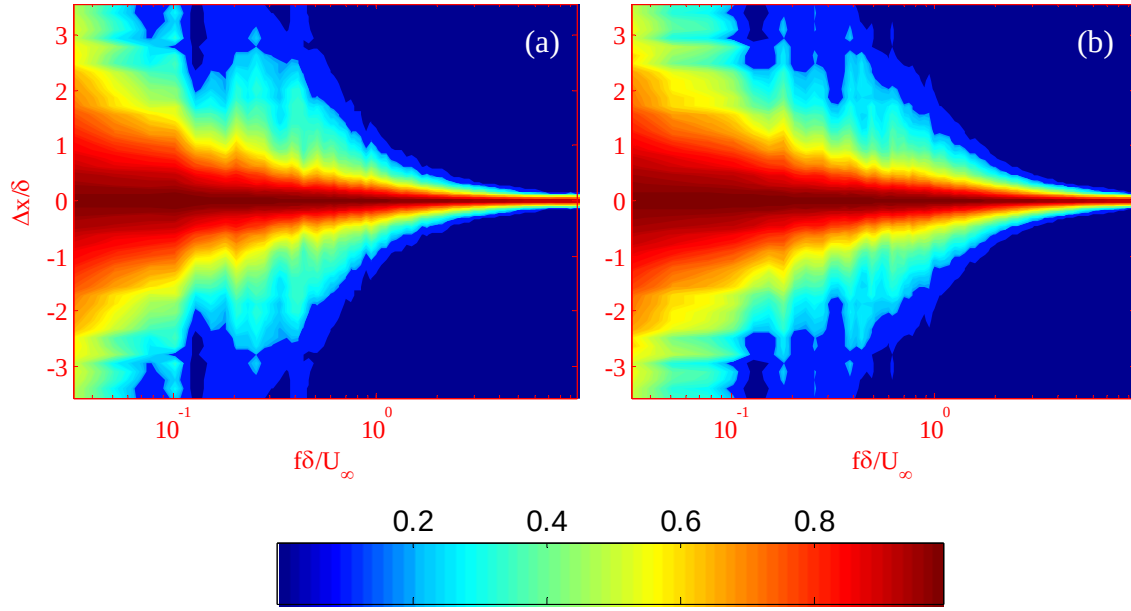


Figure 3-75 Contours of streamwise coherence of surface pressure fluctuations for undisturbed boundary layer flow at (a) $U_\infty = 30$ m/s and (b) $U_\infty = 40$ m/s.

Let us now consider how the presence of rounded forward steps in the flow modifies the coherent structures influencing the surface pressure fluctuations. We will first consider the spanwise coherence, the representation of which will follow the same convention as that for undisturbed boundary layer in Figure 3-73 except that the spanwise separation will be normalized on the step height since in these flows it is a relevant parameter. Note that ordering the spanwise separations as done for undisturbed boundary layer flow is also valid here since the flow is spanwise homogeneous. The results for coherence in step flows will only be presented for $U_\infty = 30$ m/s since the results for other velocities are qualitatively the same. Figure 3-76 shows the spanwise coherence for all four rounded steps measured 3.1 step heights downstream of the step. The spanwise scale in each plot is restricted to $\Delta z = \pm 3h$ since there is no significant coherence ($\gamma^2 > 0.05$) observed beyond this range for any of the cases being considered here. Also shown in this plot is the spanwise coherence for undisturbed boundary layer flow (Figure 3-76 (e)) with the spatial separation normalized on step height for the sake of comparison.

Consider now the spanwise coherence for $r/h = 0\%$ shown in Figure 3-76 (a). Looking at this figure and comparing it with the smooth wall coherence in Figure 3-76 (e) we see a large increase in the spanwise coherent length scale which can be more than 2 step heights on either side of the $\Delta z = 0$ axis. This large increase in spanwise coherence is due to the large slower moving structures created by the flow separation from the step corner. Note that this measurement location is within the separation bubble ($x_r/h = 3$) for this step. As the step corner is rounded to $r/h = 6.25\%$ (Figure 3-76 (b)) $\mathcal{L}_{Cz}$ is reduced to about 2 step heights between approximately $0.2 < f\delta/U_\infty < 1.2$. Note that for this step the measurement location is downstream of shear layer reattachment and so the reduction in spanwise coherence is likely due to the shear layer breakdowns which occurs at reattachment leading turbulence with a smaller spanwise scale. As the step corner is rounded further to $r/h = 12.5\%$ (Figure 3-76 (c)) we see a further reduction of $\mathcal{L}_{Cz}$ due to shrinking of the separation bubble. For the highest rounding of $r/h = 25\%$ the spanwise coherent length decreases further and is mostly limited to $\Delta z/h = \pm 0.5$ throughout the frequency range. These results are consistent with the point pressure spectra measured at approximately the same location (Figure 3-64 (d)) which showed a reduction in lower frequency content of the spectrum as the step is rounded. The results are also consistent with the time mean spanwise correlations shown in the previous section which showed a consistent reduction in the spanwise correlation at this location with increasing rounding.

The spanwise coherence measured at $x/h = 1.3$ for $U_\infty = 30$ m/s for rounded steps is shown in Figure 3-77. Here we see that the unrounded step (Figure 3-77 (a)) shows a smaller $\mathcal{L}_{Cz}$ compared to that seen at $x/h = 3.1$ (Figure 3-76 (a)). Cherry *et al.* (1984) who studied the separation bubble created downstream of the leading edge of a blunt flat plate found that the spanwise correlation length scale of the velocity in the shear layer grows almost linearly up to the reattachment point. Since the pressure fluctuations are related to the velocity fluctuations the reduction of the length scale $\mathcal{L}_{Cz}$ seen here seems consistent with their results. As the step corner is rounded to $r/h = 6.25\%$ (Figure 3-77 (b)) we see that unlike at $x/h = 3.1$ the coherence between this step and the unrounded step looks quite similar. This is because now we are within the separation bubble for both the steps and presumably a slight rounding of the corner does not affect the structure of the separation bubble. As we round the step corner further to $r/h = 12.5\%$ (Figure 3-77 (c)) the coherence at the lowest frequencies ($f\delta/U_\infty < 0.4$) still looks similar to the lower rounding cases but at higher frequencies we see a slight reduction in the coherent length scale. For the highest rounding there is no change from the coherence seen further downstream due to the limited streamwise extent of the separation bubble.

Figure 3-78 shows the spanwise coherence measured on the face of each of the rounded forward steps. Unlike the behavior seen downstream of the step, the coherence on the step face is independent of corner rounding and the larger spanwise coherence is limited to very low frequencies. Also note that for $r/h = 0\%$ and 6.25% (Figure 3-78 (a) and (b)) the electrical noise seen in undisturbed boundary layer flow coherence (Figure 3-73) spills to lower frequencies. The likely reason for this is that the pressure fluctuations here are dominated by much larger scales than on the downstream surface which results in the spectral attenuation due to spatial filtering by the microphone occurring at lower frequencies. The independence of coherence maps on corner rounding seen here is consistent with time mean spanwise correlations (Figure 3-71 (c)) that also showed a similar behavior. Recall however that the single point pressure spectra on the step face shown in §3.2.2 did show a variation with corner rounding. This discrepancy is perhaps because the array measurements are made further away from the rounded corner than the probe microphone and so these measurements are shielded from the corner rounding effects.

Figure 3-79 shows the streamwise coherence measured downstream of each of the rounded forward steps at $U_\infty = 30$ m/s. The presentation of the coherence follows the same format as for spanwise coherence except for one major difference. The microphone separation shown here are not ordered. Instead, the coherence has been calculated between the most upstream microphone in the array (located at 2.4 step heights downstream in each case) and the rest of the microphones in the array (except the smaller B&K 4138 microphone). This was done because the streamwise flow downstream of the step can no longer be regarded as homogeneous. Note that the streamwise coherence for the undisturbed boundary layer at $U_\infty = 30$ m/s has been replotted to reflect this change in spatial separation (Figure 3-79 (e)). Looking at the figure we first note that for $r/h \leq 12.5\%$ the streamwise coherent length ($\mathcal{L}_{Cx}$) at low frequencies is much shorter and is mostly limited to about 8 step heights downstream of the step. This reduction in $\mathcal{L}_{Cx}$ is due to shear layer breakdown at reattachment. For the highest rounding of $r/h = 25\%$ (Figure 3-79 (d)) however, we see a large extent of the streamwise coherent length at low frequencies which resembles the undisturbed boundary layer flow due to smaller separation bubble and quicker flow recovery for this rounding. We also note that for $r/h \leq 12.5\%$ $\mathcal{L}_{Cx}$ decreases slightly with increasing rounding because of the reduced streamwise extent of the separation bubble.

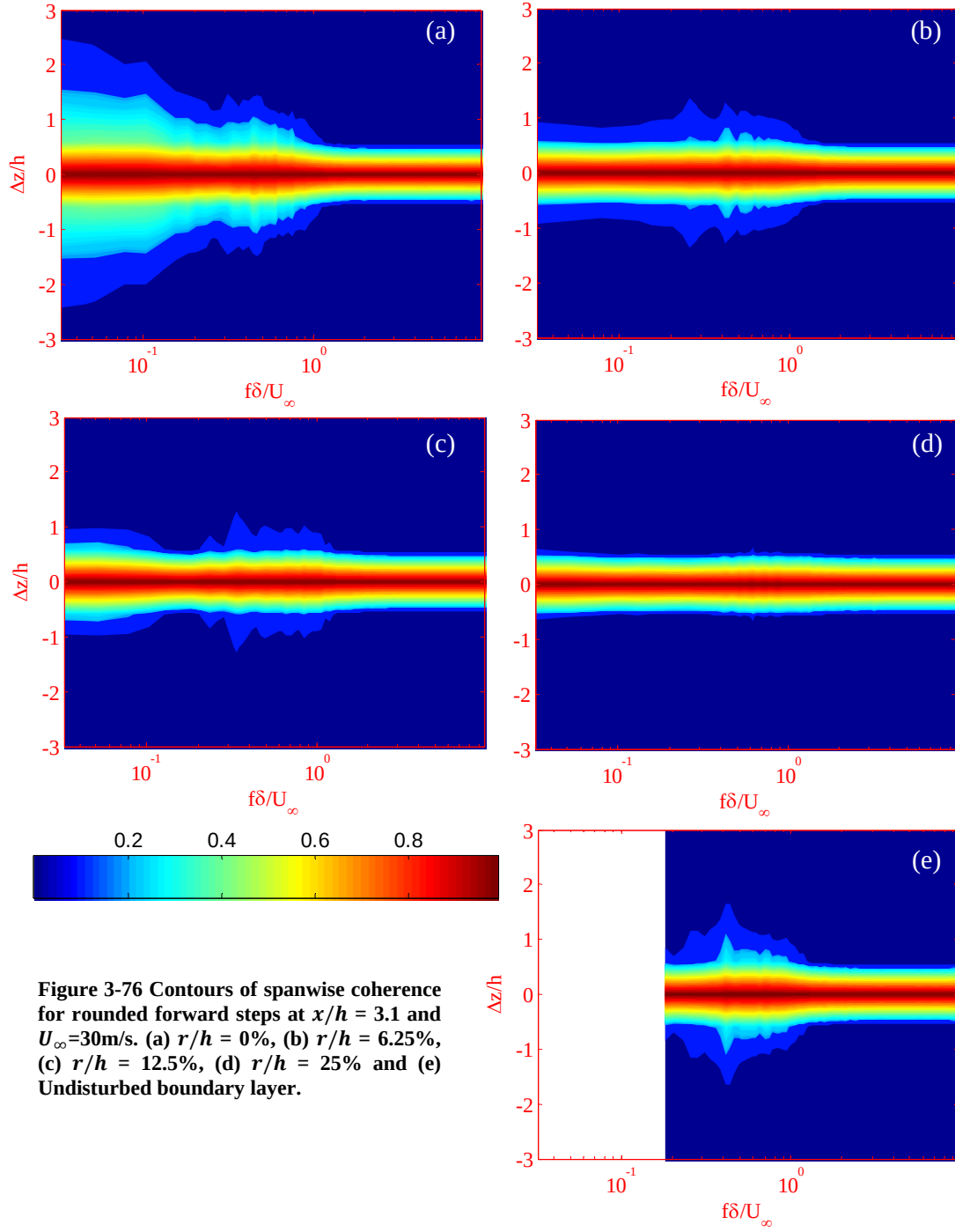


Figure 3-76 Contours of spanwise coherence for rounded forward steps at $x/h = 3.1$ and $U_\infty = 30 \text{ m/s}$. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$, (d) $r/h = 25\%$ and (e) Undisturbed boundary layer.

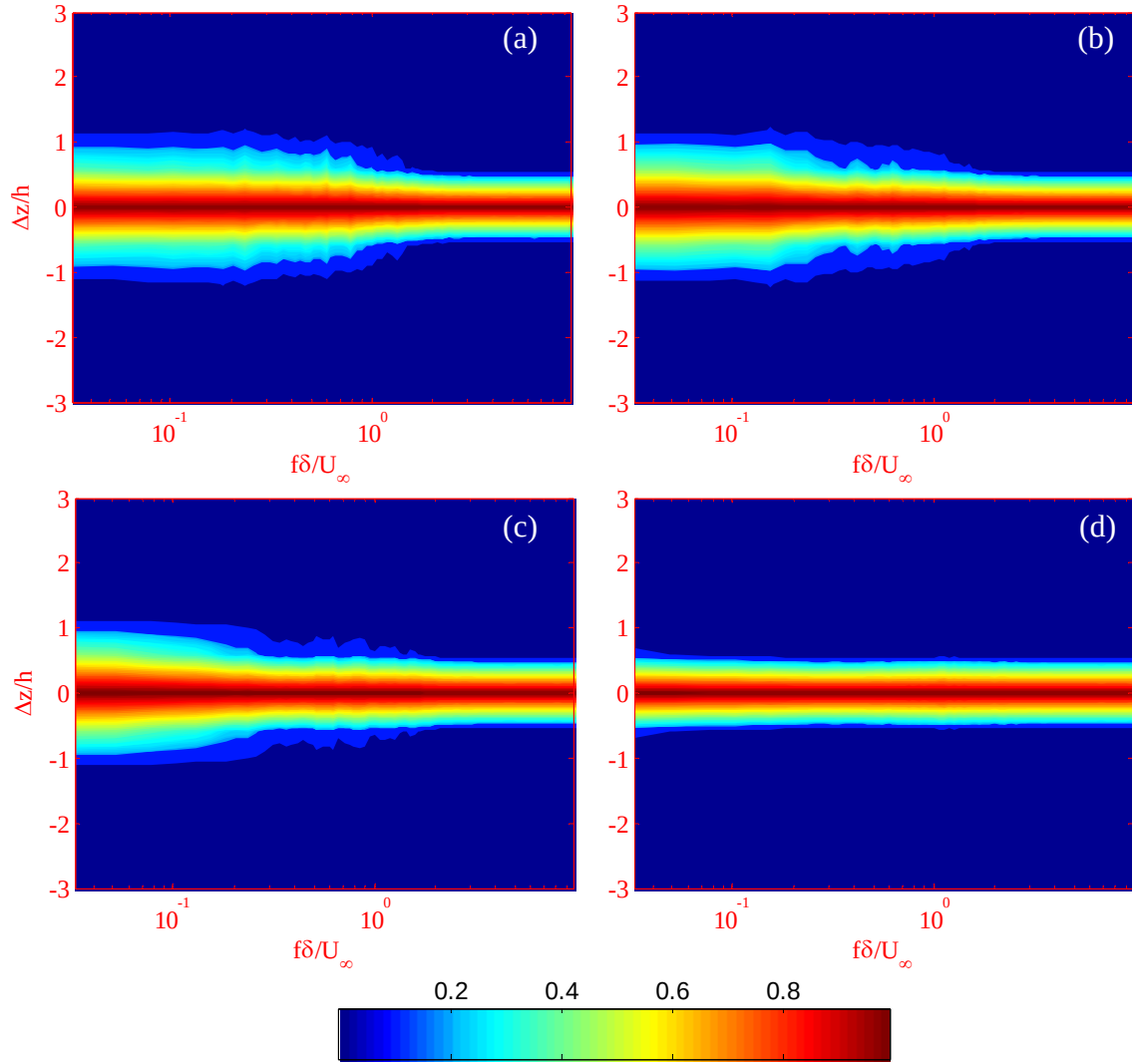


Figure 3-77 Contours of spanwise coherence for rounded forward steps at $x/h = 1.3$ and $U_\infty = 30$ m/s. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$, (d) $r/h = 25\%$.

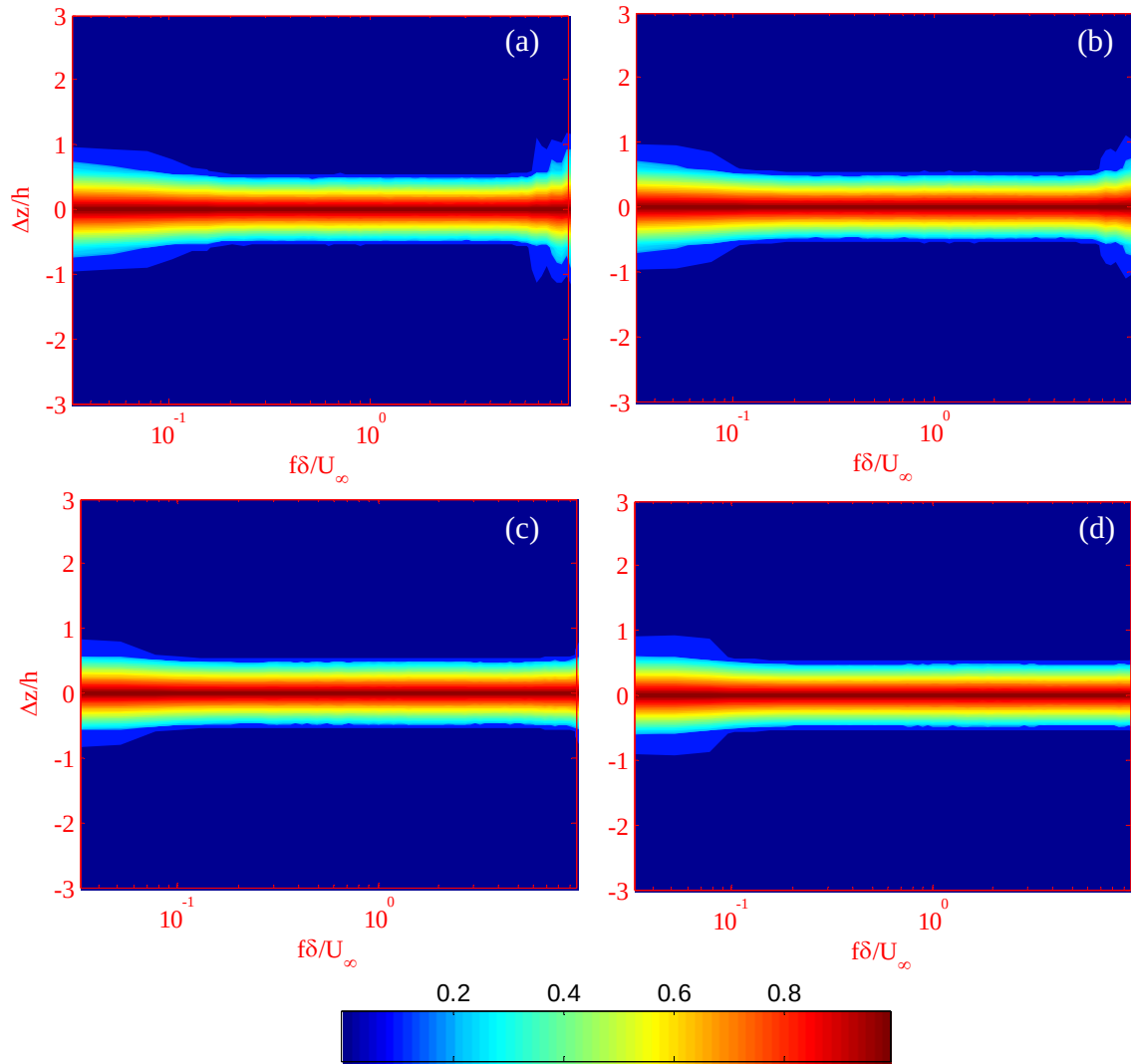


Figure 3-78 Contours of spanwise coherence on the face of rounded forward steps ($x/h = 0$, $y/h = 0.38$) and $U_\infty = 30$ m/s. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$, (d) $r/h = 25\%$.

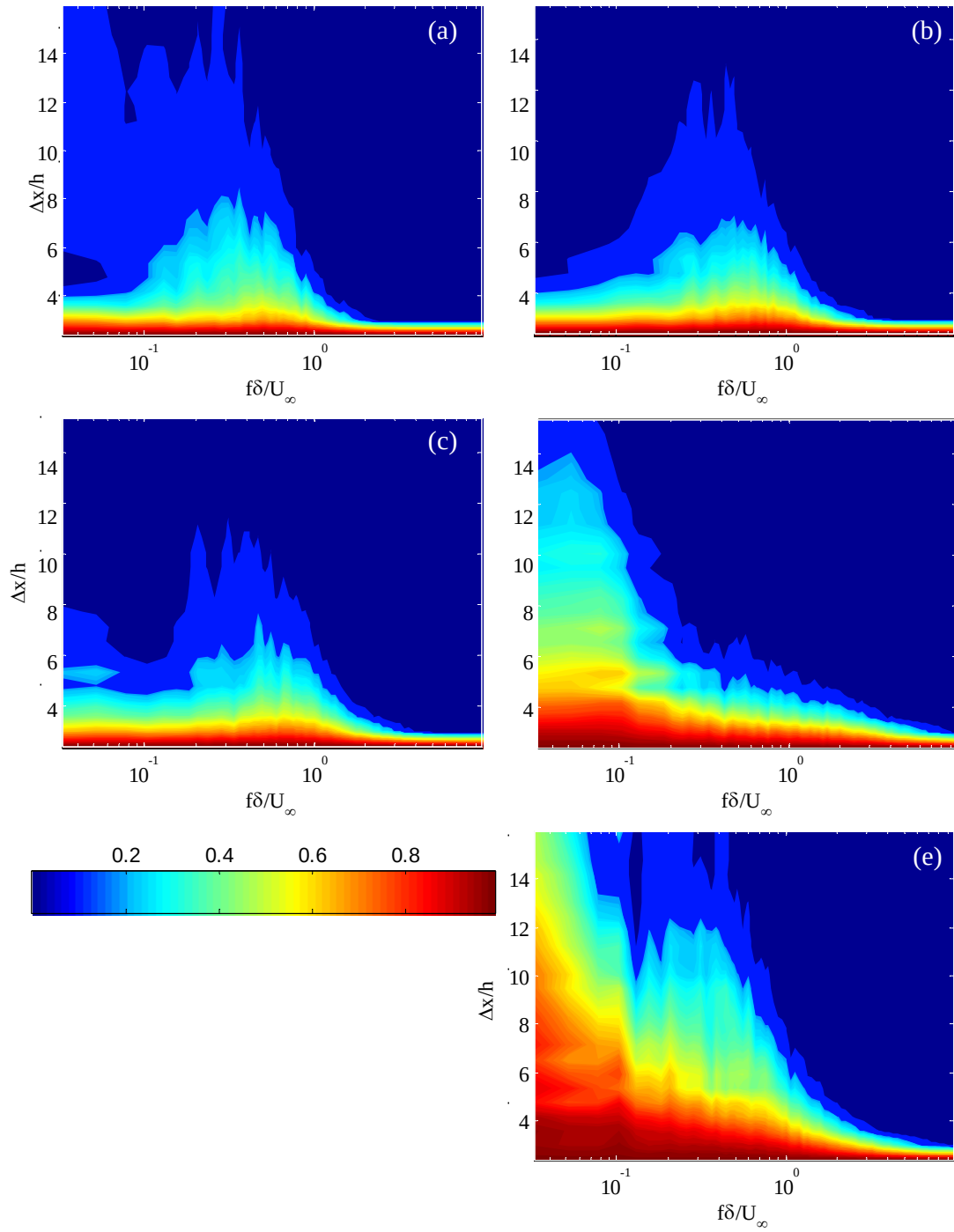


Figure 3-79 Contours of streamwise coherence of surface pressure fluctuations for rounded forward steps at $U_\infty = 30$ m/s. Coherence is calculated between the most upstream microphone in the streamwise array located at $x/h = 2.4$ and the rest of the microphones in the array located downstream of it. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$, (d) $r/h = 25\%$ and (e) Undisturbed boundary layer.

The results from both the streamwise and spanwise coherence of pressure fluctuations for rounded steps show a behavior which is consistent with previous results from flow visualization, mean and fluctuating point pressure. This behavior refers to the shortening of the separation bubble downstream of the step with increasing rounding that results in a reduction of the streamwise extent over which coherent pressure fluctuations are seen. These results also show that the downstream flow structure undergoes a major change once the step is rounded beyond $r/h = 12.5\%$. This change in flow structure correlates well with a significant lowering of far field sound seen in the wall-jet measurements suggesting that the downstream surface might be contributing to the far field sound. The results for the face of the step in this section show that the pressure fluctuations are independent of corner rounding. On the other hand, the identical behavior of the pressure fluctuations measured by the array on step face suggests that if this surface is responsible for noise generation then the lowering of sound is caused solely by the changing Green's function. However, as seen in the single point pressure fluctuation results the pressure statistics on the face do show variation as one moves closer to the rounded corner of the step. Furthermore, these statistics bear a close resemblance to those immediately downstream of the step in the separation bubble. This suggests that the overall sound may be a result of the events on both the surfaces of forward steps as proposed by Glegg *et al.* (2014).

To further explore the relationship between near and far field we now turn to the unsteady surface force measurements which in low Mach number flows is directly responsible for far field sound.

3.2.4 Unsteady Surface Force Measurements

The sound source mechanism in presence of solid surfaces like forward step is driven by the unsteady force on the surface in accordance with Curle's (1955) theorem. Thus, one can hope to gain some insight into the fundamental sound source involved in flow over forward steps through knowledge of unsteady forces. In order to extract the unsteady force we look at the array as a single sensor with discrete sensing points which are represented by the individual microphones. Due to the discrete nature of the array, the spatial sensitivity of the array is not uniform. This leads to the array response to the zero spanwise wavenumber component which represents the unsteady surface force being biased towards the large streamwise scales in the flow (i.e. smaller streamwise wavenumbers) in a similar fashion as the response of a single microphone discussed in 3.1.5.1. Therefore, before we discuss the unsteady force measurement results from the array we need to establish the limitations on these measurements. To do so, in the following sections we will compare the response of our actual discrete array when installed in spanwise orientation to that of an ideal sensor with uniform sensitivity and an infinite length in the spanwise direction. The analysis will involve passing a model of wavenumber-frequency spectrum of pressure fluctuations through the filters associated with both the discrete and the ideal sensor. Their frequency responses will then be compared to yield an upper bound up to which the discrete array approximates a spanwise infinite sensor with uniform sensitivity.

3.2.4.1 Array Wavenumber Response Function

The individual microphone sensitivity function (described in §3.1.5.1) can be combined together using the shifting property of Fourier transform operation to yield the sensitivity function for the entire array. Mathematically, the array response function for an array with N microphones then is given by

$$W(\boldsymbol{\kappa}) = \frac{1}{N} \sum_{n=0}^{N-1} H_n(\vec{\mathbf{k}}) e^{-i\mathbf{n}\mathbf{d}\cdot\boldsymbol{\kappa}} \quad (3-48)$$

where $H_n(\boldsymbol{\kappa})$ is the wavenumber filter function for the n^{th} microphone in the array, $\mathbf{d}$ is the separation vector which is aligned along the axis of the array and $\boldsymbol{\kappa}$ is the wave-vector. The wavenumber filter function for a circular transducer already derived in §3.1.5.1 is given by

$$H_n(\vec{k}) = \frac{2\alpha[|\kappa|R_n J_0(|\kappa|R_n) J_1(\alpha) - \alpha J_0(\alpha) J_1(|\kappa|R_n)]}{|\kappa|R_n J_2(\alpha)[\alpha^2 - |\kappa|^2 R_n^2]} \quad (3-49)$$

where R_n represents the diaphragm radius of the n^{th} microphone in the array, $\alpha = 2.675$ is a constant as given by Farabee and Geib (1991) and $J_m()$ is the m^{th} order Bessel function of first kind. Note that $R = 11.1$ mm for all the B&K 4190 microphones used in the array and $R = 3.2$ mm for the single B&K 4138 microphone at the center of the array. The center to center separation for each microphone in the array is $d = 14.9$ mm. Figure 3-80 shows a top view schematic describing the microphone arrangement in the array when it is in spanwise orientation. The figure also shows the array local coordinate system with its origin at the center of the microphone on extreme left when the array is viewed with respect to the oncoming flow. The figure also shows the position of the smaller B&K 4138 microphone which is at the center of the array. Note that the microphone separation vector $\mathbf{d}$ is aligned with the spanwise wave-vector κ_z in this particular case.

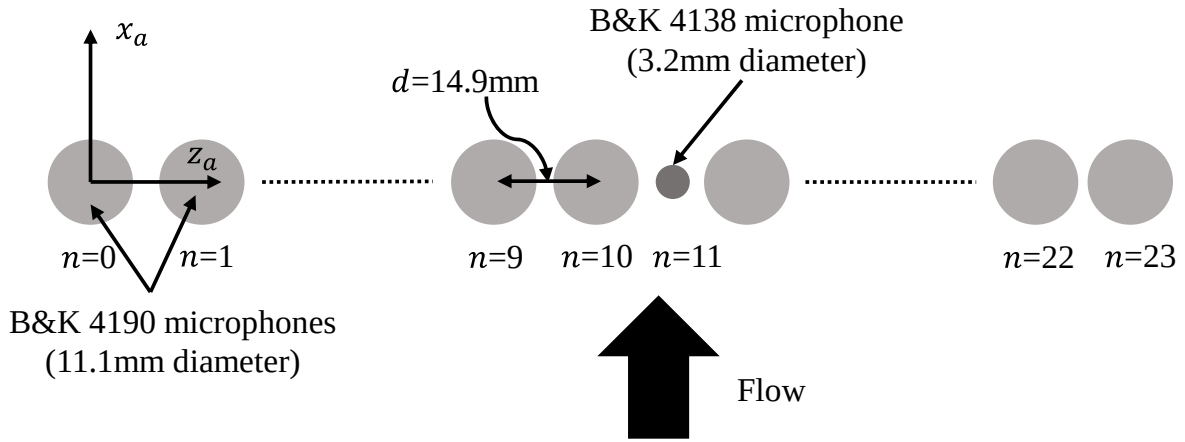


Figure 3-80 Illustration of the microphone arrangement and array local coordinate system for unsteady force measurement array in spanwise orientation.

When the array is in spanwise orientation the array response function takes the following form

$$W(\kappa) = \frac{1}{N} \sum_{n=0}^{N-1} H_n(\vec{k}) e^{-ind\kappa_z} \quad (3-50)$$

where d is now simply the center to center separation between each microphone and κ_z is the spanwise wavenumber. As shown in §3.1.5.1 the wavenumber and frequency response of a sensor are separable if the integral of the sensitivity function over the sensitive area of the sensor is equal to 1. This criteria was satisfied for a single circular microphone by introducing a constant S_0 . In the case of the array response function this criteria is inherently satisfied since it is the linear superposition of the response of individual microphone responses.

Figure 3-81 shows the zero spanwise wavenumber array response function for the present 24 element discrete array. Also shown in the figure are the individual response functions of the two different types of microphones used in the array. As seen in the figure the low wavenumber response ($\kappa_x < 200$ rad/m) of the array is identical to those of single microphones. At higher wavenumbers the array response lobes become more non-uniform compared to those for the microphones. While for the individual microphones the position of the troughs and peaks occur at integer multiples of wavenumber associated with their sensitive diameter, the locations of these features for the entire array varies with wavenumber.

This is because the array spatial sensitivity has a greater non-uniformity than a single microphone due to presence of gaps between the microphones as well as due to two sensors with different diameters.

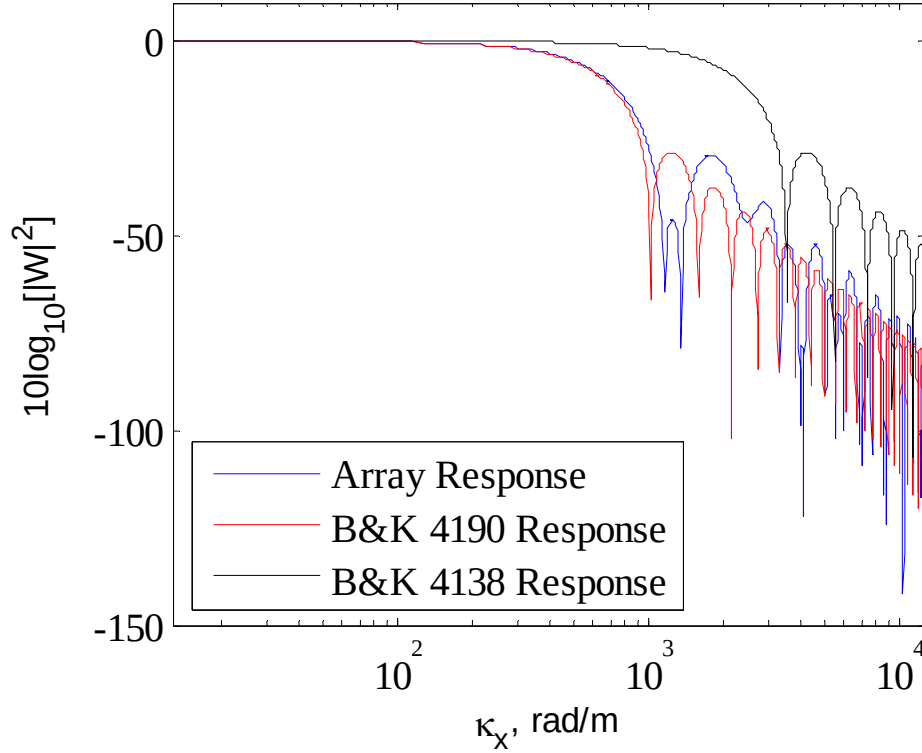


Figure 3-81 Array response function as a function of streamwise wavenumbers at $\kappa_z = 0$ rad/m. Also shown are the individual response functions of both the microphones used in the array.

3.2.4.2 Continuous Strip Sensor Response Function

In order to assess the capability of the present array to measure the unsteady surface force we need to compare its response to that of a continuous strip sensor whose span is infinite and is located at the same location as the present array. This strip force sensor is assumed to have a continuous uniform response to the flow field. Figure 3-82 (a) shows a top view schematic of this hypothetical strip sensor in spanwise orientation with respect to the flow. Also shown is the definition of the coordinate system local to this strip sensor which will be followed in the present discussion. Note that the point of origin of this coordinate system in streamwise direction is the same as that for the discrete array. The width of this sensor in the streamwise direction is a and the origin of the coordinate system that will be used in the present discussion is at mid-span and at the streamwise mid-point of the sensor as shown in the figure. Note that this view is comparable to the schematic of the actual array presented in Figure 3-80. The spatial sensitivity function of this strip sensor is shown in Figure 3-82 (b) in cross sectional view of the sensor. From the figure it can be seen that the spatial sensitivity function of this strip sensor is given by a boxcar function as

$$w_{strip}(x_s, z_s) = \begin{cases} S_0, & -\frac{a}{2} \leq x_s \leq \frac{a}{2} \text{ and } -\infty \leq z \leq +\infty \\ 0, & \text{otherwise} \end{cases} \quad (3-51)$$

where we recall that S_0 is a constant required to ensure that the wavenumber and frequency response of the sensor are separate. This constant then satisfies the following criteria

$$\int_{-\infty}^{\infty} \int_{-\infty}^{\infty} w_{strip}(x_s, z_s) dx dz = 1 \quad (3-52)$$

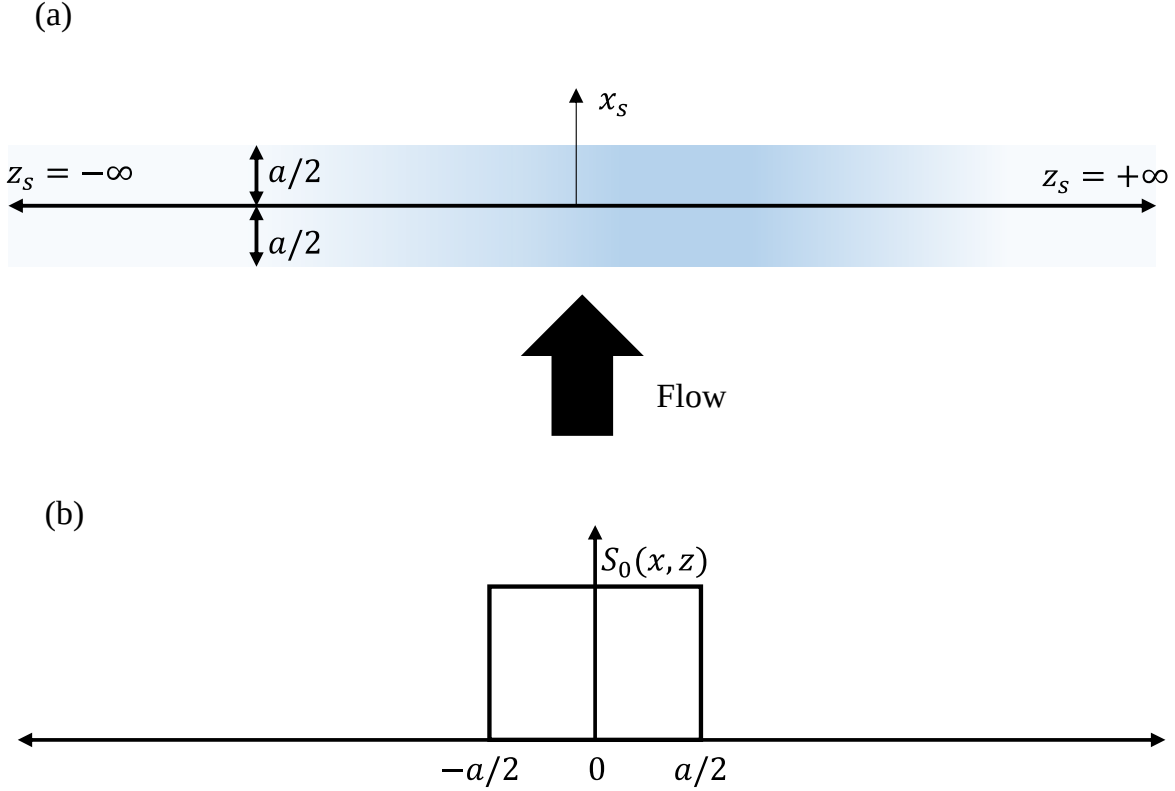


Figure 3-82 Illustration of a hypothetical spanwise infinite strip sensor with a uniform response. (a) Top view schematic of the sensor as seen by the flow and (b) Spatial sensitivity function of the strip sensor.

The wavenumber response function for this strip sensor is then given by the Fourier transform of the spatial sensitivity function. According to the definition of Fourier transform used in the present work, the wavenumber response is given by

$$W_{strip}(\kappa_x, \kappa_z) = \int_{-\infty}^{\infty} \int_{-\infty}^{\infty} S_0 e^{-i(\kappa_x x_s + \kappa_z z_s)} dx_s dz_s \quad (3-53)$$

Now, adjusting the limits of integration to reflect the streamwise extent of the strip sensor we get

$$W_{strip}(\kappa_x, \kappa_z) = \int_{-\infty}^{\infty} \int_{-a/2}^{a/2} S_0 e^{-i(\kappa_x x_s + \kappa_z z_s)} dx dz \quad (3-54)$$

Realizing that S_0 is just a constant the integrals in the above equation can be separated and written as

$$W_{strip}(\kappa_x, \kappa_z) = S_0 \int_{-\infty}^{\infty} e^{-ik_z z_s} \left[\int_{-\frac{a}{2}}^{\frac{a}{2}} e^{-i\kappa_x x_s} dx_s \right] dz_s \quad (3-55)$$

Then, solving the inside integral we get

$$W_{strip}(\kappa_x, \kappa_z) = \frac{2S_0 \sin\left(\frac{a\kappa_x}{2}\right)}{\kappa_x} \int_{-\infty}^{\infty} e^{-ik_z z_s} dz_s \quad (3-56)$$

The left over integral is just the Fourier transform of 1 in the spanwise direction so we get the following expression for the strip sensor response function

$$W_{strip}(\kappa_x, \kappa_z) = \frac{4\pi S_0 \sin\left(\frac{a\kappa_x}{2}\right)}{\kappa_x} \delta(\kappa_z) \quad (3-57)$$

where $\delta(\kappa_z)$ is the Dirac Delta function. In the above equation the scaling constant S_0 still remains unknown. To determine its value we solve the inverse problem that the inverse Fourier transform of the above equation must yield the spatial sensitivity function and its integration over the streamwise and spanwise boundaries of the sensor must be equal to 1 i.e.

$$\int_{-\infty}^{\infty} \int_{-a/2}^{a/2} \mathcal{F}^{-1}[W_{strip}(\kappa_x, \kappa_z)] dx_s dz_s = 1 \quad (3-58)$$

where $\mathcal{F}^{-1}[\]$ is the inverse Fourier transform operator. Then, using the definition of inverse Fourier transform and substituting the value for $W_{strip}(\kappa_x, \kappa_z)$ we get

$$\int_{-\infty}^{\infty} \int_{-a/2}^{a/2} \left[\frac{1}{(2\pi)^2} \int_{-\infty}^{\infty} \int_{-\infty}^{\infty} \frac{4\pi S_0 \sin\left(\frac{a\kappa_x}{2}\right)}{\kappa_x} \delta(\kappa_z) e^{i(\kappa_x x_s + \kappa_z z_s)} d\kappa_x d\kappa_z \right] dx_s dz_s = 1 \quad (3-59)$$

Now, evaluating the inverse Fourier transform defined inside [] we get

$$\int_{-\infty}^{\infty} \int_{-a/2}^{a/2} [2\pi S_0 \delta(z)] dx_s dz_s = 1 \quad (3-60)$$

where $\delta(z)$ is now the Dirac Delta function with the spanwise coordinate as its argument. Separating the two integral above yields

$$\int_{-\infty}^{\infty} \left[\int_{-\frac{a}{2}}^{\frac{a}{2}} [2\pi S_0] dx_s \right] \delta(z) dz = 1 \quad (3-61)$$

$$2\pi a S_0 \int_{-\infty}^{\infty} \delta(z) dz = 1 \quad (3-62)$$

The integral in the above definition is equal to 1 according to the definition of the Dirac Delta function. So, solving the above equation for S_0 finally gives

$$S_0 = \frac{1}{2\pi a} \quad (3-63)$$

Substituting this value of the scaling constant in equation (3-57) yields the final form of the wavenumber response function of the spanwise infinite continuous strip sensor as

$$W_{strip}(\kappa_x, \kappa_z) = \frac{2 \sin\left(\frac{a\kappa_x}{2}\right)}{a\kappa_x} \delta(\kappa_z) \quad (3-64)$$

Figure 3-83 (a) shows a comparison between the wavenumber filter function for the 24 discrete element array used in measurements and a strip sensor whose width in streamwise direction is $a = 11.1$ mm which is equal to the sensitive diameter of the B&K 4190 microphones used in the array. The filter function is shown as a function of streamwise wavenumbers for zero spanwise wavenumber. As seen in the figure the response of the discrete and continuous array is the same at small streamwise wavenumbers due to both array being unbiased towards larger structures. The first null in the response of the continuous sensor occurs at a wavenumber which corresponds to the width a ($\kappa_x = 2\pi/a$) of the sensor assumed in computation of the filter function. The subsequent nulls occur at even integer multiples of the wavenumber associated with array width ($\kappa_x = 2n\pi/a, n = 2, 4, \dots$) and the peaks occur at odd integer multiples ($\kappa_x = 2n\pi/a, n = 3, 5, \dots$). For the discrete array the first null appears at a wavenumber which corresponds to a length scale of about 6.6 mm after which the array response appears noisier and attenuates with a faster slope than the continuous array response.

If we now arbitrarily change the width of the continuous sensor to nearly coincide with where the first null appears in the discrete array response function ($a = 6.6$ mm, Figure 3-83 (b)) we see that the first null in the discrete array coincides with the null in strip sensor response after which the discrete array response continues its roll-off.

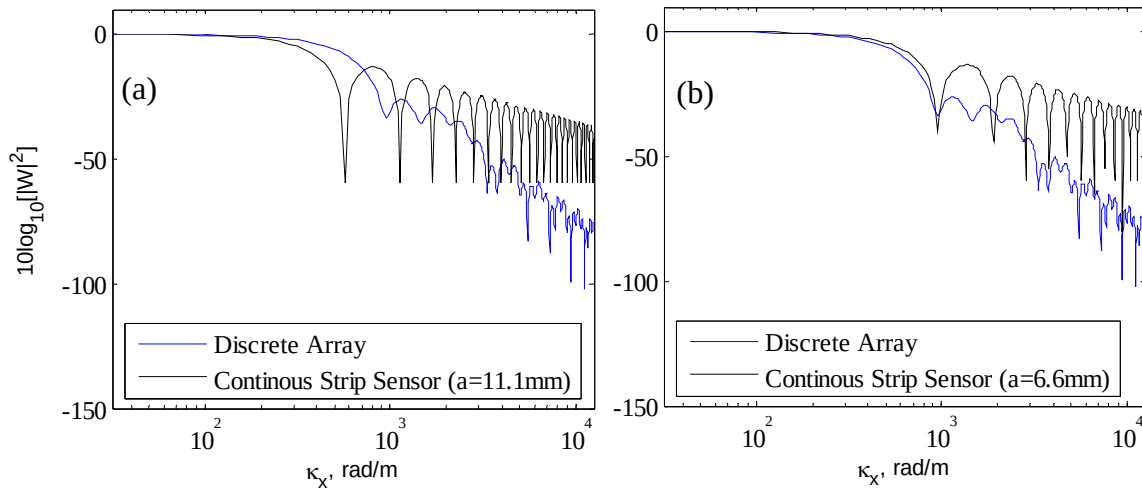


Figure 3-83 Zero spanwise wavenumber filter function for 24 element discrete array and continuous strip sensor as a function of streamwise wavenumber. (a) $a = 11.1$ mm and (b) $a = 6.6$ mm.

3.2.4.3 Comparison between Discrete Array and Continuous Strip Sensor

Now that we have established the wavenumber response of both the continuous and discrete array, analysis similar to that for a single microphone in §3.1.5.1 can be performed to establish the limitations on the frequency response of the discrete array. To do this, we will pass a wavenumber-frequency spectrum model of surface pressure through both the filter functions and then compare the frequency response. The frequency spectrum of surface pressure that is obtained in presence of an array with a filter function $W(\kappa_x, \kappa_z)$ is given by

$$\phi_{D0}(f) = \int_{-\infty}^{\infty} \int_0^{\infty} \Phi(\kappa_x, \kappa_z, \omega) |W(\kappa_x, \kappa_z)|^2 d\kappa_x d\kappa_z \quad (3-65)$$

Note that we have adjusted the limits of integration so that the integration is carried out for positive streamwise wavenumbers only. For a continuous strip sensor of width a the frequency spectrum is given by

$$\phi_{S0}(f) = \int_{-\infty}^{\infty} \int_0^{\infty} \Phi(\kappa_x, \kappa_z, \omega) |W_{strip}(\kappa_x)|^2 d\kappa_x d\kappa_z \quad (3-66)$$

The wavenumber-frequency spectrum to be used in equations (3-65) and (3-66) is the Corcos (1964) model the details of which are described in §3.1.5.1. Recall that four different models were used in this section to assess the effects of the spatial filtering action by a single microphone. When the results of using each model were compared to the measured data it was found that the Corcos (1964) model provided the best overall agreement. Therefore, this is the model we choose for the current analysis. Now, to establish the limits on the frequency response of the array we can combine equations (3-65) and (3-66) to form a ratio which we call *Array Factor*, this ratio is defined as

$$AF(f) = \frac{\phi_{D0}(f)}{\phi_{S0}(f)} \quad (3-67)$$

This *Array Factor* essentially compares the wavenumber response of the present discrete array and a hypothetical continuous strip sensor of width a placed at the same location and exposed to the same wavenumber-frequency spectrum of surface pressure. The width of the continuous sensor can be varied and the resulting frequency response can be compared to the discrete sensor. Then, we define the optimum width of a spanwise infinite sensor which is approximated by the discrete array as the width for which the upper limit of the frequency within which the $AF < 2$ dB.

Although equations (3-65) and (3-66) used to compute the *Array Factor* are analytical expressions the integral in each equation was evaluated numerically. Therefore, it was important to first check the numerical convergence of these integrals. The numerical convergence was tested by passing the Corcos (1987) wavenumber-frequency spectrum model at $U_{\infty} = 30$ m/s through a 11 mm wide strip sensor using different wavenumber resolution $(\Delta\kappa_x, \Delta\kappa_z)$ and the wavenumber limit $(\kappa_{x,max}, \kappa_{z,max})$. It was found that a wavenumber resolution of $\Delta\kappa_x = \Delta\kappa_z = 5\pi$ and a wavenumber range of $-2000\pi \leq \kappa_z \leq 2000\pi$ and $0 \leq \kappa_x \leq 4000\pi$ was sufficient to achieve numerical convergence.

Figure 3-84 shows the *Array Factor* calculated for different strip sensor width a . The width of the strip sensor considered is between 1 mm and 12 mm and it covers the diameter of both types of microphones used in the array. Also shown on each plot are dashed horizontal lines that represents ± 2 dB, the limit. Looking first at the figure it can be seen that at lowest frequencies the response of the discrete array and the strip sensor of any width is exactly the same. Around 300 Hz however, the strip sensor response begins to deviate from the discrete array. For a strip sensor width $a < 7$ mm the discrete array shows a drop while for $a \geq 7$ mm the discrete array begins to over estimate the strip sensor response with the peak value

increasing as the strip width increases. Looking at each of the cases shown in Figure 3-84 and applying the ± 2 dB criteria specified earlier we see that a strip sensor with $a = 7$ mm is the optimum case with a cutoff frequency of 3090 Hz.

One issue that must be addressed in the analysis above is the validity of using an undisturbed boundary layer wavenumber-frequency spectrum model to estimate the limitations of the present microphone array. This issue can be addressed if one considers the fact that the attenuation of force measured by the array in the undisturbed boundary layer should be a conservative estimate due to the lower wavenumbers dominating the flow structure in separated flows. This point is exhibited clearly by the analysis of the single point pressure data from the array previously done in §3.1.5. This analysis showed that the wavenumber-frequency spectrum models (especially the Corcos (1964) model used here) were able to, with reasonable accuracy, predict the frequency above which the attenuation from a larger diameter microphone becomes significant in an undisturbed boundary layer flow. Additionally, when the attenuation predicted by these models was compared to the attenuation measured in the step flow it was found that the modelling provided a good conservative estimate of the frequencies above which the attenuation from a larger microphone became significant in separated flow. In light of these facts it can be said that the analysis performed here to determine the limitations of the discrete microphone array in measuring the unsteady surface force is applicable to the flows being studied in the present work.

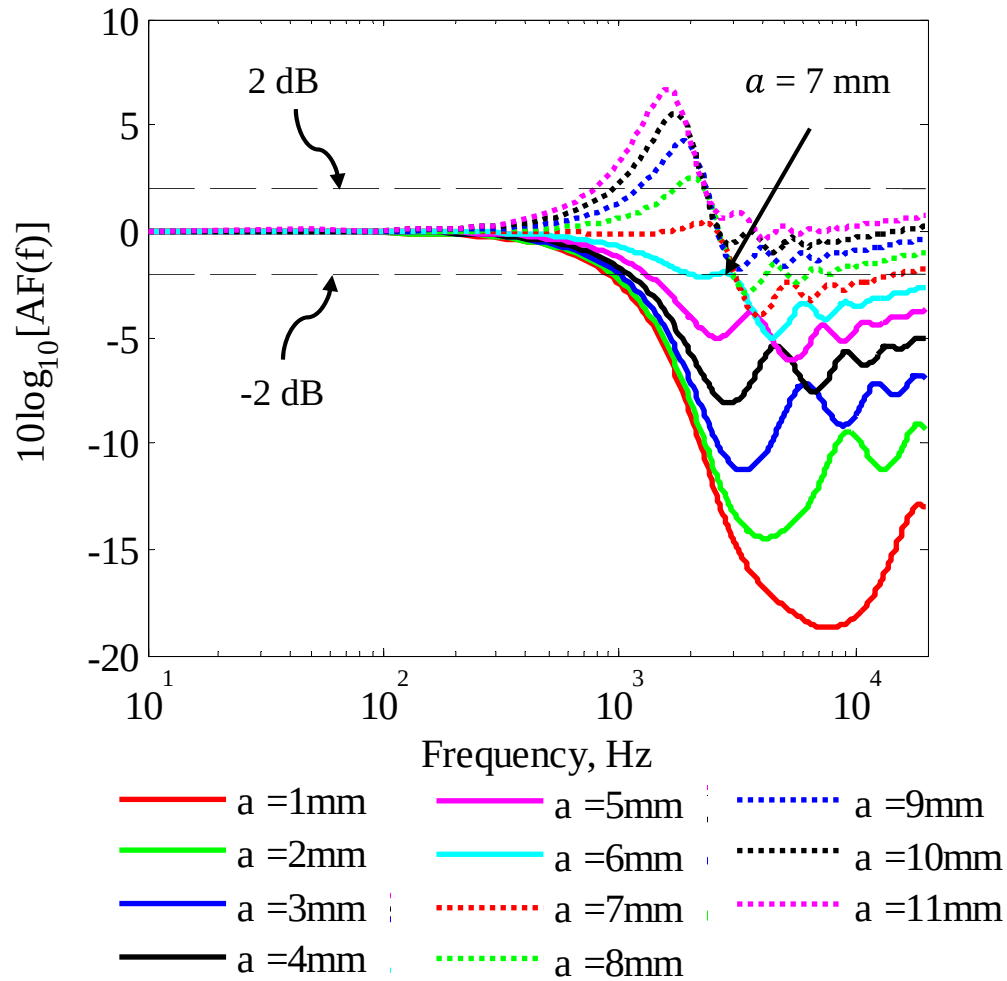


Figure 3-84 Array factors for different width of continuous strip sensor at $U_\infty = 30$ m/s. Also shown on the plot are the bounds for ± 2 dB used to define the cutoff frequency for the array.

3.2.4.4 Unsteady Surface Force in the Vicinity of Rounded Forward Facing Steps

The spectral density of the unsteady surface force is equivalent to pressure fluctuations spatially averaged over the entire sensitive area of the array. We denote the spectral density of the area averaged unsteady pressure as $\bar{\phi}_{pp}$ and it is given by

$$\bar{\phi}_{pp}(f) = E[P_{avg}(f)P_{avg}^*(f)] \quad (3-68)$$

where $E[]$ is the expected value operator, $P_{avg}(f)$ refers to the Fourier transform of the spanwise averaged pressure and $P_{avg}^*(f)$ is its complex conjugate. $P_{avg}(f)$ is given by

$$P_{avg}(f) = \mathcal{F} \left[\frac{1}{N} \sum_{n=0}^{N-1} p_n(t) \right] \quad (3-69)$$

where $\mathcal{F}[]$ is the Fourier transform operator, N is the number of spanwise microphones in the array, n is the index which runs over the microphones with 0 denoting the first microphone in the array and $N - 1$ denoting the last microphone and, $p_n(t)$ is the time series pressure signal measured by the n^{th} microphone in the array. To obtain $P_{avg}(f)$ the time series pressure signals were first averaged and then Fourier transformed to obtain the spectral density of unsteady surface force.

Figure 3-85 (a) and (b) show $\bar{\phi}_{pp}$ (in dB, $p_{ref} = 20 \mu\text{Pa}$) measured at three different streamwise location for the unrounded forward step at $U_\infty = 30 \text{ m/s}$ and 40 m/s respectively. Also shown in each plot are the spectra obtained for undisturbed boundary layer flow at that particular free stream velocity. The ‘×’ marks on each of the curves denote the frequencies at which the response of the measurement array is expected to deviate more than 2 dB from the response of a 7 mm wide infinite span sensor with a uniform response. Recall that the upper limit of the array response as established by the analysis in previous section is 3090 Hz. In any subsequent discussion in this section, the spatially averaged unsteady pressure will only be shown for frequencies below this cutoff frequency. Note that the $\bar{\phi}_{pp}$ for the undisturbed boundary layer flow at $U_\infty = 30 \text{ m/s}$ shown in Figure 3-85 (a) is only shown for frequencies above 50 Hz due to low frequency contamination of the microphone signal by the facility noise. Consider now the unsteady pressure spectrum at $U_\infty = 30 \text{ m/s}$ in Figure 3-85 (a). We first note that the pressure at frequencies below about 100 Hz is approximately the same for each measurement location shown and exceeds undisturbed flow levels by more than 20 dB. Comparing now the pressure on the downstream surface of forward step ($x/h = 1.3$ and 3.1) we see that the spectrum is identical for both locations except for a small range between about 100 and 350 Hz where the spectrum at $x/h = 3.1$ exceeds that at $x/h = 1.3$ by up to about 2 dB. For the most downstream measurement station, which is within the reattachment zone ($x_r/h = 3$), the spectrum begins to roll-off at a frequency of about 200 Hz while the spectrum at $x/h = 1.3$ begins to roll-off at approximately half of that frequency. These roll-off frequencies are consistent with the decay of spanwise coherence seen in §3.2.3.2 as one moves further upstream into the separation bubble. Overall, however, the spectra at $x/h=1.3$ and 3.1 are surprisingly similar suggesting that the form and level of the unsteady force spectrum may not vary much over the bubble length.

Now consider the unsteady force spectra on the face of the step at $U_\infty = 30 \text{ m/s}$ as shown in Figure 3-85 (a) which would be representative of a streamwise aligned dipole acoustic source with the dipole axis parallel to the velocity vector. We see that at frequencies between 100-350 Hz the pressure on the face is about 5-7 dB lower than that on the downstream side. At frequencies above 350 Hz, $\bar{\phi}_{pp}$ on the two surface of the step are within 3 dB. At first glance, this difference between the pressures on the two surfaces does not quite agree with the hypothesis of Glegg *et al.* (2014) that the force on the two surface of the step is the same. However, one must remember that the results shown here for the step face do not represent the entire force on the step.

If we now increase the free stream velocity to $U_\infty = 40$ m/s (Figure 3-85 (b)) we see no qualitative change in the behavior of $\bar{\phi}_{pp}$ with the two downstream spectra being nearly identical and the spectra on the face rolling off at extremely low frequencies.

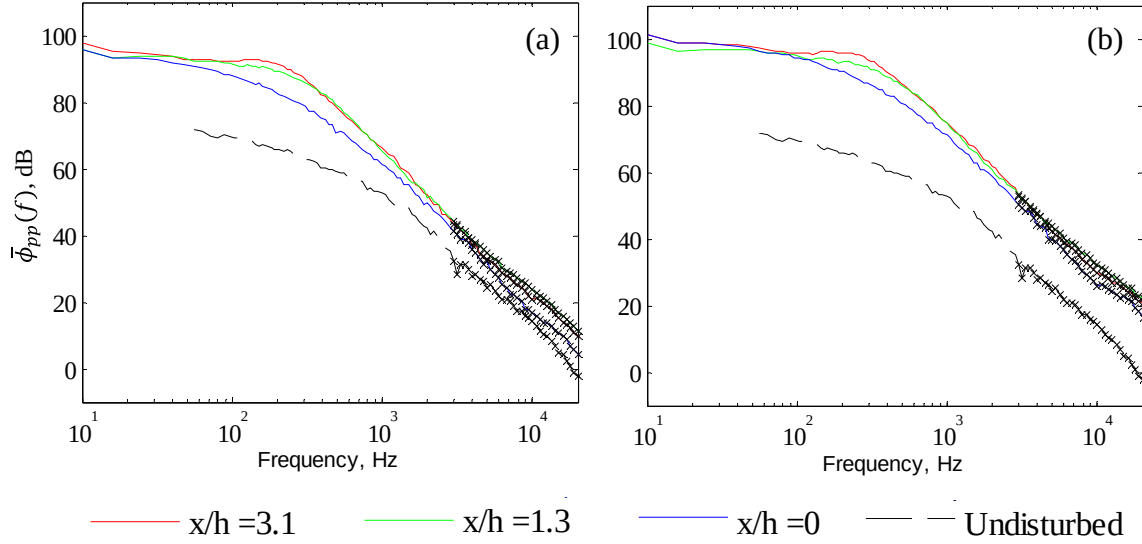


Figure 3-85 Unsteady force spectra at three streamwise measurement location for flow over an unrounded forward step. (a) $U_\infty = 30$ m/s and (b) $U_\infty = 40$ m/s. The ‘x’ marks on each curve denote the frequency range for which the discrete array response deviates more than 2dB from a 6 mm wide spanwise infinite strip sensor. Also note that for $U_\infty = 30$ m/s in (a) the unsteady force spectra for undisturbed flow has been shown only for frequencies above 50 Hz due to background contamination.

Figure 3-86 shows the unsteady force for $r/h = 0\%$ step as a function of five different velocities at each of the three streamwise measurement locations. The highest velocities in these spectra is $U_\infty = 40$ m/s because for most cases increasing the velocity beyond this induced pressure fluctuations which exceeded the dynamic range of B&K 4190 microphones resulting in clipped signals. Looking at the figure we first note that increasing the velocity shifts the spectra up in each case quite uniformly without affecting the shape. On the downstream surface of the step the spectra at 40m/s at both $x/h = 1.3$ and 3.1 (Figure 3-86 (a) and (b)) exceeds those measured at 20 m/s by 8-10 dB and 25 dB at lowest and highest plotted frequencies, respectively. As we move to the face of the step (Figure 3-86 (c)) increasing the velocity has slightly more effect at the lowest frequencies than seen downstream but overall the dependence on velocity is quite similar.

We can scale these spectra by choosing a length and a velocity scale. We scale the spectra on the dynamic pressure, free stream velocity and the step height, this scaling is given by

$$\bar{\phi}_{pps}(f_s) = \frac{\bar{\phi}_{pp}(f)U_\infty}{(\rho U_\infty)^2 h} \text{ and } f_s = fh/U_\infty \quad (3-70)$$

where the subscript S now refers to the scaled spectra and frequency. Figure 3-87 shows the scaled spectra. This scaling collapses the spectra quite well on both the step face and the downstream surface. This result shows that the form of the unsteady force spectra on step faces is almost invariant with flow velocity over the range measured.

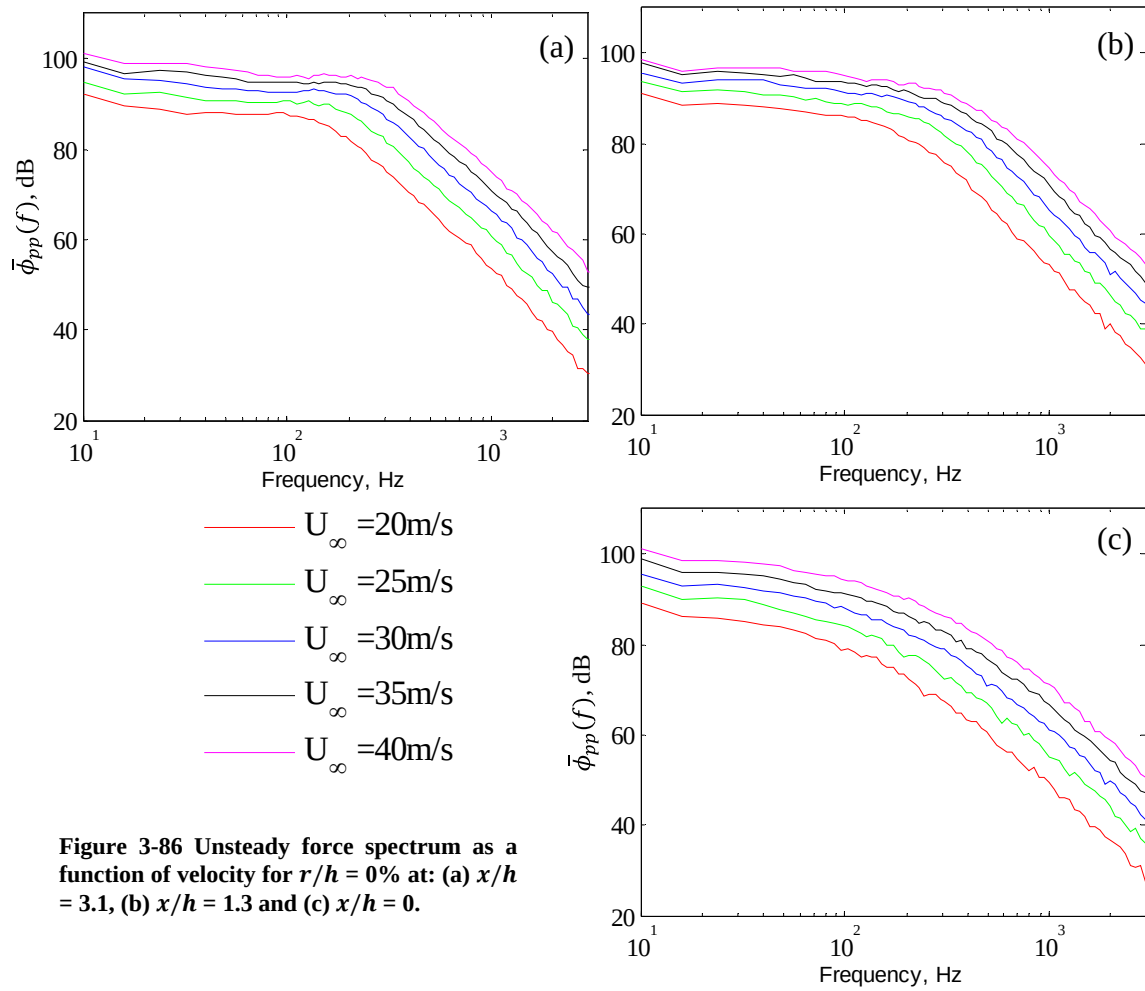


Figure 3-86 Unsteady force spectrum as a function of velocity for $r/h = 0\%$ at: (a) $x/h = 3.1$, (b) $x/h = 1.3$ and (c) $x/h = 0$.

We now consider the effect of rounding the step corner on $\bar{\phi}_{pp}$, shown in Figure 3-88 (a)-(c) for $U_\infty = 30$ m/s. Also shown on each plot is the spatially averaged pressure spectrum for the undisturbed boundary layer. For $r/h = 6.25\%$ (Figure 3-88 (a)) low-frequency spectral levels are 20 dB above those of the undisturbed boundary layer, as for the unrounded step. The most noticeable difference with the unrounded step is seen in the spectrum at $x/h = 3.1$. For the unrounded step the spectra at $x/h = 3.1$ showed levels which were similar to that at $x/h = 1.3$ due to both these locations being within the separation bubble for that step. However, for $r/h = 6.25\%$ $x/h = 3.1$ is no longer within the separation bubble and spectral levels are about 8 dB lower at the lowest frequencies. At frequencies above about 100 Hz however, the spectrum at $x/h = 3.1$ begins to rise and comes within 2.5 dB of that measured at $x/h = 1.3$. The reason behind this high frequency behavior is perhaps that the unsteady pressure immediately downstream of the separation bubble is being dominated by the faster moving vortices being shed from the separation bubble. The spectra on the step face for this step is similar to that seen for $r/h = 0\%$ in that it has a very small plateau region at extremely low frequencies after which it rolls-off steeply.

As we round the step corner further to $r/h = 12.5\%$ (Figure 3-88 (b)) the separation bubble shrinks further in size ($x_r/h = 1.6$) which shifts the measurement station $x/h = 3.1$ further downstream of the bubble and leads to a further reduction in low frequency component of $\bar{\phi}_{pp}$ there. Within the separation bubble ($x/h = 1.3$) and on the step face however, the spectrum looks very similar to that for $r/h = 6.25\%$. For the highest rounding radii of $r/h = 25\%$ (Figure 3-88 (c)) the spectrum on the downstream surface shows a much different behavior, consistent with the overall change in flow structure to a weak and thin

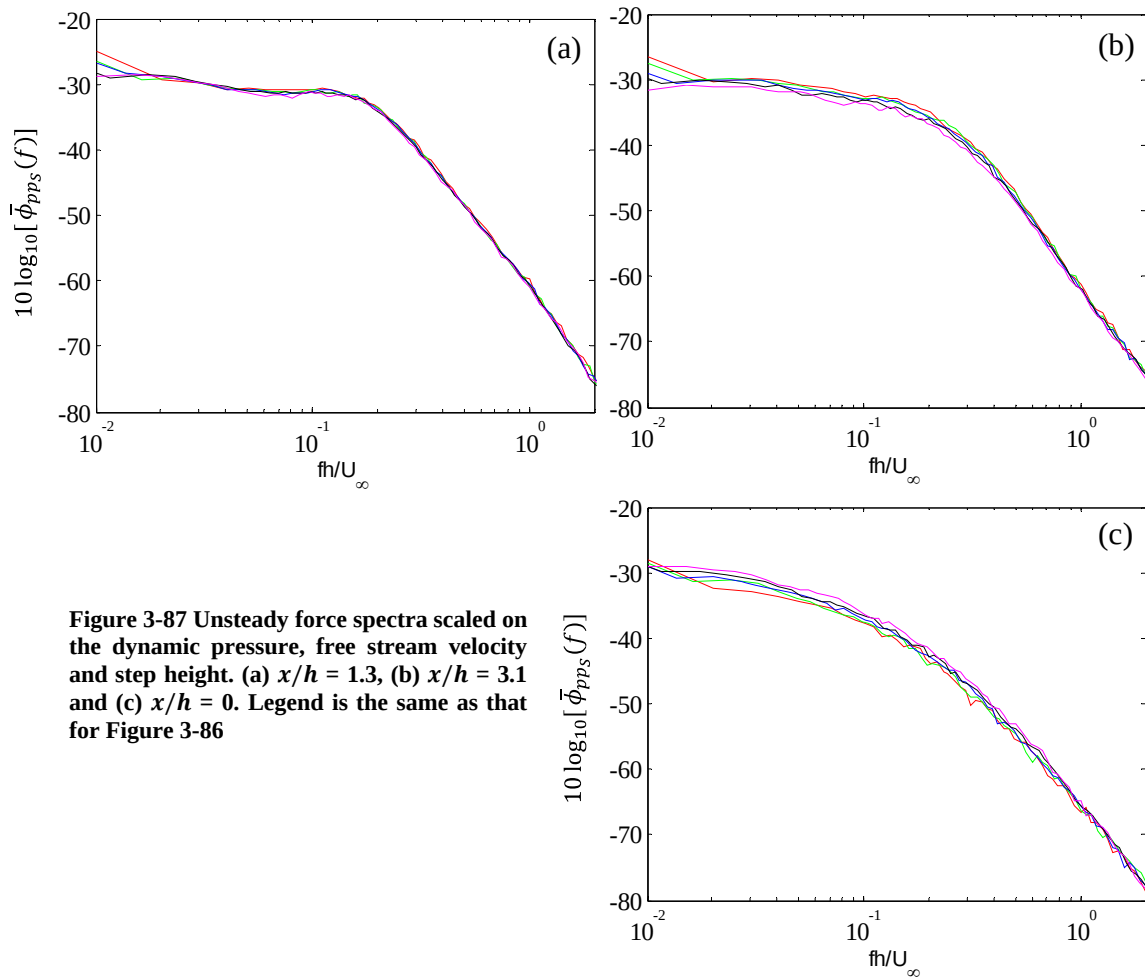
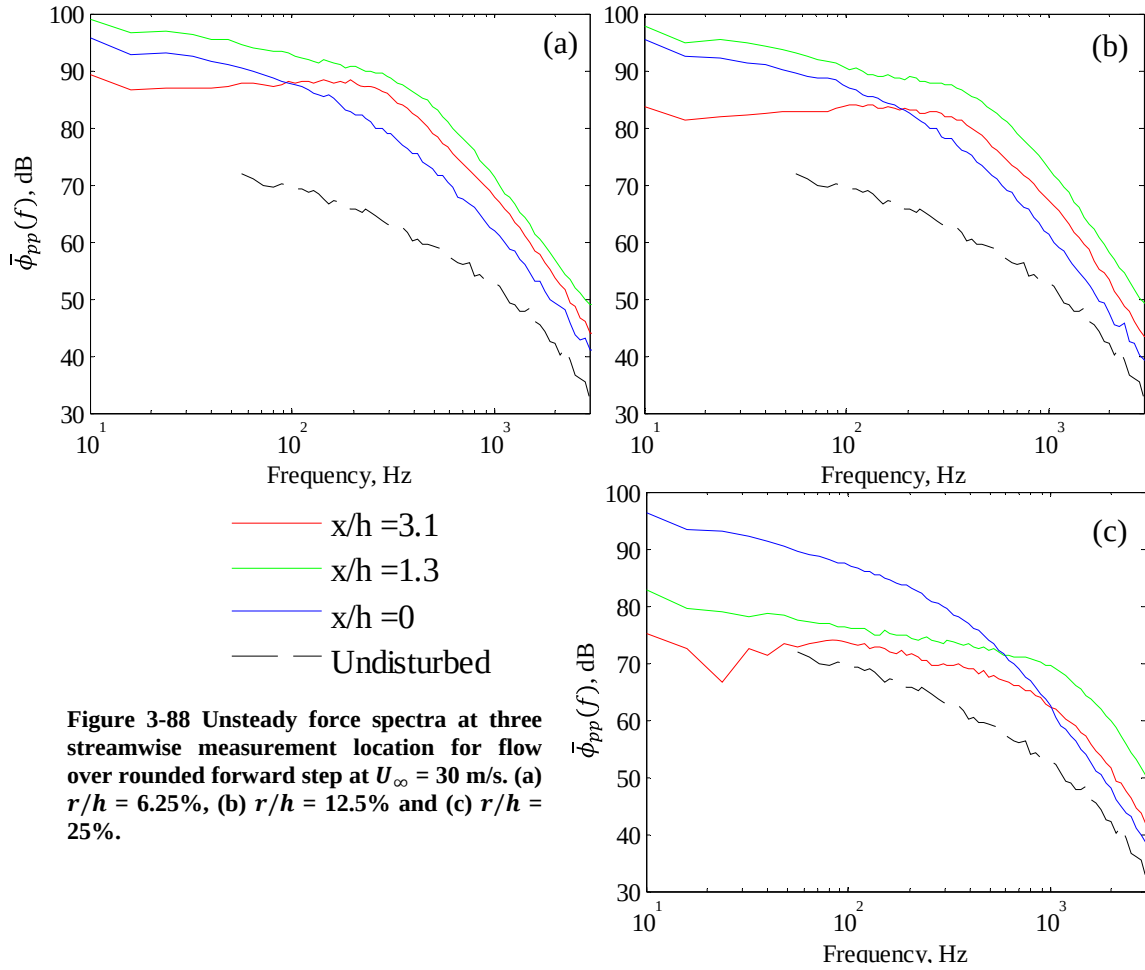


Figure 3-87 Unsteady force spectra scaled on the dynamic pressure, free stream velocity and step height. (a) $x/h = 1.3$, (b) $x/h = 3.1$ and (c) $x/h = 0$. Legend is the same as that for Figure 3-86

separation bubble observed in other quantities (mean pressure, two point fluctuating pressure statistics etc.). The spectrum on the step face for $r/h = 25\%$ however is still significantly elevated at low frequencies compared to both the downstream measurement stations and seems independent of the corner rounding.

This becomes much clearer when the unsteady force spectra $\bar{\Phi}_{pp}$ are plotted as a function of rounding at each measurement location, Figure 3-89. At $x/h = 3.1$ (Figure 3-89 (a)) we see a consistent reduction in the low frequency part of the spectrum with increasing rounding as this measurement location becomes further downstream of the separation bubble as the step corner is rounded. The reduction is about 6 dB as the step is rounded to $r/h = 6.25\%$, a further 6 dB as it is rounded $r/h = 12.5\%$ and about when the rounding is further increased to $r/h = 25\%$. Interestingly, rounding of the corner does not affect the spectra at the highest frequencies, particularly for $r/h \leq 12.5\%$.

At $x/h = 1.3$ (Figure 3-89 (b)), we see quite similar spectra for $r/h = 0\%$, 6.25% and 12.5% , the three cases for which this location is within the separation bubble. Quantitatively, there are some differences however. At frequencies below about 400 Hz spectral levels for these cases are within 3 dB of each other with levels increasing slightly with rounding. Above 400 Hz the spectra for $r/h = 6.25\%$ and 12.5% show similar levels but roll off later than that for the unrounded step resulting in levels 6-8 dB higher at the same frequency. This could be a result of the location of measurement with respect to the size of the separation bubble. As the rounding increases this particular location finds itself further downstream within the separation bubble. Based on the oil flow visualization measurements $x/h = 1.3$ is located at $x/x_r = 44\%$, 62% and 81% for $r/h = 0\%$, 6.25% and 12.5% respectively. Thus, assuming that the structure of the bubble

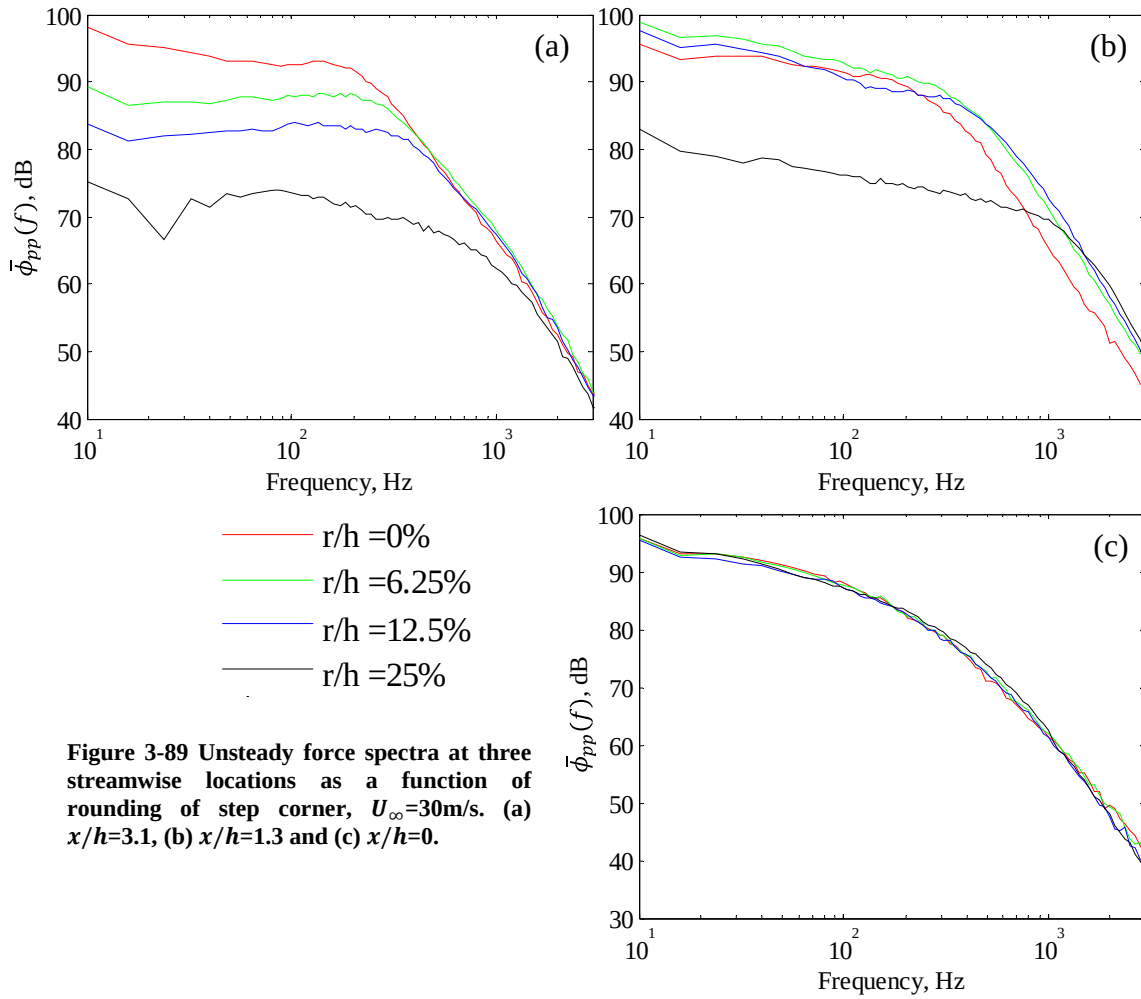


does not change significantly it appears that the unsteady force increases as one moves towards the reattachment.

Finally, consider $\bar{\Phi}_{pp}$ on the step face, Figure 3-89 (c), which is almost completely independent of step rounding. This un-changing nature of the force on the step face suggests that it is being governed completely by the events upstream of the step, including the upstream separation bubble whose size as shown by the oil flow visualization is nearly independent of the step rounding. Gaudent and Winter (1973) who measured drag on the face of forward steps with rounded corners in low Mach number flow ($M = 0.2$) have shown that the drag (or steady force on step face assuming step acts like a bluff body) decreases with increasing rounding and this reduction is larger for a taller step. In terms of their step height in wall units ($h^+ = hu_t/\nu$) their drag measurements show that at for a $h^+ = 1000$ there is a significant reduction in force on the face with increasing rounding but for $h^+ = 200$ this reduction is not as intense. The step height in present work in wall units is approximately $h^+ = 1650$ at $U_\infty = 30$ m/s and so according to their results the steady force on the step face should decrease with increasing rounding. However, the results of Figure 3-89 (c) which are made within the lower half (and in the region with a lower value of h^+) of the face of the step show that this is not the case. These measurements therefore do not imply that the force on the entire face is independent of the corner rounding and it is possible that the force varies with rounding on the upper half of step face which would lead to an overall variation in drag force with rounding.

The theory of Glegg *et al.* (2014) suggests that the unsteady force on the top surface and step face is the same and its resultant placed at the corner of the step combined with a suitable Green's function

determines the far field sound behavior. The present measurements of the spatially averaged unsteady pressure shows some interesting features as they relate to this theory. Firstly, we see that the behavior of the unsteady force is very similar within the downstream separation bubble for each rounded step. This similarity is further demonstrated below in Figure 3-90 which shows the spectra measured at $x/h = 1.3$ normalized on the free stream dynamic pressure and respective reattachment length for each step. Disregarding $r/h = 25\%$ (since the measurement location is far downstream of separation bubble) we see that the spectra for each rounded step has the same non-dimensional shape and are within 3 dB of each other. The similarity of the spectra suggests that if one were to just assume that the force on the step face is independent of rounding, one possible explanation for attenuation of far field sound with rounding as seen in wall-jet measurements could be the shrinking separation bubble size combined with reduced scattering efficiency due to rounding of the corner (which would be described by a suitable Green's function).



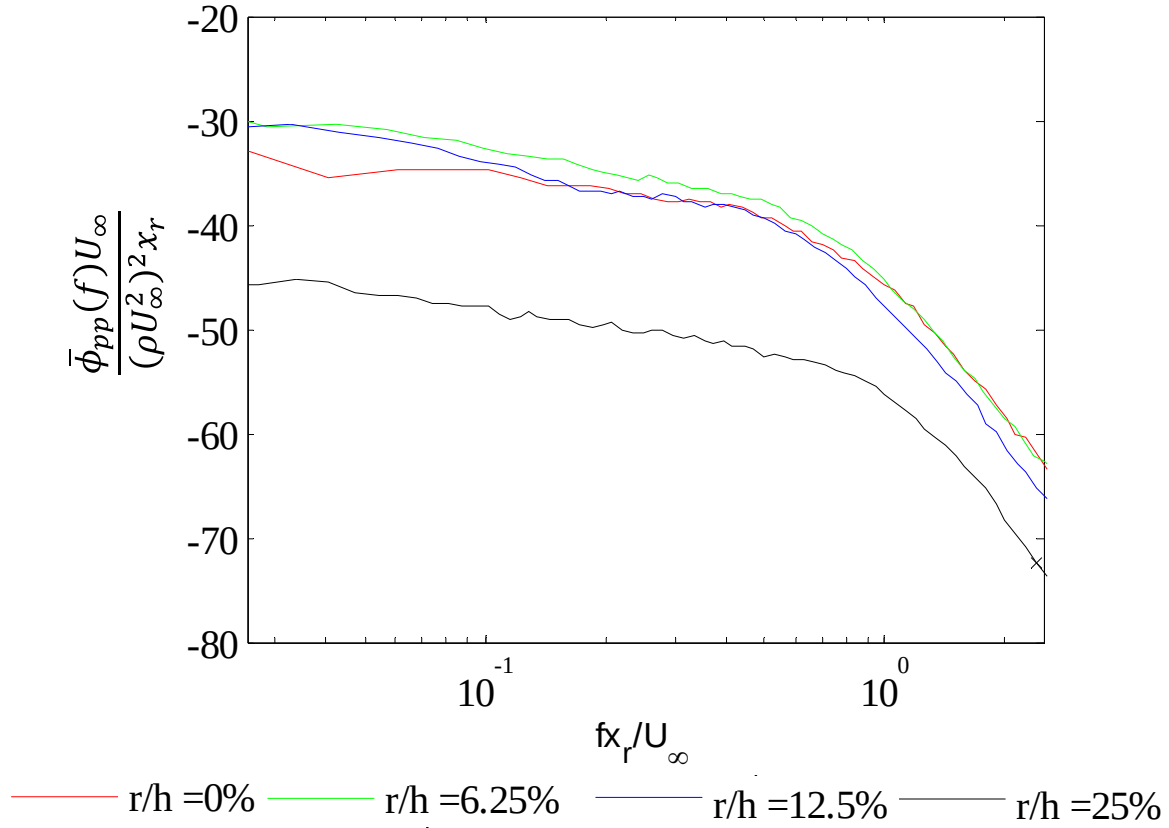


Figure 3-90 Unsteady force spectra for rounded forward steps within the separation bubble normalized on the reattachment length associated with each rounding.

Finally, we look at the velocity scaling of $\bar{\phi}_{pp}$ for rounded forward steps using the same scaling law as used for unrounded forward step (equation (3-70)). Figure 3-91 shows the scaled spectra for the three rounded steps downstream of the step at $x/h = 1.3$ and on the step face. Looking at these spectra we see that scaling based on 3rd power of U_{∞} also collapses each data set for rounded forward steps except downstream of the $r/h = 25\%$ forward step due to this location experiencing a completely different flow structure compared to other forward steps. On the face of this step however, we still see excellent collapse of the data. These scaling again show that within the downstream separation bubble the unsteady force (and hence the dipole sound source) has the same form for $r/h \leq 12.5\%$ and on the step face the force (at least on the lower half of the face) has the same form for rounding up to $r/h = 25\%$. Although not shown, the scaling also collapses the data at $x/h = 3.1$ for $r/h = 6.25\%$ and 12.5% but not for $r/h = 25\%$.

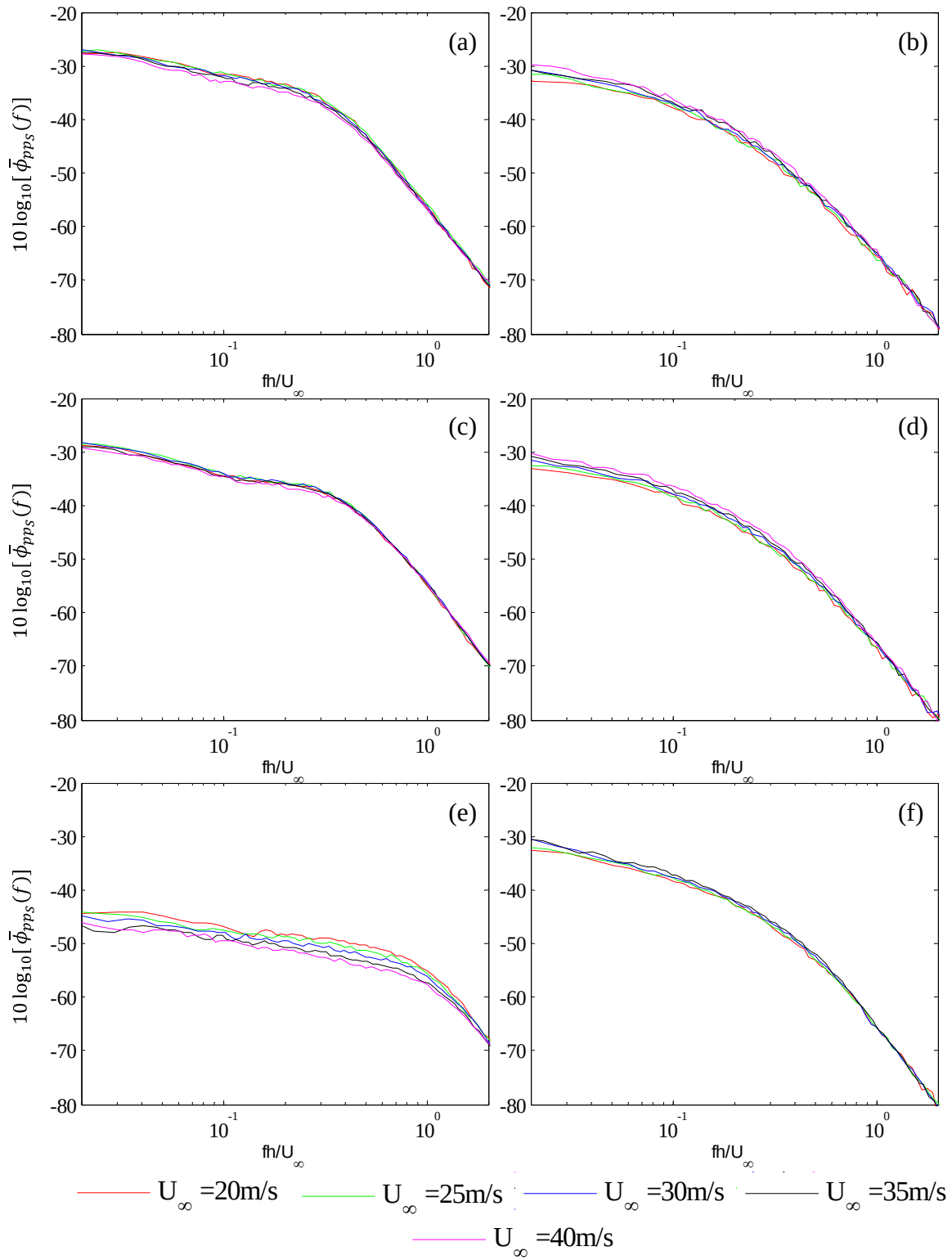


Figure 3-91 Scaled unsteady force spectra for rounded forward steps. $r/h = 6.25\%$: (a) $x/h = 1.3$ and (b) $x/h = 0$. $r/h = 12.5\%$: (c) $x/h = 1.3$ and (d) $x/h = 0$. $r/h = 25\%$: (e) $x/h = 3.1$ and (f) $x/h = 0$.

3.2.5 Far Field Sound from In-Flow Microphones

The far field sound measured by the four in-flow microphones (see §3.1.7 for details of the setup) will now be discussed. The time series pressure recorded by these microphone was Fourier transformed to yield the auto and cross-spectral densities of sound pressure. The microphone locations with respect to the inside corner of the step have been shown in Figure 3-46 of §3.1.7. As shown in this figure the radial distance of the microphone is defined from the inside corner of the step and is approximately 1.9 m for each microphone. The angular position of the microphones is defined such that $\theta = 0^\circ$ points in the downstream direction. Note that this nomenclature is consistent with that followed in the wall-jet facility work.

Figure 3-92 shows the far field sound spectra measured for $r/h = 0\%$ forward steps at four observation angles for $U_\infty = 30$ m/s. Also shown in each plot is the measurement made by the same microphone in absence of the forward step. Looking at Figure 3-92 (a) which shows the spectra at $\theta = 155.1^\circ$ we can see that even though the presence of the step is detected by these microphones, the SNR is only about 2 dB throughout the frequency range. The spectra measured at $\theta = 141.9^\circ$ (Figure 3-92 (b)) also shows similar qualitative and quantitative behavior. As one moves downstream of the step (Figure 3-92 (c) and (d)) the SNR stays the same but the sound from both the step and no-step case is higher at lower frequencies than that observed upstream of the step. The likely explanation for these elevated levels is that the downstream in-flow microphones are located close to the hard-wall diffuser section and are probably being contaminated by acoustic reflections of the facility noise. If we now increase the velocity to $U_\infty = 40$ m/s the overall sound levels, as shown in Figure 3-93 (a) and (b) for the microphones at $\theta = 155.1^\circ$ and 15.2° respectively, measured by the microphones increase but the SNR stays fairly the same due to enhanced facility noise.

The examination of Figure 3-92 and Figure 3-93 suggests that even though the presence of step is detected by the in-flow microphones the spectra are contaminated by the background acoustics making it difficult to distinguish the step noise. Let us now look at the effect rounding the step corner on the sound measured by in-flow microphones. The sound spectra measured by the four in-flow microphones at $U_\infty = 30$ m/s for all four step corner rounding have been shown in Figure 3-94. The background sound (measured in absence of the step by the same microphones) has been subtracted from these spectra. The subtracted levels for frequency bins where the SNR (ratio between step and no-step case in dB) is less than 1 dB have not been shown in this figure. Looking at the figure it can be seen that the in-flow microphones are unable to distinguish the effect of corner rounding on far field sound due to low SNR. As evident in Figure 3-94 the background contamination of sound measured by in-flow microphones makes it difficult to interpret the results from these measurements. Although not shown here, the SNR at other velocities is also low.

The reason a single microphone is unable to distinguish the effect of rounding in the present facility is because the sound pressure measured by the microphone is spatially unbiased. In other words, it is not possible for a single microphone to differentiate between the sound being radiated by the flow over the test artifact and the sound being radiated by the facility. This inability of the in-flow microphones to measure the effect of rounding the step corner on far field noise was the motivation behind using the phased array described in §3.1.6. As described in that section, a phased array consists of a set of microphones whose combined output along with the known source to observer distance of each microphone in the array can be used to provide spatial filtering that can be helpful in locating the sound source of interest. The results from this phased microphone array will now be described in the following section.

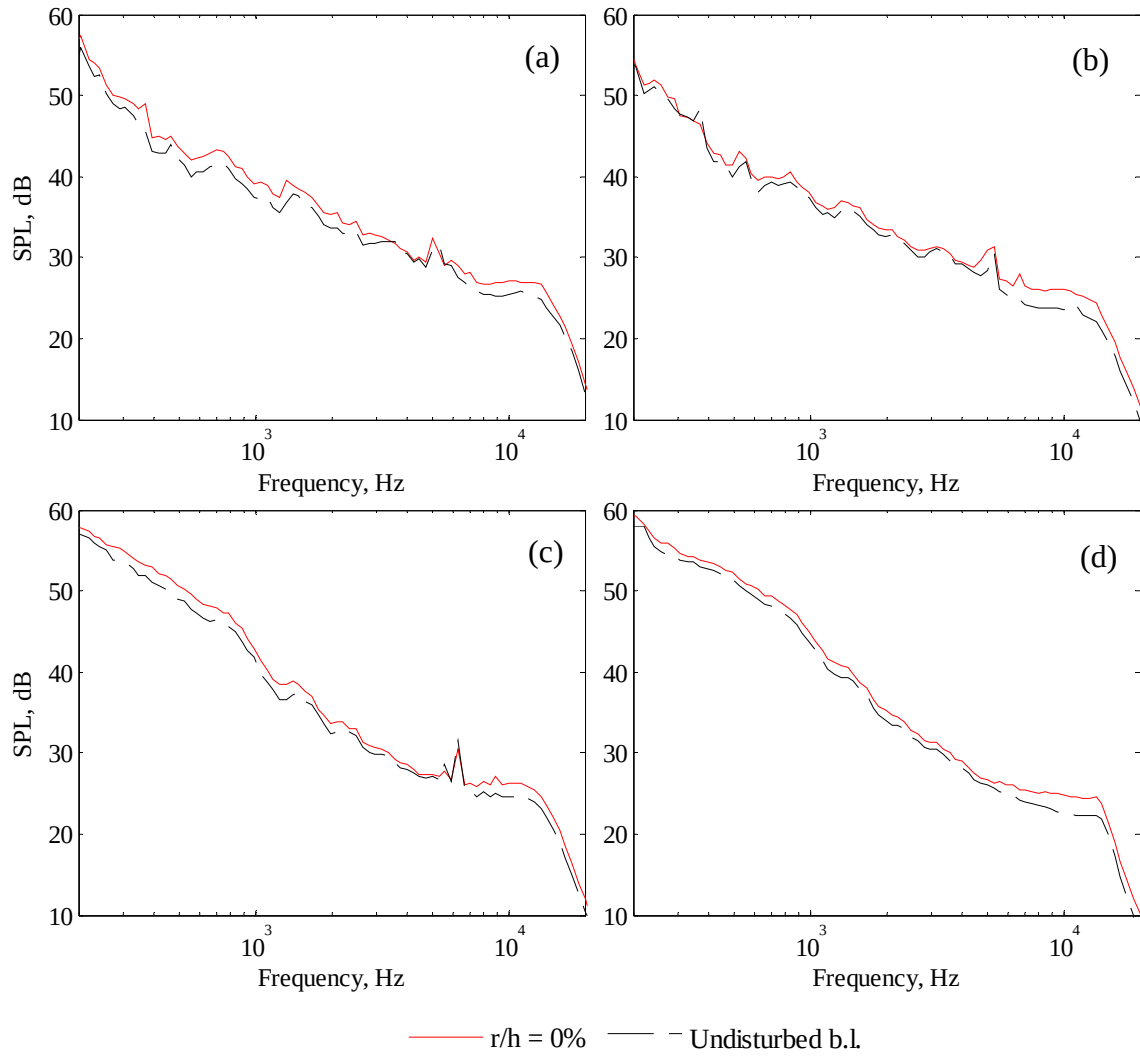


Figure 3-92 Far field sound measured for $r/h=0\%$ forward step compared to undisturbed boundary layer at $U_\infty = 30$ m/s using in-flow microphones. (a) $\theta = 155.1^\circ$, (b) $\theta = 141.9^\circ$, (c) $\theta = 22^\circ$ and (d) $\theta = 15.2^\circ$.

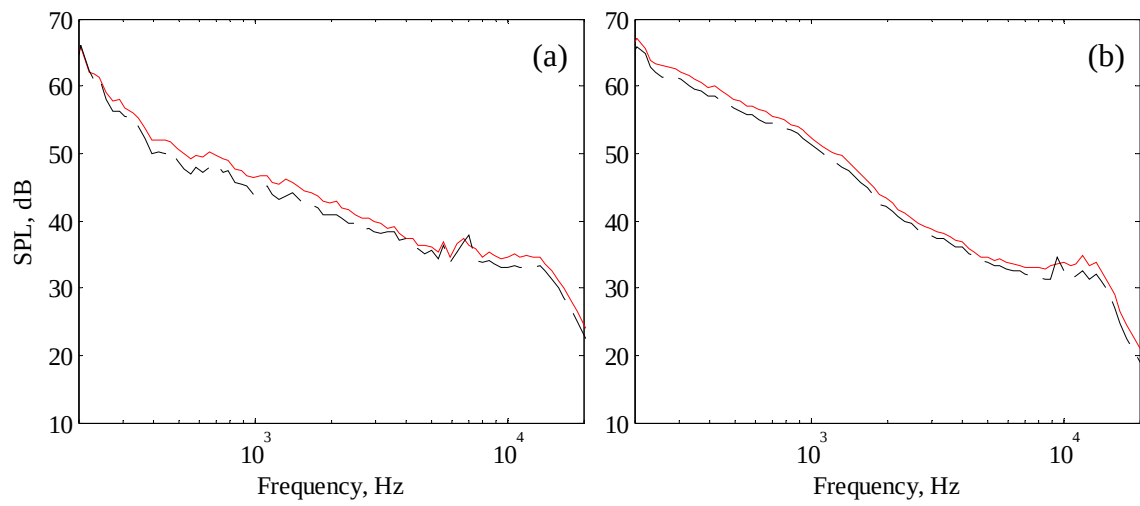


Figure 3-93 SNR of far field sound measured by in-flow microphones for $r/h = 0\%$ at $U_\infty = 40$ m/s. (a) $\theta = 155.1^\circ$ and (b) $\theta = 15.2^\circ$. Legend is same as that for Figure 3-92.

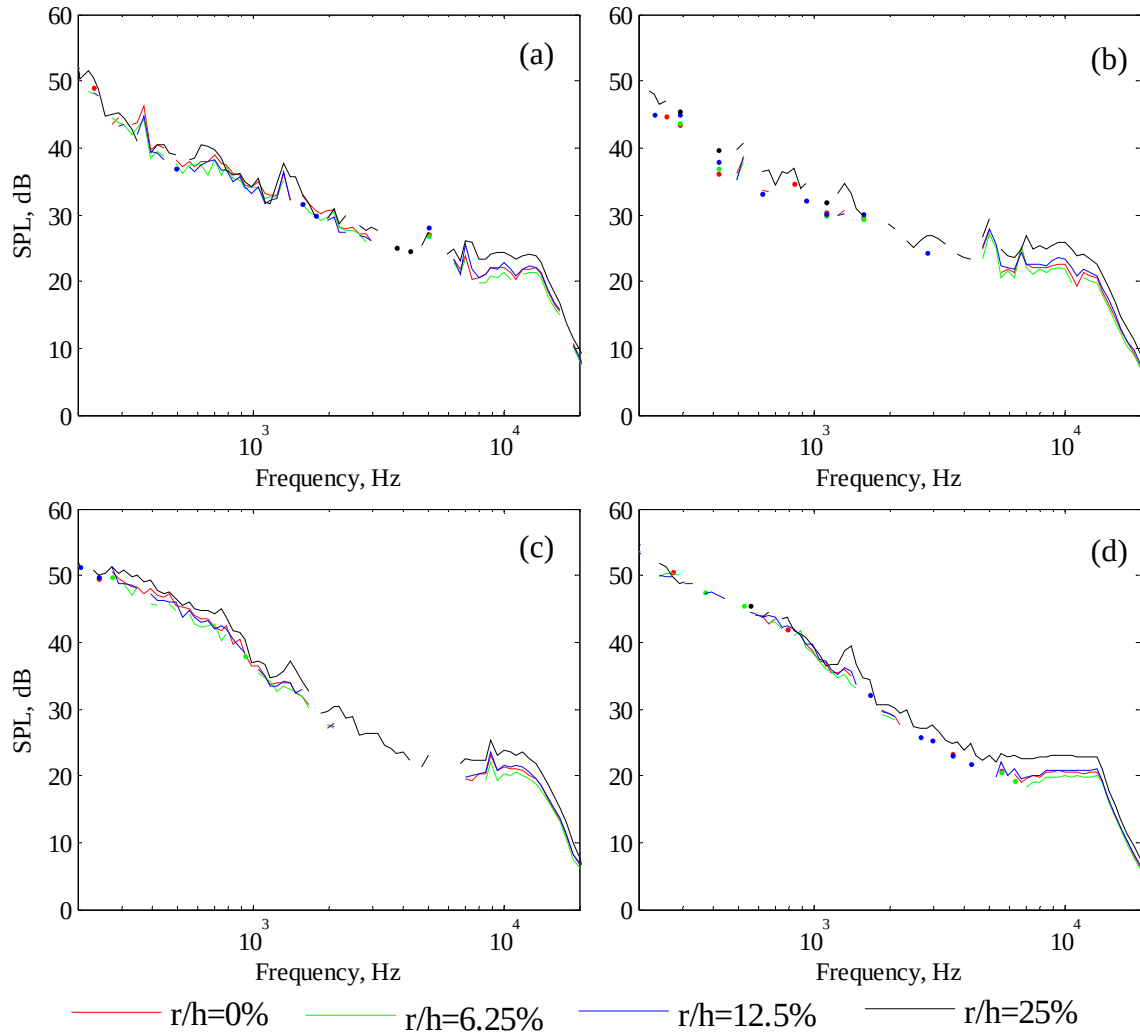


Figure 3-94 Background subtracted sound spectra for rounded forward steps at $U_\infty = 30$ m/s measured by the in-flow microphones at four observer locations. (a) $\theta = 155.1^\circ$, (b) $\theta = 141.9^\circ$, (c) $\theta = 22^\circ$ and (d) $\theta = 15.2^\circ$.

3.2.6 Far Field Sound from Rounded Forward Steps using Phased Microphone Array

The far field sound from the 117 microphone phased array was measured by deploying it out of the flow region in the port side anechoic chamber as described in §3.1.6. The details regarding the beamforming procedure, corrections applied to the data and deconvolution method used are also provided in that section. The phased array measurements were made for all four rounded forward steps at velocities between $U_\infty = 20$ m/s and 60 m/s with increments of 5 m/s. The observer location for the phased array measurement is the geometric center of the array and remains unchanged for each case. Figure 3-95 shows the observer location for phased array measurement of far field sound from rounded forward steps. Note that the definition of $R - \theta$ coordinates is the same used for far field sound measurements in the wall-jet facility (see §2.1.6.1) and measurements using in-flow microphones (see §3.2.5). As shown in the figure the observer for phased array measurements is located 2.54 ± 0.01 m from the inside corner of each forward step and the forms and angle of $95.6^\circ \pm 2^\circ$ with respect to the vector aligned with the streamwise velocity of the flow.

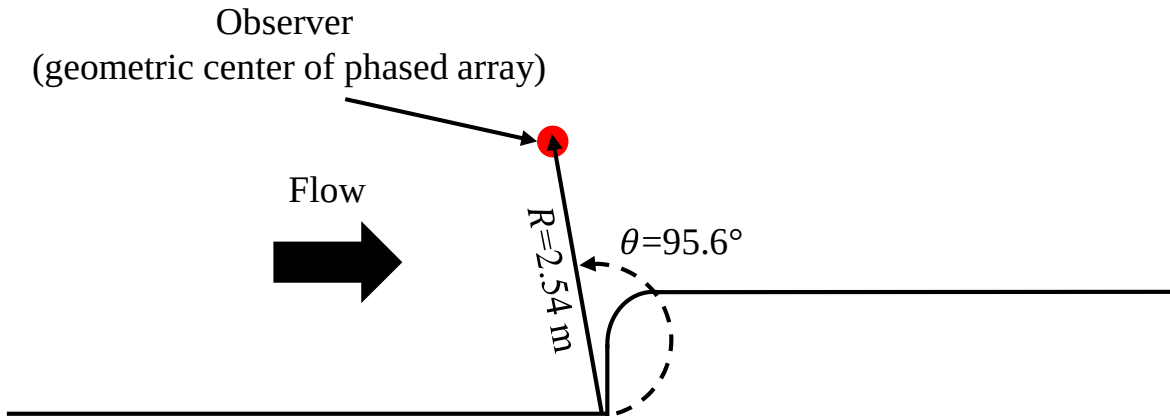


Figure 3-95 Observer location for phased array measurement of far field sound from rounded forward steps.

3.2.6.1 Facility Noise Levels

Before we discuss the results from the phased array measurements for surface discontinuities let us first look at the sound maps generated using beamforming technique for an undisturbed boundary layer flow. This will help us to assess the aeroacoustic character of the false wall configuration of Virginia Tech Stability Tunnel. Figure 3-96 (a) and (b) show the contours of sound levels (in dB, $p_{ref} = 20 \mu\text{Pa}$) at a fixed frequency of 2000 Hz across the entire false for undisturbed boundary layer flow at $U_\infty = 30 \text{ m/s}$ and 60 m/s respectively. These maps were obtained using the conventional delay and sum beamforming procedure described in §3.1.6 which involves mathematically steering the array output to different parts of the false wall. The contour levels are shown up to 10 dB below the maximum sound level yielded by the beamforming output. Note that the contour scales (shown in the figure below each plot) for each plot shown in this particular figure are different. The abscissa and ordinate of Figure 3-96 represent the streamwise and spanwise coordinates of the false wall respectively. The origin of the streamwise coordinate ($X = 0$) is at the streamwise location where the vertical face of the rounded forward steps was installed and the origin of the spanwise coordinate ($Y = 0$) is located at the mid-height of the test section. The wall-normal distance for which beamforming is performed is such that the scanning plane (the distance normal to the array used in the steering vector) lies in the plane of the false wall. Throughout our discussion of the results from phased array measurements this coordinate system will be followed.

Consider first the sound map at $U_\infty = 30 \text{ m/s}$ shown in Figure 3-96 (a). In this map, the flow direction is from left to right and we see that around the test section-contraction junction (around $X = -3 \text{ m}$ mark) the maps show the presence of intense sound with maximum value of around 30 dB. This sound is due to the facility noise being reflected by the contraction area which is a hard wall surface. The beamformed map also shows a presence of sound source between $X = -2 \text{ m}$ and -1.5 m which is believed to be due to the in-flow microphone stands. These stands (described in §3.1.7) though streamlined using a NACA 0012 airfoil profile to reduce noise can still generate some self-noise. There are two stands present upstream of the step whose projected distance in the false wall plane is approximately $X = -1.7$ and 1.5 m which is the region where the beamformed map shows a presence of sound source. Finally, far downstream towards the trailing edge of the test section ($X > 2 \text{ m}$) the map shows presence of sound which is due to the reflections from the hard-wall surface of the diffuser section of the facility. Despite of the noise being generated by the facility and instrumentation, the area of interest i.e. where surface discontinuities are installed ($X = 0 \text{ m}$ for forward steps and $0 \leq X \leq 0.9 \text{ m}$ for circular embossment) is generally quiet and the noise in this region is 10 dB or less than the facility noise. As the velocity increases to $U_\infty = 60 \text{ m/s}$ (Figure 3-96 (b)) we see the background noise intensifying with maximum sound levels in the map exceeding those seen for $U_\infty = 30 \text{ m/s}$ by about 15 dB. However, the region where the surface discontinuities are installed is still seen to contain noise levels that are 10 dB or lower compared to the facility noise. These results

shows that the facility is well suited for measurements of boundary layer noise as long as the test artifact is kept sufficiently away from the edges of the test section or other surfaces that protrude in the flow.

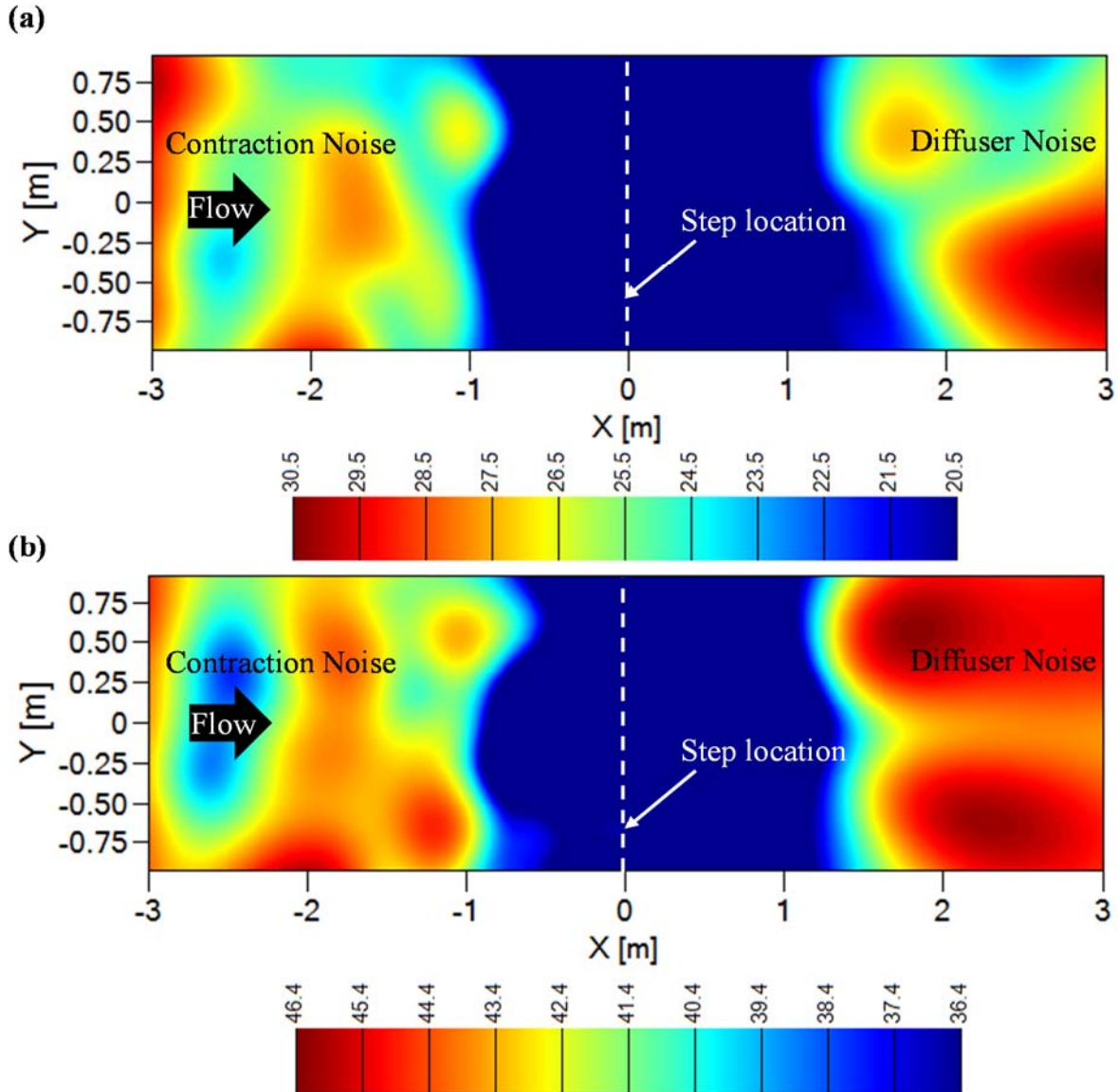


Figure 3-96 Beamformed sound maps at 2000 Hz for undisturbed boundary layer flow. (a) $U_{\infty} = 30$ m/s and (b) $U_{\infty} = 60$ m/s. Note the contour scales for sound levels (in dB, $p_{ref} = 20 \mu\text{Pa}$) are different for both plots and have been shown below each map.

To further assess the consistency of beamforming results and the aeroacoustic quality of the facility consider now beamformed sound maps at 2000 Hz frequency for undisturbed flow at four other free stream velocities as shown in Figure 3-97. These sound maps have been plotted so that the contour scale is fixed by the sound levels produced at the lowest velocity ($U_{\infty} = 40$ m/s) considered in these plots. The color scale corresponding to sound levels is shown at the bottom left of the figure. Locking the contour scale in this manner shows us the effect of increasing the flow speed on the beamforming results and the noise being produced in the area of interest. Looking at the figure it can be seen that increasing the velocity of the flow has expected outcome in that the sound sources near the leading and trailing edge of the test section intensify. Although not apparent in these plots due to contour scale locking, the maximum sound levels

(which occur near the leading and trailing edge of the test section) in the maps for highest velocity can exceed those for the lowest velocity by more than 6 dB. However, the sound in the region of interest still remains around the values being produced at the lowest velocity depicted in this figure. This shows that the region of the wall used to install discontinuities on is the ideal location in terms of sound measurements.

3.2.6.2 Sound Source Maps Obtained from Deconvolution (CLEAN-er) Procedure

The beamformed sound source maps shown in the previous section are simply obtained from the array output and as explained in §3.1.6.1 and contain the effects of spatial aliasing (side lobes) due to the discrete nature of the array as well as contamination due to facility noise. These features pose a problem in correct identification of the actual noise source and their removal or minimization is necessary to obtain accurate results. As pointed out in 3.1.6.3, in present work, an approach known as CLEAN-er which is part of an unpublished work by Dr. Patricio Ravetta of AVEC Inc. was used to remove the undesired noise from the beamformed maps. This approach which is based on CLEAN-SC deconvolution algorithm is derived from the work of Sijtsma (2007) and involves selecting a region of interest in the flow field outside which any sources present are rejected. The purpose of this section is to present and compare the results from this approach to the conventional beamforming sound maps presented in previous section. In the discussion to follow in this section and subsequent sections on beamforming results the term *deconvolution* refers to CLEAN-er algorithm used to remove undesired noise from the sound source maps.

The region of interest which was selected for the application of deconvolution algorithm for rounded forward steps was $-0.6 \text{ m} \leq X \leq 0.6 \text{ m}$ in the streamwise direction and $-0.75 \text{ m} \leq Y \leq 0.75 \text{ m}$ in the spanwise direction. The choice of this region was motivated by the consideration that this region should be sufficiently away from the boundaries of the facility (i.e. floor/ceiling and entrance/exit of the test section) yet should include region where actual sound sources from surface discontinuities may be present. For forward steps the selected region extends over 23 step height upstream and downstream of the vertical face of the step and excludes the region where the trailing edge transition (see §3.1.1 for a description of this trailing edge transition) was installed. In case of the wall mounted circular embossment, the deconvolution region was selected to be between $-0.6 \text{ m} \leq X \leq 1.2 \text{ m}$ and $-0.75 \text{ m} \leq Y \leq 0.75 \text{ m}$. The downstream extent of this region is larger than that for 2D steps because the trailing edge of the circular embossment is located at $X = 0.91 \text{ m}$ and so it must be within the deconvolution region. It was ensured that the choice of the selected region did not affect the far field sound results by varying the streamwise and spanwise boundaries of the region of interest and observing the results. It was found that for both the forward steps and circular embossment increasing the width of deconvolution region in either streamwise or spanwise direction had no impact on the far field sound spectrum.

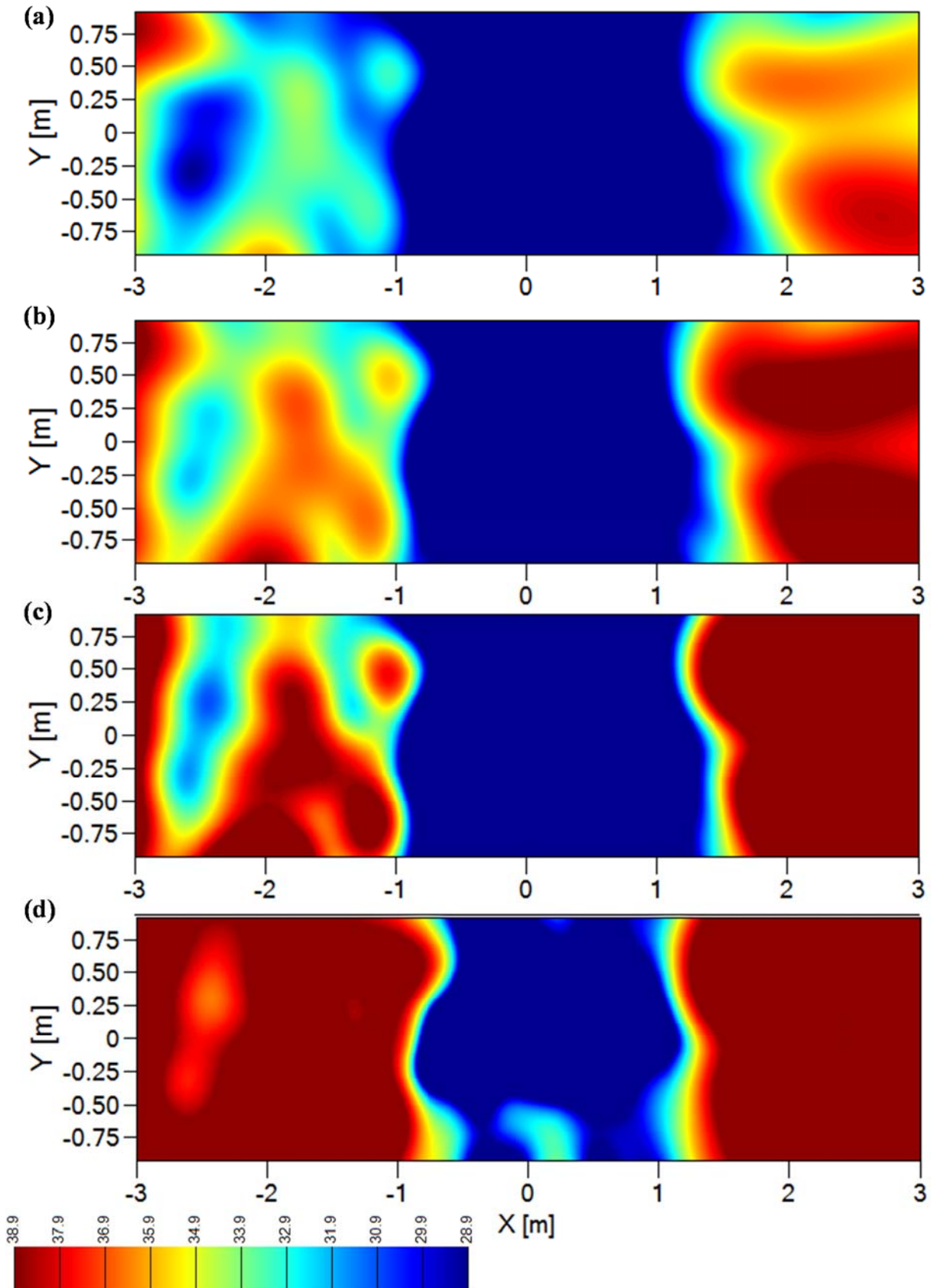


Figure 3-97 Beamformed sound maps at 2000 Hz for undisturbed boundary layer flow with the contour scale locked. (a) $U_\infty = 40$ m/s, (b) $U_\infty = 45$ m/s, (c) $U_\infty = 50$ m/s and (d) $U_\infty = 55$ m/s. The color scheme corresponding to the sound levels (in dB) has been shown at the bottom left.

3.2.6.3 Sound Source Maps for Rounded Forward Steps

The sound source maps at a frequency of 2000 Hz for $r/h = 0\%$ step at two different free stream velocities are shown in Figure 3-98. In this section we will only be showing the source maps at 2,000 Hz since the array bandwidth (spot size of the array PSF) below this frequency is quite large making it difficult to appreciate the quality of results. The effect of this large spot size on the source maps below 2,000 Hz will be considered in detail in the next section. The figure shows both the beamformed and the post deconvolution sound source maps for 30 m/s and 60 m/s. The vertical dashed line on each map shows the location of the vertical face of the step. Looking at Figure 3-98 (a) which shows the beamformed map at 30 m/s velocity the presence of forward step can be clearly seen. The noise from the step is spread in a vertical lobe about 0.7 m wide in the streamwise direction and with maximum sound occurring around the location of the step face ($X = 0$ m). Near the floor and floor/ceiling test section junction the sound levels are slightly lower due to the presence of curved fairing (see §3.1.1 for a description of these fairings) used to minimize the junction noise. It can also be seen that the sound map is uniform along the span of the step due to two-dimensionality of the flow. Also seen in this source map is the facility noise due to the contraction/diffuser section of the tunnel and the noise due to the in-flow microphone stands which was also seen in the undisturbed boundary layer flow source maps (Figure 3-96 (a)) at the same frequency and free stream velocity. Comparing now the portion of the false wall where the effect of step is seen to the undisturbed boundary layer source map one can see that the sound due to the step can exceed the undisturbed levels by more than 10 dB. The post deconvolution source map for $U_\infty = 30$ m/s shown in Figure 3-98 (b) removes the extraneous facility noise from the map improving the SNR of the beamformed map. Figure 3-98 (c) and (d) show the beamformed and de-convolved source maps respectively at $U_\infty = 60$ m/s for 2000 Hz frequency. These maps show similar characteristics as those for 30 m/s case and the deconvolution procedure is seen to improve the quality of the beamformed source maps. Note that from here on all the source maps shown in this text will be those obtained after deconvolution procedure unless otherwise stated.

The effect of increasing the velocity on the sound source maps for $r/h = 0\%$ step is shown in Figure 3-99. This figure shows the post deconvolution maps at 2000 Hz for velocities between 40 m/s and 55 m/s with increments of 5 m/s. The contour color scale (shown to the right) has been locked to the scale for the highest velocity ($U_\infty = 55$ m/s) so that the change in sound levels with changing speed are apparent. Looking at the figure it can be seen that the sound source maps clearly show that increasing the velocity leads to consistent increase in sound levels. The maximum sound levels in each of these maps are 41.3 dB, 44.3 dB, 47.1 dB and 49.5 dB for 40 m/s, 45 m/s, 50 m/s and 55 m/s respectively. This corresponds to an increment of 3 dB, 2.9 dB and 2.4 dB with velocity increasing every 5 m/s past 40 m/s. If we now consider the $U_\infty = 40$ m/s as our baseline case and recall the 5th power of velocity scaling from our wall-jet far field sound measurements in §2.2.4, the predicted increase for sound levels are approximately 2.5 dB, 2.3 and 2.1 dB for 45 m/s, 50 m/s and 55 m/s respectively. These values are slightly lower than those indicated by the source maps in Figure 3-99.

The sound source maps shown so far were for a single frequency of 2000 Hz only. Similar maps exist at multiple different frequencies which can be used to generate a sound spectrum for a particular test case. But, before we consider the sound spectrum it is useful to analyze the quality and behavior of the source maps at additional frequencies. Figure 3-100 shows the source maps measured at six different frequencies for flow over $r/h = 0\%$ forward step at $U_\infty = 60$ m/s. To accommodate these maps on a single figure the streamwise spatial scale of the maps has been compressed in these plots so that only the portion of the false wall between $X = -1$ m and $X = 1$ m is shown on each plot. The maximum and minimum sound levels within the map on each of these plots is different but the resolution (change in color scheme with per dB sound reduction) and the range (10 dB down from maximum level in the map) has been kept consistent with the source maps discussed previously. The color bar shown at the bottom of the figure indicates this color scheme with red showing the maximum sound level and blue showing the minimum sound level within each plot. The maximum and minimum sound levels within the each plot have been indicated within

the legend on the top of each map along with the frequency associated with that map. Consider the map at $f = 5000$ Hz (top left of the figure) first, this map shows the clear presence of sound from the forward step with red contours showing the maximum SPL of 35 dB and blue contours showing sound levels of 25 dB or below. As the frequency increases to 7100 Hz and 8000 Hz the step sound can also be clearly seen separated from the rest of the background. However, at 9000 Hz frequency some noisy spots begin to develop around the lobe showing the step noise and the population of these spots increases as the frequency is further increased to 11,200 Hz and 13,200 Hz. The reason for this noise in high frequency sound source maps is the reduction in array SNR with increasing frequency which was earlier discussed in §3.1.6.1. Recall that as discussed there, the array SNR refers to the ratio between the power of the main lobe and the side lobe (which is present due to spatial aliasing) and it decreases with increasing frequency. Thus, with increasing frequency the separation between the actual noise source and artificial noise due to spatial aliasing is reduced. In fact, if we look closely at the source maps at frequencies of 11,200 Hz and 13,200 Hz we can see that slightly upstream of the step, a structure appears which is reminiscent of the geometry of the array itself. The noise present in these maps therefore is not the actual forward step noise and must be kept in mind during interpretation of array results at high frequencies.

To further consider the effects of array SNR on the quality of results produced by the phased array let us look at the source maps for $r/h = 0\%$ at $U_\infty = 30$ m/s as shown in Figure 3-101 for different frequencies. The layout of the figure and the frequencies being considered are the same as Figure 3-100 discussed above. This figure shows that the array noise becomes a factor at lower frequencies compared to that for $U_\infty = 60$ m/s. The extraneous noise becomes apparent in these maps around 8000 Hz whereas for the higher velocity case it is not significant until frequencies higher than 9000 Hz. The reason for this is that the source strength at higher frequencies becomes lower as the velocity is lowered which leads to appearance of spurious noise sources in the map due to low SNR. For instance, comparing the maps at 8000 Hz at $U_\infty = 30$ m/s to those at $U_\infty = 60$ m/s in Figure 3-100 we see that the signal strength in dB is almost halved in the present case. Thus, lower the velocity, lower is the upper limit of the frequency up to which the actual noise sources can be resolved. It is important to consider the quality of the sound source maps because as will be seen shortly the maps at each frequency are integrated to yield the sound spectrum of the far field sound. Therefore, one must carefully analyze each map at different frequencies to ensure that the integration being performed at each frequency includes only the actual noise source and not any spurious noise present in the source maps.

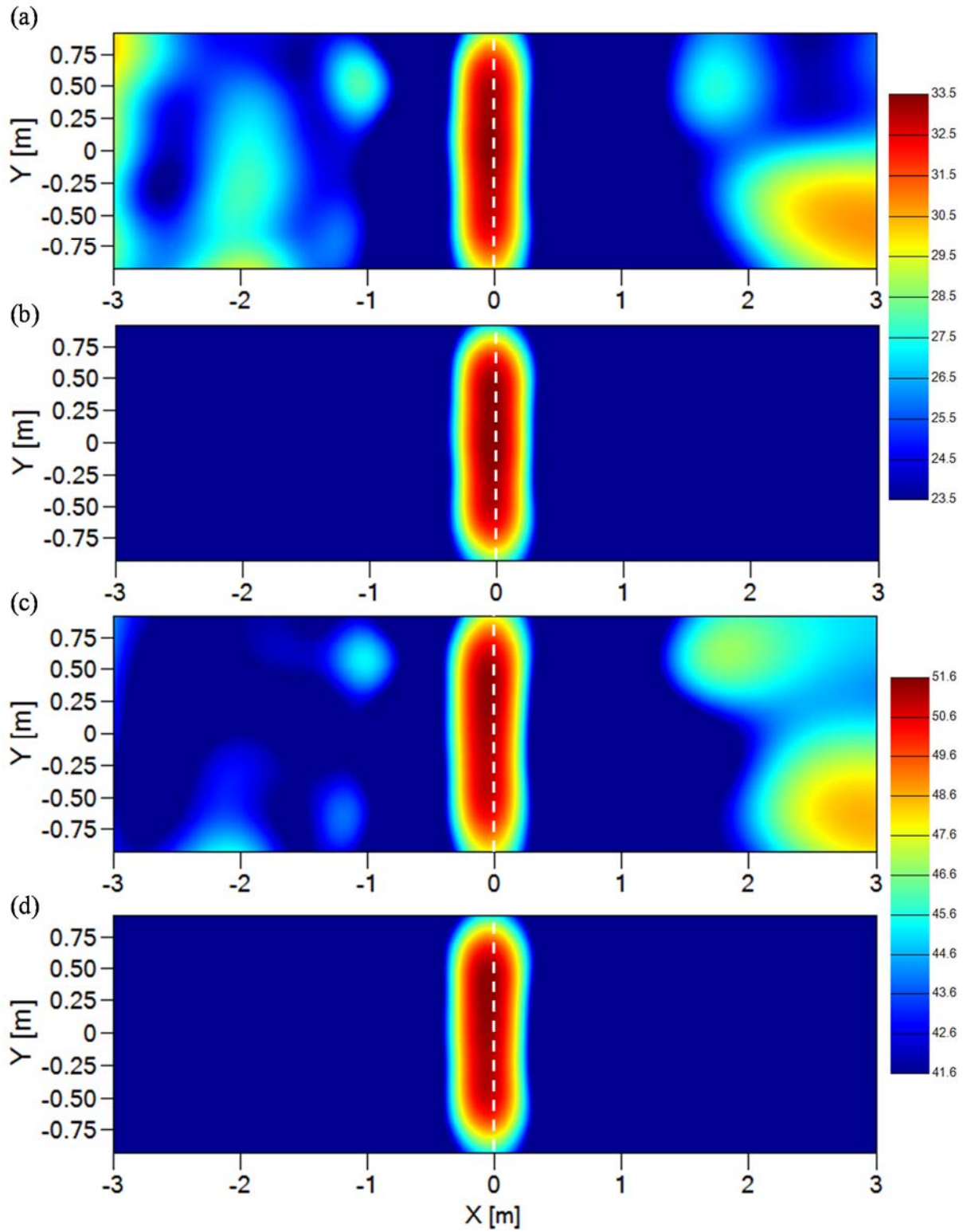


Figure 3-98 Sound source maps at 2000 Hz frequency for flow over $r/h = 0\%$ step at $U_\infty = 30$ m/s: (a) Beamformed, (b) Deconvolved, and, at $U_\infty = 60$ m/s: (c) Beamformed, (d) Deconvolved.

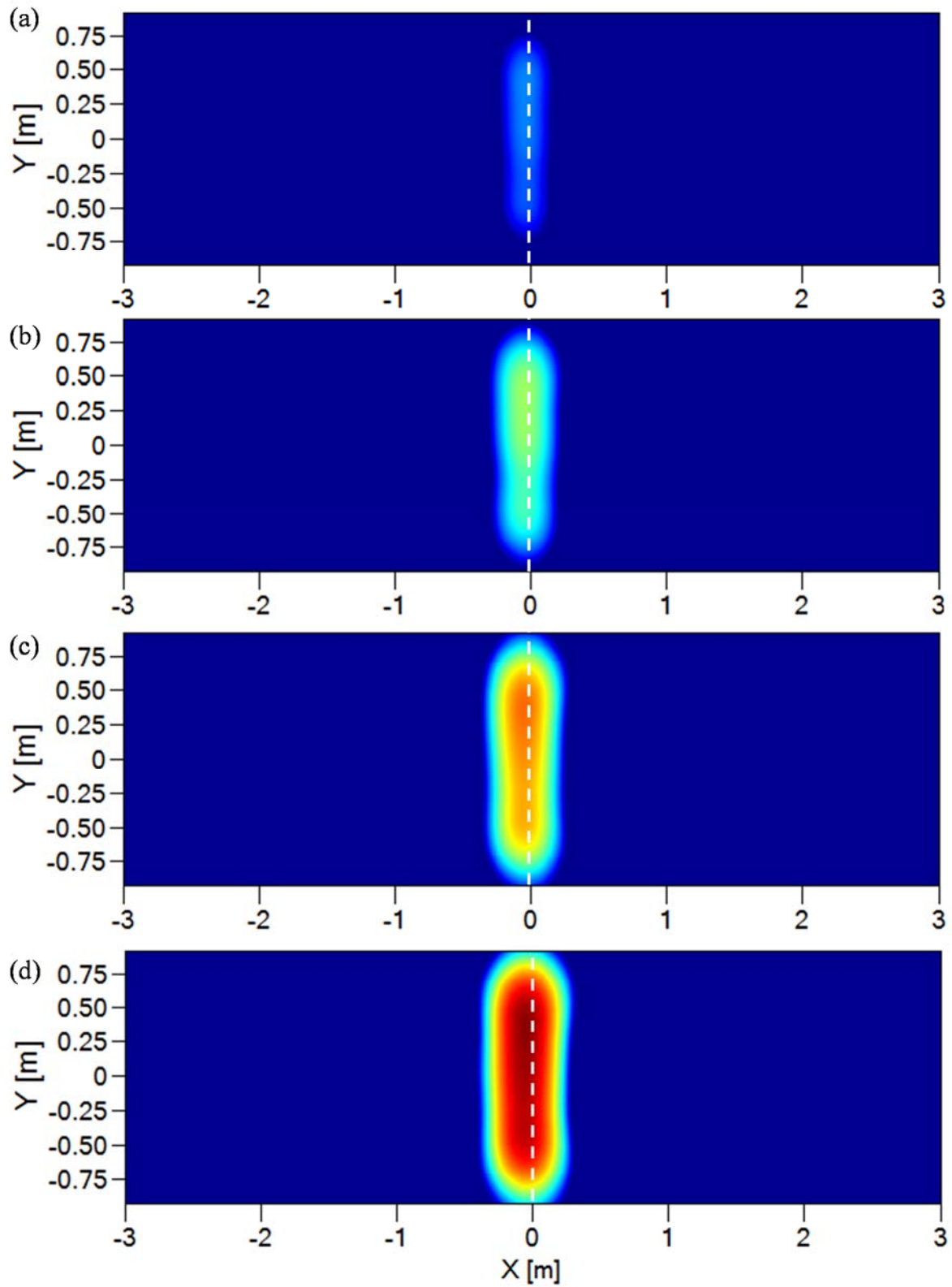


Figure 3-99 Sound source maps at 2000 Hz frequency after deconvolution procedure for $r/h = 0\%$ forward step at four different free stream velocities of (a) $U_\infty = 40$ m/s, (b) $U_\infty = 45$ m/s, $U_\infty = 50$ m/s and $U_\infty = 55$ m/s.

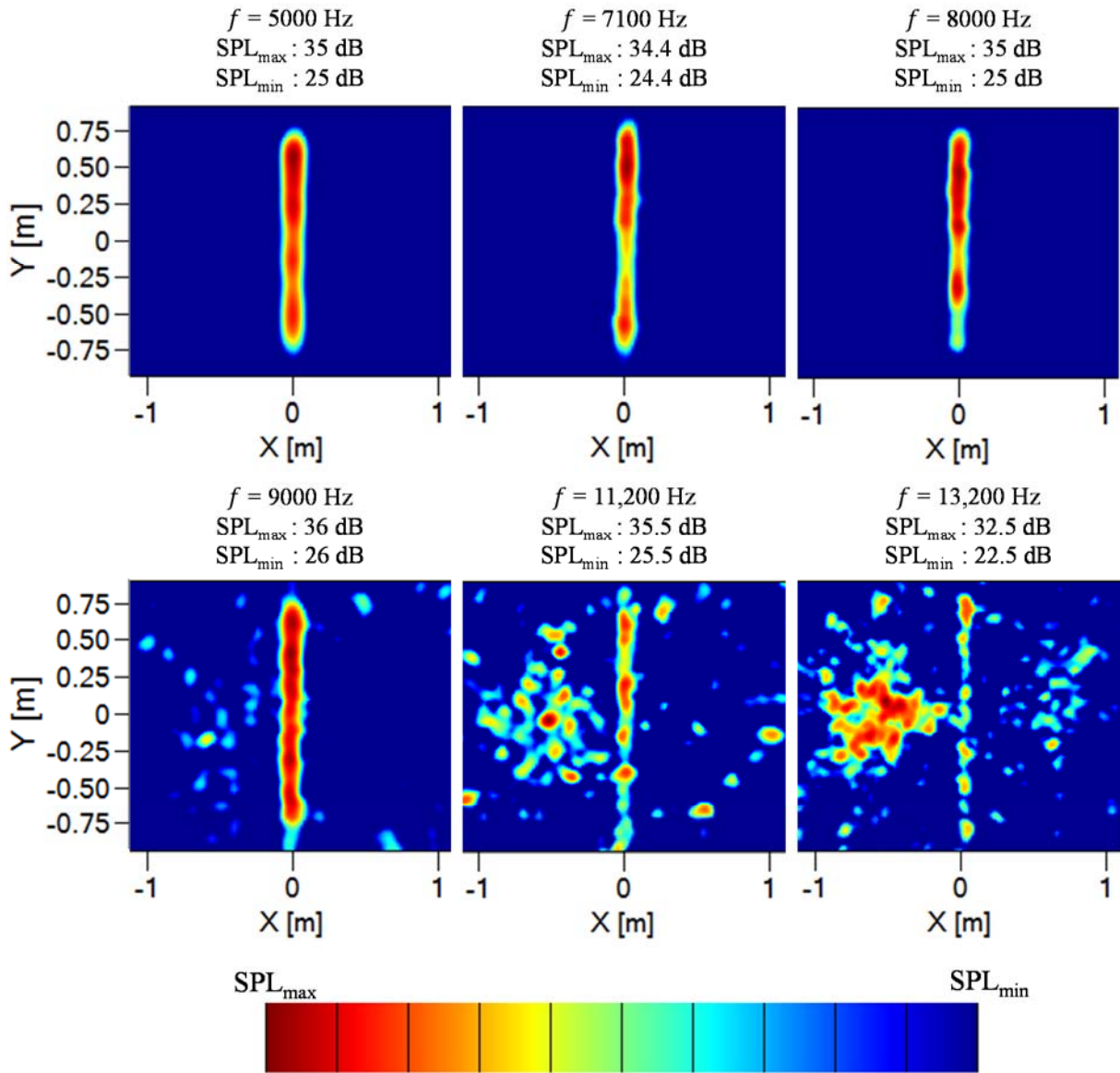


Figure 3-100 Sound source maps at six different frequencies for flow over $r/h = 0\%$ forward step at $U_\infty = 60$ m/s. The contour color scale for each map is different showing minimum sound levels to maximum sound levels (in dB) in decrement of 1 dB. The color scale at the bottom depicts the scheme used to represent these sound levels.

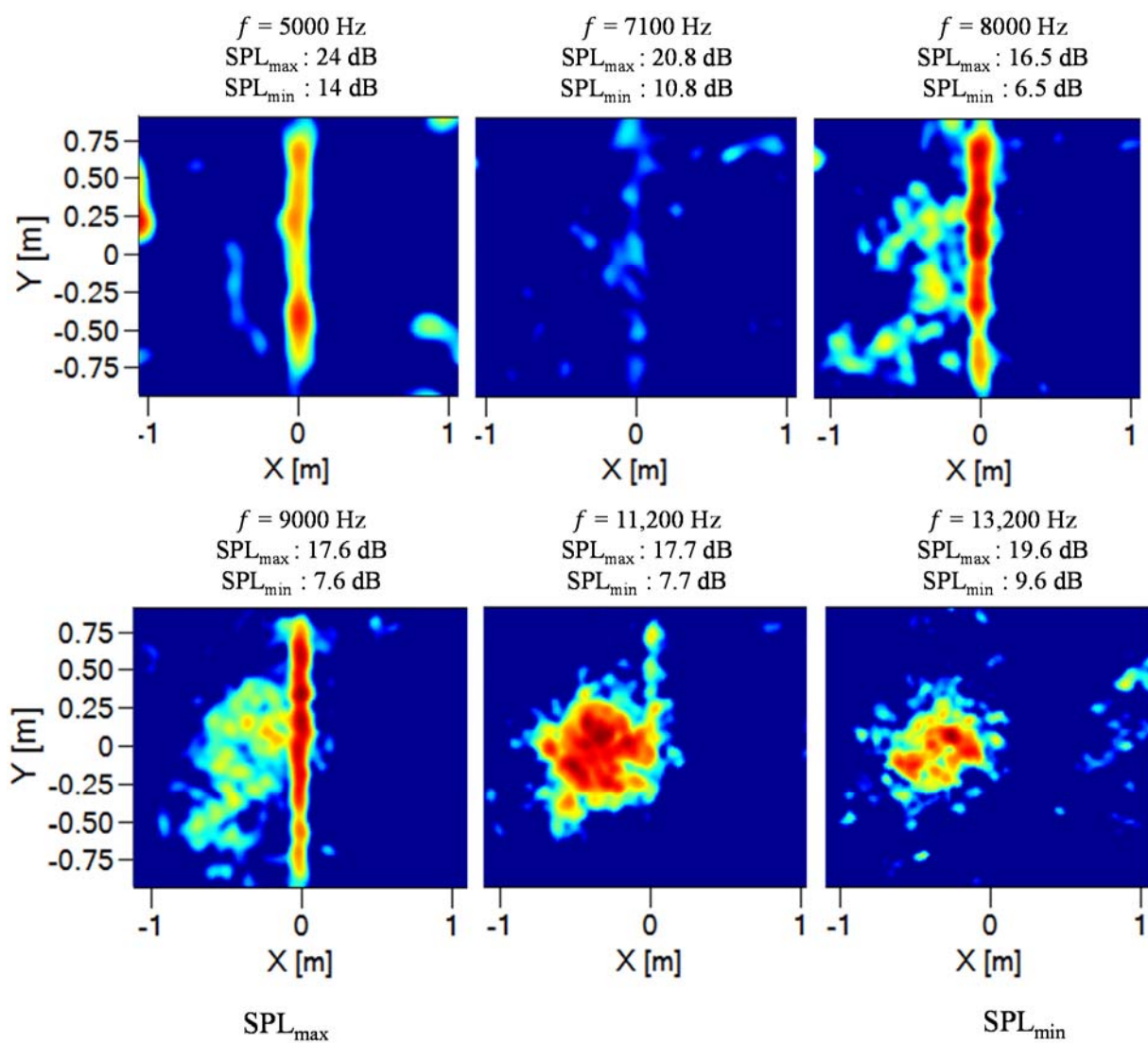


Figure 3-101 Sound source maps at six different frequencies for flow over $r/h = 0\%$ forward step at $U_\infty = 30$ m/s. The frequencies for each map are indicated in the legend above it. The representation of these maps follows the same convention used in Figure 3-100.

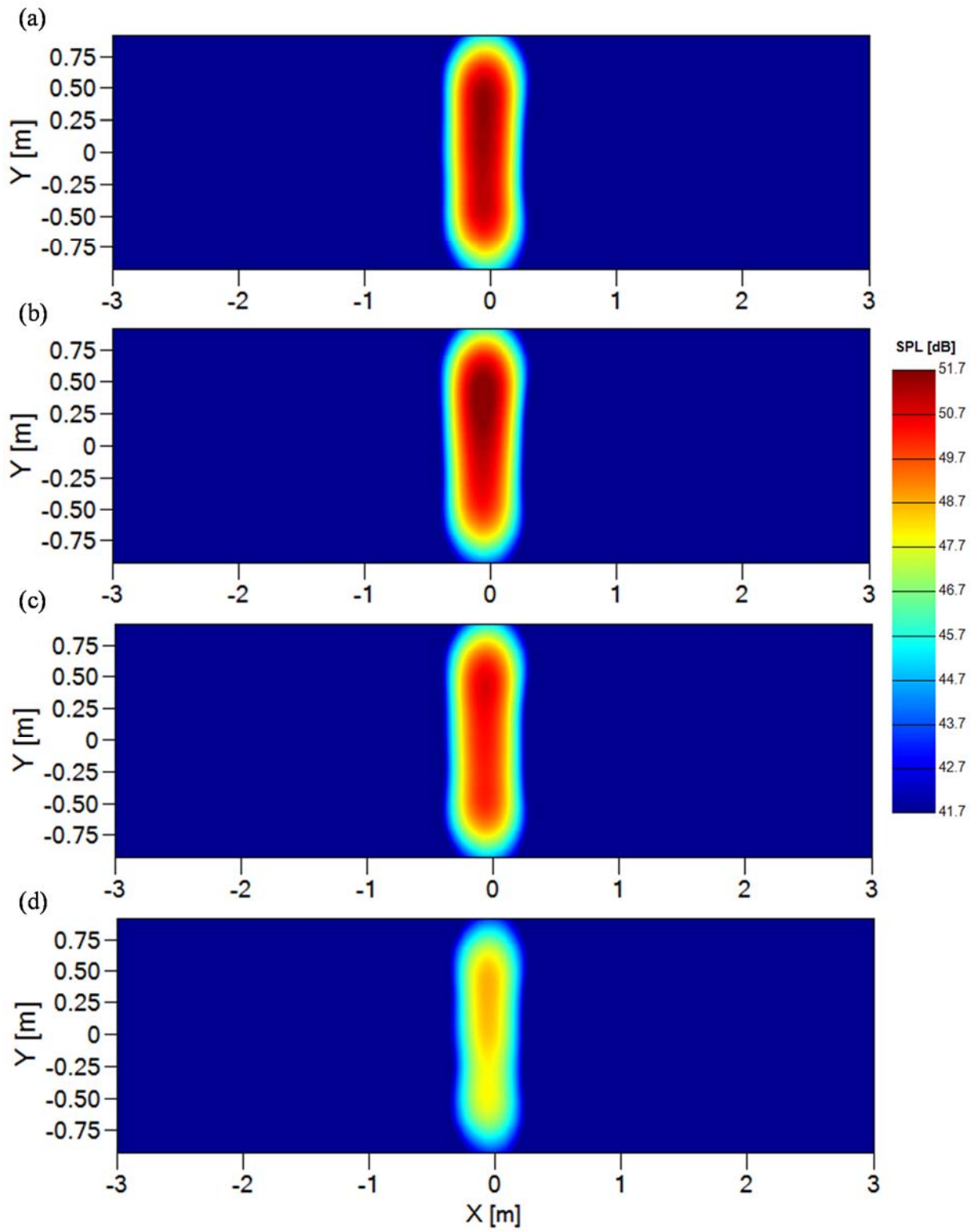


Figure 3-102 Sound source maps at 2000 Hz for flow over rounded forward steps, $U_{\infty} = 60$ m/s. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$ and (d) $r/h = 25\%$.

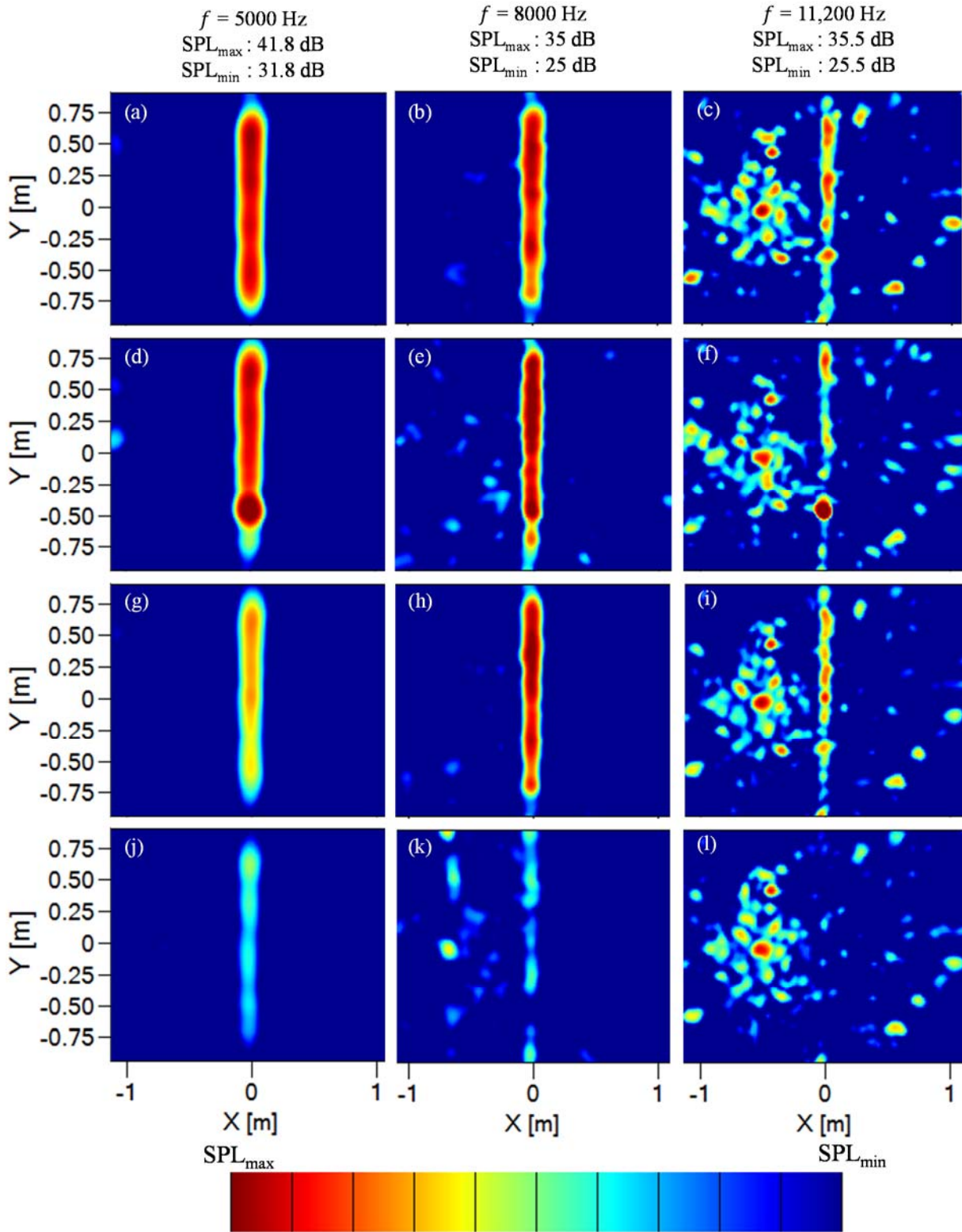


Figure 3-103 Sound source maps at three different frequencies for rounded forward steps at $U_\infty = 60$ m/s. (a)-(c) : $r/h = 0\%$, (d)-(f) : $r/h = 6.25\%$, (g)-(i) : $r/h = 12.5\%$ and (j)-(l) : $r/h = 25\%$. Each column represents a particular frequency which is shown in the legend on top of the column. The contour scales follow the same format as Figure 3-100 and the scale for each column is locked at the scale for $r/h = 0\%$ step at that particular frequency. The maximum and minimum sound levels for the contour scale used in each column are shown in the legend on top of the respective column.

3.2.6.4 Integration of Beamformed Sound Source Maps

The beamformed and de-convolved sound source maps discussed in previous section can be spatially integrated at each frequency to yield the sound spectrum for a particular case. The purpose of this section is to highlight some additional issues specific to the present work that must be accounted for before the final integrated sound spectrum is obtained. The issues which must be addressed during integration of source maps are following

- o Spatial boundaries of the integration region
- o Actual vs spurious noise in source maps
- o Integration cutoff

The spatial boundaries of the integration region must be chosen to satisfy two criteria. Firstly, they should be within the spatial boundaries of the deconvolution region which was chosen in the present work to reject facility noise. Secondly, the boundaries must be sufficiently removed from the test section floor/ceiling-wall junction so that the end effects are not included in the integration. To address both these issues the spanwise extent of the integration region was chosen to be $-0.3 \text{ m} \leq Y \leq 0.3 \text{ m}$. The choice of the streamwise integration region is more complex because the source maps in the previous section showed an array reflection which develops at high frequencies immediately upstream of the step in each case. The frequencies at which this noise structure appears varies with the SNR and is seen to shift to lower frequencies with decreasing velocity or increasing rounding. Therefore, it was important to exclude the region where this noise source occurs at high frequencies but include the same region during low frequency integration since the source maps at those frequencies clearly show presence of actual flow noise. To determine the effect of the choice of integration region on the sound spectrum 8 different regions with the same spanwise width of 0.6 m ($Y = \pm 0.3 \text{ m}$) but different streamwise extent were chosen. The streamwise extent of these regions ranges between $X = \pm 0.1 \text{ m}$ to $X = \pm 0.8 \text{ m}$ with a resolution of 0.1 m. Figure 3-104 shows these integration regions superimposed on the sound source maps at a low and a high frequency for $r/h = 0\%$ at $U_\infty = 60 \text{ m/s}$. Looking at the lower frequency source map ($f = 2,000 \text{ Hz}$, Figure 3-104 (a)) we can see that shrinking the integration region beyond about $X = \pm 0.4 \text{ m}$ can lead to exclusion of the actual step noise source. On the other hand, at a high frequency ($f = 11,200 \text{ Hz}$, Figure 3-104 (a)) including a region upstream of $X = -0.2 \text{ m}$ leads to inclusion of the array reflection in the integration.

Figure 3-105 (a)-(e) shows the integrated sound spectrum in dB ($p_{ref} = 20 \mu\text{Pa}$) obtained using each of the integration regions shown in Figure 3-104 for $r/h = 0\%$ forward step and five different free stream velocities. The integration has been carried out using a cutoff of 5 dB (the implications of integration cutoff will be discussed shortly) and the frequency range shown is the full range used in the beamforming process ($200 \text{ Hz} \leq f \leq 20,000 \text{ Hz}$). Note that the ordinate scale in each plot is kept different to show the absolute effects of integration region selection on the spectrum. Before we begin our discussion on the influence of integration regions on the sound spectra it is important to acknowledge the limitations of the array results. These limitations refer to the frequencies at which the array can resolve the noise from surface discontinuities. The lower frequency resolution is limited due to both the facility noise and the large spot size of the array PSF. On the other hand, the higher frequency resolution is limited due to the low SNR at these frequencies which is attributed to the spatial aliasing effects.

Let us first consider the limitations on the array output at the lowest frequencies used in the beamforming procedure. We pick the case of sound radiated by $r/h = 0\%$ forward step at $U_\infty = 20 \text{ m/s}$ for the sake of this discussion due to low SNR associated with it. Figure 3-106 (a)-(c) shows the beamformed source maps at frequencies of 500 Hz, 1,000 Hz and 1500 Hz for $r/h = 0\%$ forward step and $U_\infty = 20 \text{ m/s}$. Looking at these maps it can be seen that the step noise is not at all apparent in the source map at 500 Hz and therefore the integration at this frequency is meaningless. At a frequency of 1,000 Hz (Figure 3-106 (b)) the step noise represented by the faint lobe centered around $X = 0 \text{ m}$ appears in the beamforming result. However, this frequency is still low enough so that the combined effects of the facility noise and the large spot size of the array PSF (the spot size of the array at 1,000 Hz is about 1m) make it difficult to distinguish

the step noise. As the frequency increases further to 1500 Hz (Figure 3-106 (c)) the step noise becomes clearly visible and separate from the background noise in the beamforming results. Now, consider the results of the deconvolution procedure applied to the beamformed maps of Figure 3-106 as shown in Figure 3-107. Looking at the post deconvolution map for 500 Hz (Figure 3-107(a)) we can see that the procedure does not show any step noise because of its absence in the beamforming results themselves. At 1,000 Hz frequency as shown in Figure 3-107(b) the deconvolution leads to isolation of the step noise from the background noise with a spot that extends about $X = \pm 0.75$ m in the streamwise direction. This map does contain step noise within it but due to severe background contamination of beamforming results seen in the previous figure at this frequency along with the large spot size of the array (the spot size of the PSF is about 1 m at 1,000 Hz for the present array) there is high uncertainty in this map. At the highest frequency of 1500 Hz shown in Figure 3-107 (c) we see that the quality of the deconvolution procedure improves due to less background contamination as shown in the beamforming results. Furthermore, due to the reduced spot size of the PSF the deconvolution map is more focused and is believed to be a more unambiguous representation of the step noise than those at lower frequencies. To avoid high uncertainties in the integrated spectra at lower frequencies each beamformed map and the corresponding deconvolution map was analyzed carefully and it was found that for all the cases being considered presently frequencies only above 1500 Hz could be resolved unambiguously. Therefore, in subsequent spectra obtained from the integration of source maps frequencies below 1500 Hz will not be shown.

While the low frequency end of the array output is contaminated by the facility noise, the high frequency end suffers from the low SNR which is the result of spatial aliasing due to the array being a combination of discrete sensing points. This effect of low SNR was clearly visible in Figure 3-103 and Figure 3-101 which showed visual absence of step noise from source maps at very high frequencies. Therefore, in the subsequent discussion the frequencies at which step noise is not visible in the source maps will be excluded from the integrated spectra. Note that the highest frequency up to which the noise from surface discontinuities is visible depends on both the nature of the discontinuity and the flow velocity due to varying SNR. Therefore, the highest frequency up to which the integrated spectrum will be shown will depend on the particular case being considered. Further note that as seen in the wall-jet measurements the sound spectra from forward step experiences destructive interference at frequencies corresponding to the half wavelength associated with the step height. As will be seen shortly, spectra in the present work also experience the effects of this destructive interference. Therefore, care was taken to ensure that the absence of step noise in source maps due to the destructive interference effects was not incorrectly attributed to the low SNR of the array.

With these limitations on the frequency resolution of the array in mind now consider the effects of using different streamwise integration region on the frequency spectrum of forward step noise. The integrated spectra throughout the discussion will be presented as spectral density of far field sound (in Pa^2/Hz). The frequencies at which data will be presented are in $1/12^{\text{th}}$ octave bands. Figure 3-105 (a)-(e) show the frequency spectrum of sound from an unrounded forward step at five different free stream velocities obtained using eight different integration regions whose streamwise width ranges from 0.2 m ($-0.1 \leq X \leq 0.1$ m) to 1.6 m ($-0.8 \leq X \leq 0.8$ m). The spanwise integration region boundaries for each case is restricted between $-0.3 \text{ m} \leq Y \leq 0.3 \text{ m}$. We see that at lower frequencies where the step noise is not contaminated by the facility noise (between about 1,000 Hz and 3,000 Hz) the choice of the integration region effects the calculated spectrum levels with smaller integration regions showing lower spectral values. This is because at these frequencies the spatial extent of step noise is large as seen in Figure 3-104 (a) and using smaller integration regions does not account for the entire source region. At mid frequencies the spectrum in each case is independent of the integration region width in streamwise direction. However, at higher frequencies the selection of integration region clearly effects the sound spectrum in each case with the sound levels increasing as the integration region is expanded. The reason for this increase is because as shown in Figure 3-104 (b) increasing the streamwise width of the integration region beyond a certain point includes the array reflection present upstream of the step in the integration process.

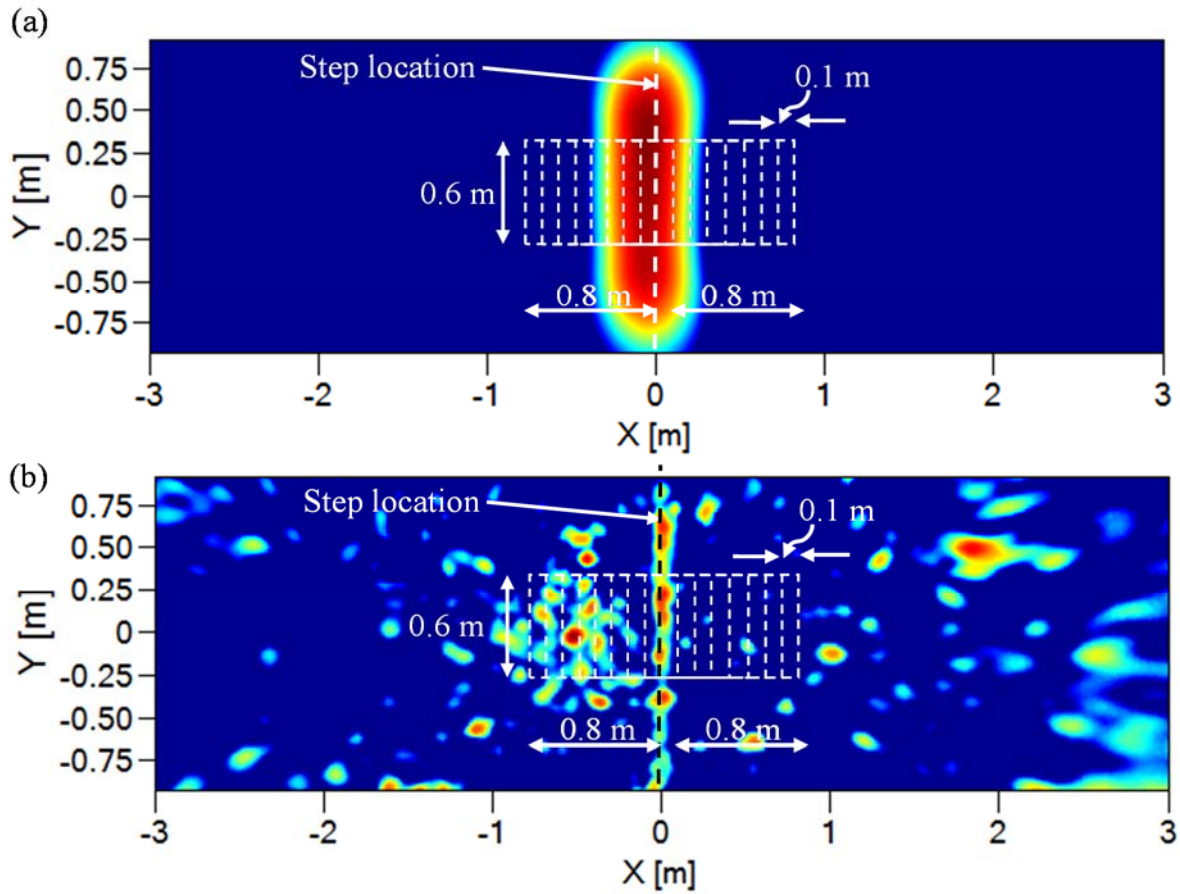
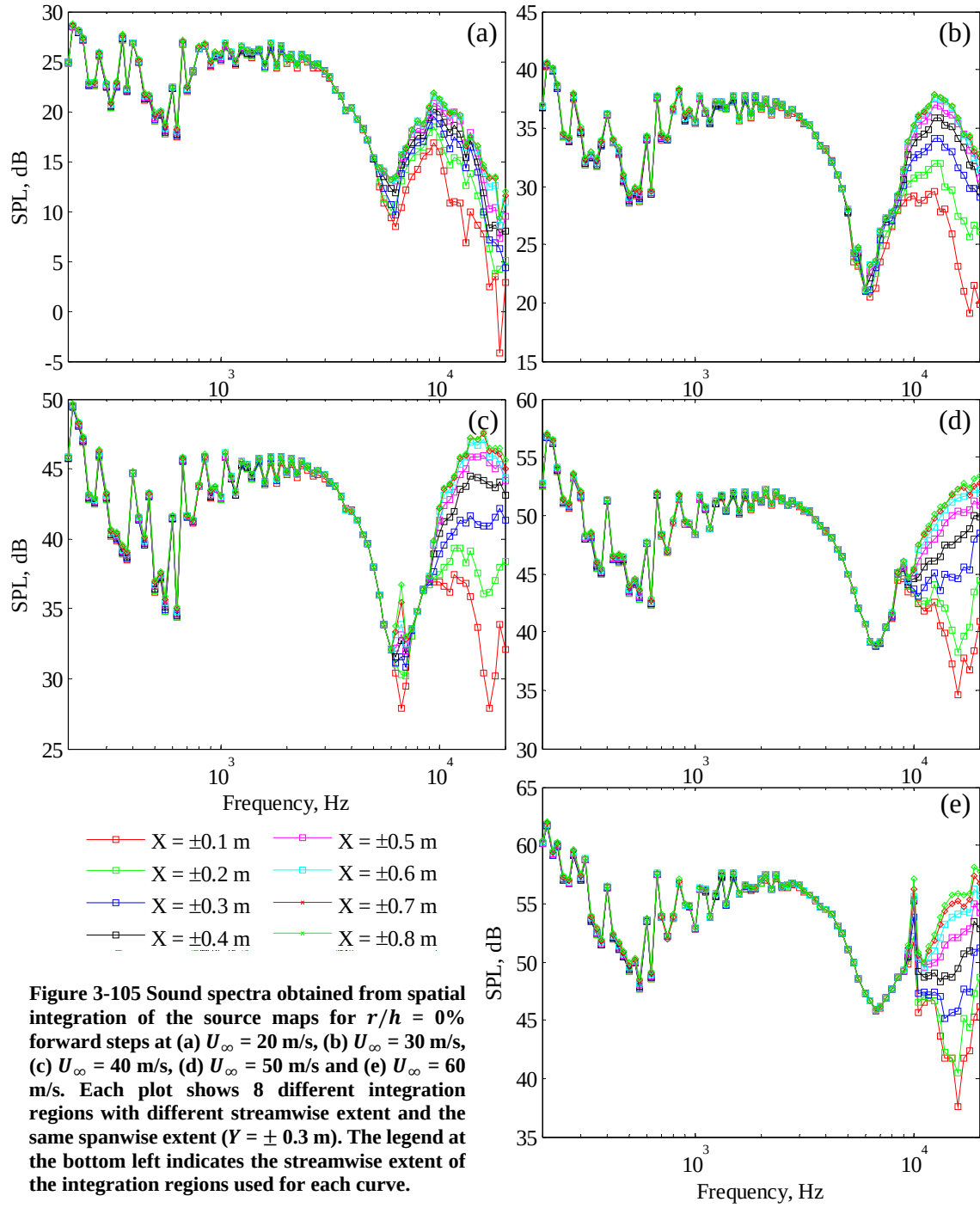


Figure 3-104 Integration regions chosen to establish the streamwise extent of integration regions for forward steps. (a) $f = 2000$ Hz and (b) $f = 11,200$ Hz.



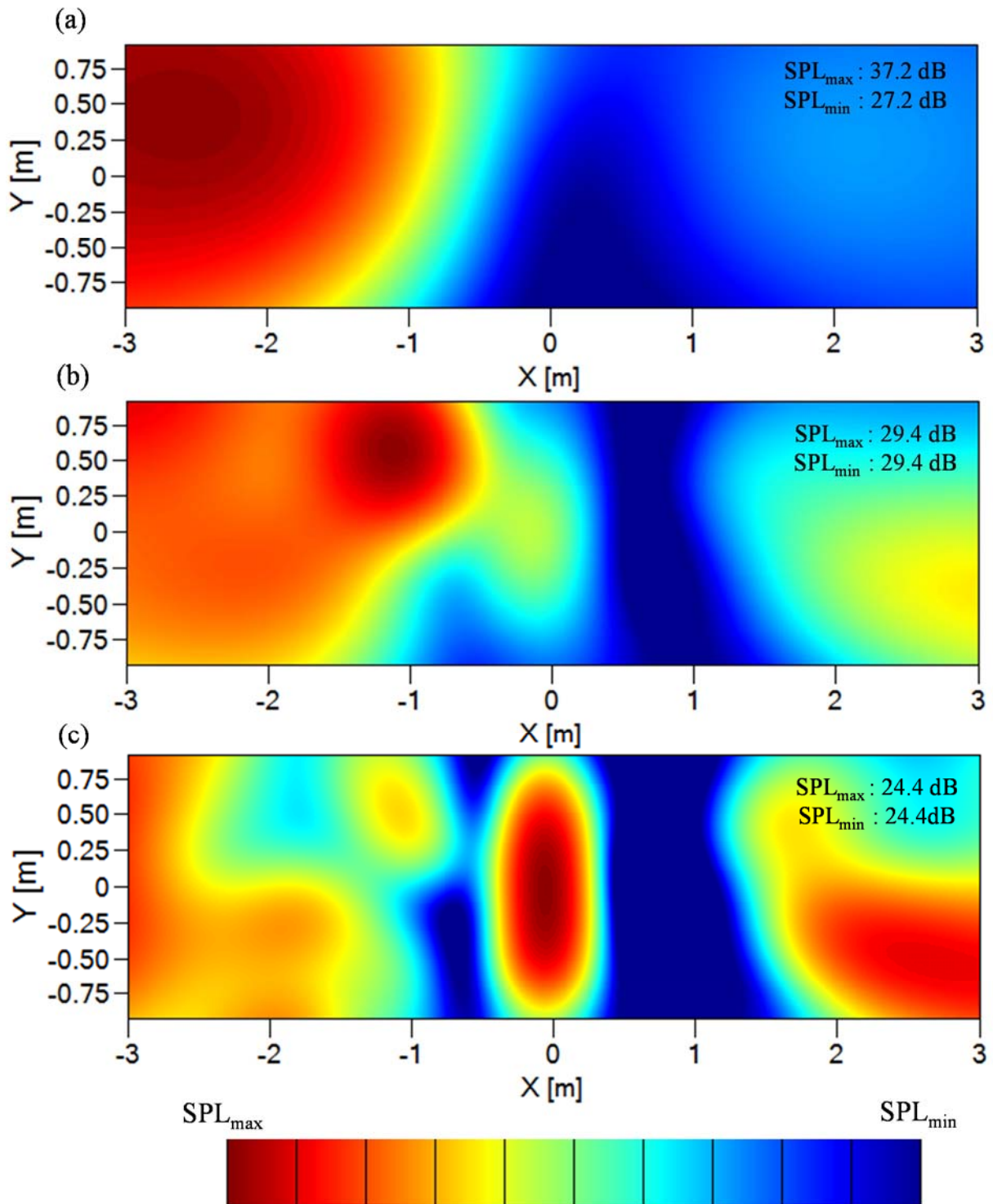


Figure 3-106 Beamformed source maps for $r/h = 0\%$ step at $U_{\infty} = 20$ m/s. (a) $f = 500$ Hz, (b) $f = 1,000$ Hz and (c) $f = 1500$ Hz.

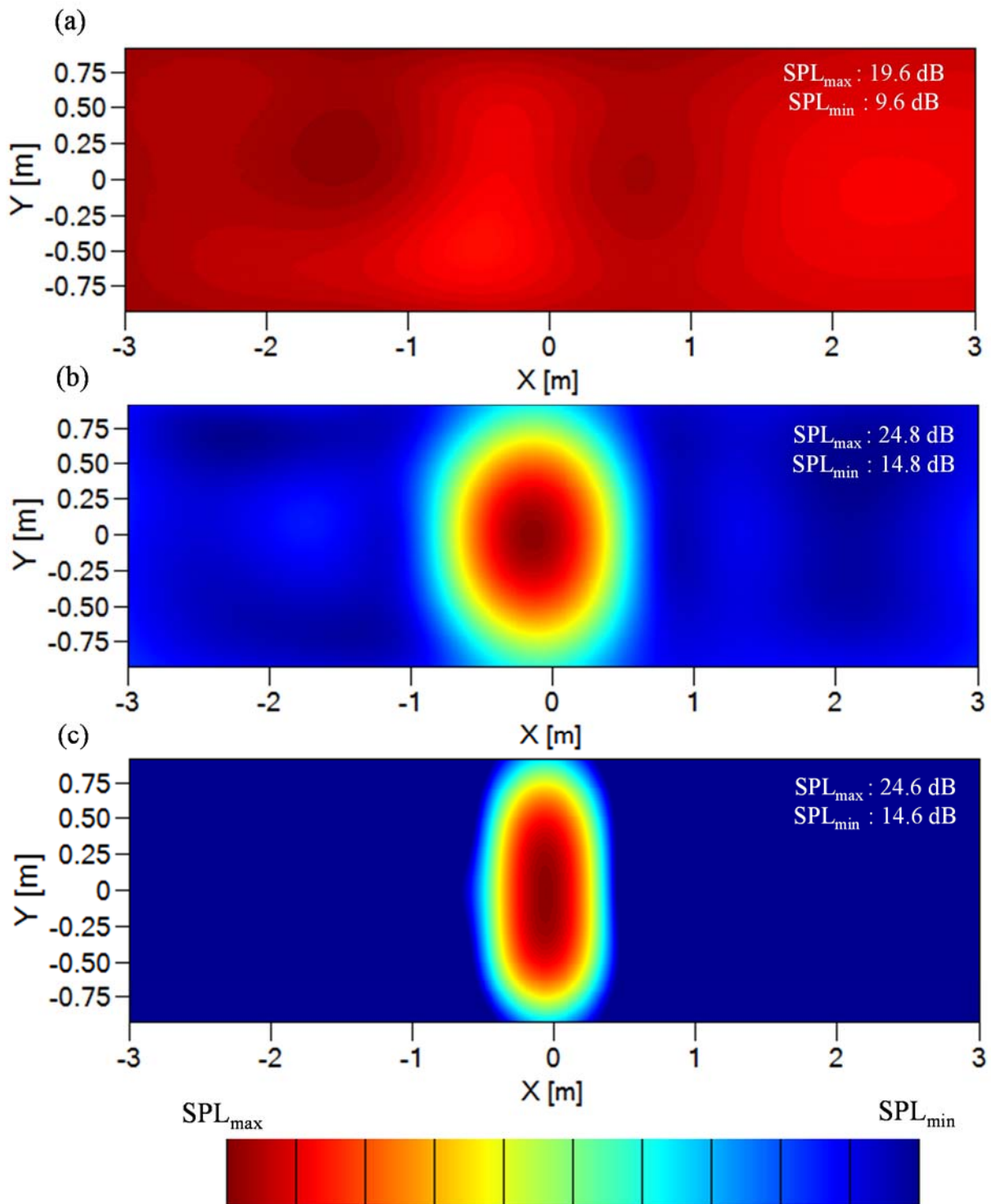


Figure 3-107 Sound source maps of Figure 3-106 after deconvolution. (a) $f = 500$ Hz, (b) $f = 1,000$ Hz and (c) $f = 1500$ Hz.

The results shown in Figure 3-105 suggest that there is no single choice of integration region which will produce the correct sound spectrum associated with the step noise. Therefore, any integration region

chosen will have to be a combination of a separate low frequency region and a high frequency region. Let us first consider the low frequency integration region. Figure 3-108 shows the integrated sound spectra for $r/h = 0\%$ at $U_\infty = 20$ m/s for frequencies between 1,000 Hz and 5,000 Hz. The spectra are plotted so that the plot on the left shows the effect of integration region on the low frequency spectrum for the four narrowest regions (in streamwise direction) and the plot on the right shows the effects of the four widest integration regions selected in the present study. The particular case of $U_\infty = 20$ m/s is chosen because it has the lowest SNR of all the measurements for $r/h = 0\%$ step. Looking at the figure it can be seen that while for the four narrow regions the spectra show a slight variability with changing regions width, the spectra for the four wider regions selected are identical to each other in the low frequency region. In fact, for the wider integration regions, the spectra lie on top of each other so it is difficult to distinguish between them. Although, not shown, this consistency of the integration regions in yielding the low frequency end of the sound spectra for $r/h = 0\%$ step is maintained at all the velocities and all surface discontinuities for which measurements were made in the present study.

The effect of the four widest integration region on the low frequency sound spectra for rounded forward steps at $U_\infty = 20$ m/s is shown in Figure 3-109 (a), (b) and (c) for $r/h = 6.25\%$, 12.5% and 25% respectively. Note that the highest frequency shown for $r/h = 25\%$ case is limited only to about 3,000 Hz because of low SNR for this particular case. Looking at this figure it can be seen that for the two lowest rounding the spectra are independent of the integration region width but for $r/h = 25\%$ they show a slight variability (~ 1 dB). The reason for this variability is that the particular case shown in Figure 3-109 (c) ($r/h = 25\%$, $U_\infty = 20$ m/s) has the lowest SNR of all the measurements made in the present work and the source maps reveal that it is difficult to isolate the step noise source from the facility noise at low frequencies for this case. In fact if we look at the integrated spectra for rounded forward steps at slightly higher velocity of $U_\infty = 30$ m/s as shown in Figure 3-110 we see that the low frequency integrated spectra are nearly identical for each rounded step for different choices of integration region used in the present study.

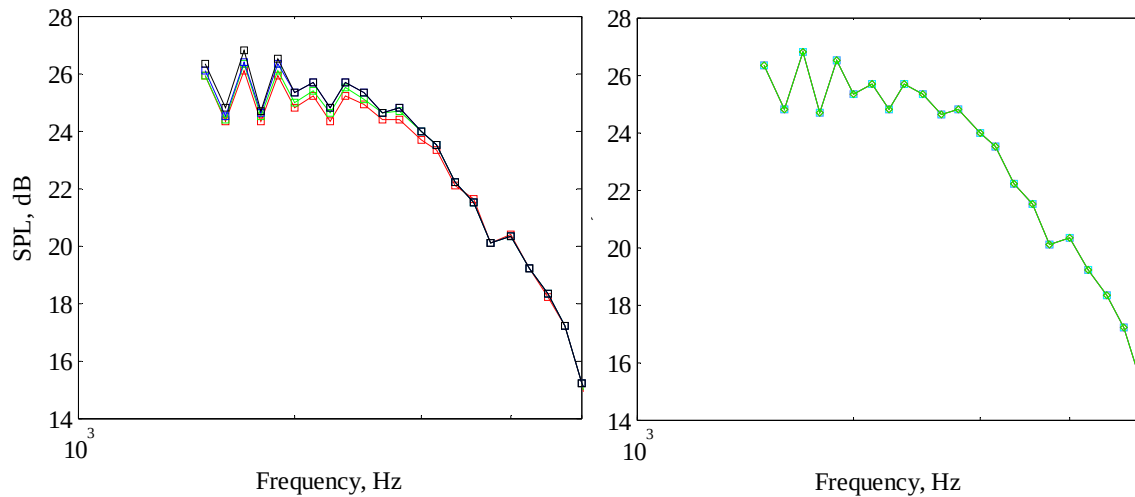


Figure 3-108 Effect of integration region selection on the low frequency end of the sound spectrum from $r/h = 0\%$ at $U_\infty = 20$ m/s. The plot on the left shows the four narrowest integration regions (in streamwise direction) selected and the plot on the right shows the four widest regions. The frequency axis on each plot has been shrunk so that the spectra shown are only between 1,000 Hz and 5,000 Hz. The legend is same as that for Figure 3-105.

The convergence of the low frequency integration region for the rounded forward steps shown in Figure 3-108-Figure 3-110 suggests that we can pick any integration region whose bounds are between 0.5 and 0.8 m upstream and downstream of the step. However, it must be remembered that during the deconvolution procedure to isolate the step noise from facility noise a region whose boundaries were between $-0.6 \text{ m} \leq X \leq 0.6 \text{ m}$ and $-0.75 \text{ m} \leq Y \leq 0.75 \text{ m}$ was used. Therefore, it is prudent that the

integration region selected be such that it is bounded by the deconvolution region in both spanwise and streamwise direction. In the spanwise direction each integration region being studied satisfies this criteria since they all have the same bounds ($-0.3 \text{ m} \leq Y \leq 0.3 \text{ m}$). However, in the streamwise direction the two regions (with bounds $-0.7 \text{ m} \leq X \leq 0.7 \text{ m}$ and $-0.8 \text{ m} \leq X \leq 0.8 \text{ m}$) that do show low frequency convergence do not qualify and therefore cannot be used. Of the remaining two the region with streamwise bounds of $-0.6 \text{ m} \leq X \leq 0.6 \text{ m}$ is at the edge of the deconvolution region and therefore it should not be used due to possible contamination of step noise by the facility noise. Therefore, as the final integration region to be used to obtain the low frequency end of the sound spectra we select the region whose streamwise boundaries are $-0.5 \text{ m} \leq X \leq 0.5 \text{ m}$ and whose spanwise boundaries are $-0.3 \text{ m} \leq Y \leq 0.3 \text{ m}$.

Let us now consider the effect of integration region selection on the high frequency end of the sound spectra. As shown in Figure 3-105 changing the streamwise extent of the integration regions can have a significant impact on the high frequency part of the sound spectra due to inclusion of spurious noise sources. Figure 3-111 shows the high frequency part of the integrated spectra for $r/h = 0\%$ at five different free stream velocities and 8 different choices of integration region. The frequency axis of these plots is restricted between 5,000 Hz and 14,000 Hz to only show the high frequency content of the spectra. The upper limit of this frequency range was chosen because the step noise is not visible beyond 14,000 Hz in any of the source maps. Furthermore, the visibility of step noise is case specific (the highest frequency up to which noise is visible increases with increasing velocity) and so those frequencies for which the source maps do not show presence of step noise have not been shown in these plots. Looking at the figure we can see that in general the changing integration region width has no impact on the lower frequency half of the range shown on these plots. However, the spectra begin to diverge at a certain frequency in each case with the divergent frequency value shifting to higher values with increasing velocity (due to rising SNR) and the spectral levels rising with increasing integration region width. As mentioned earlier this divergence is due to wider regions including the spurious noise sources at higher frequencies. Upon visual inspection of the source maps, it was found that the narrowest integration region being considered here ($-0.1 \text{ m} \leq X \leq 0.1 \text{ m}$) is the only region which avoids integrating the spurious noise sources but also includes the step noise at high frequencies.

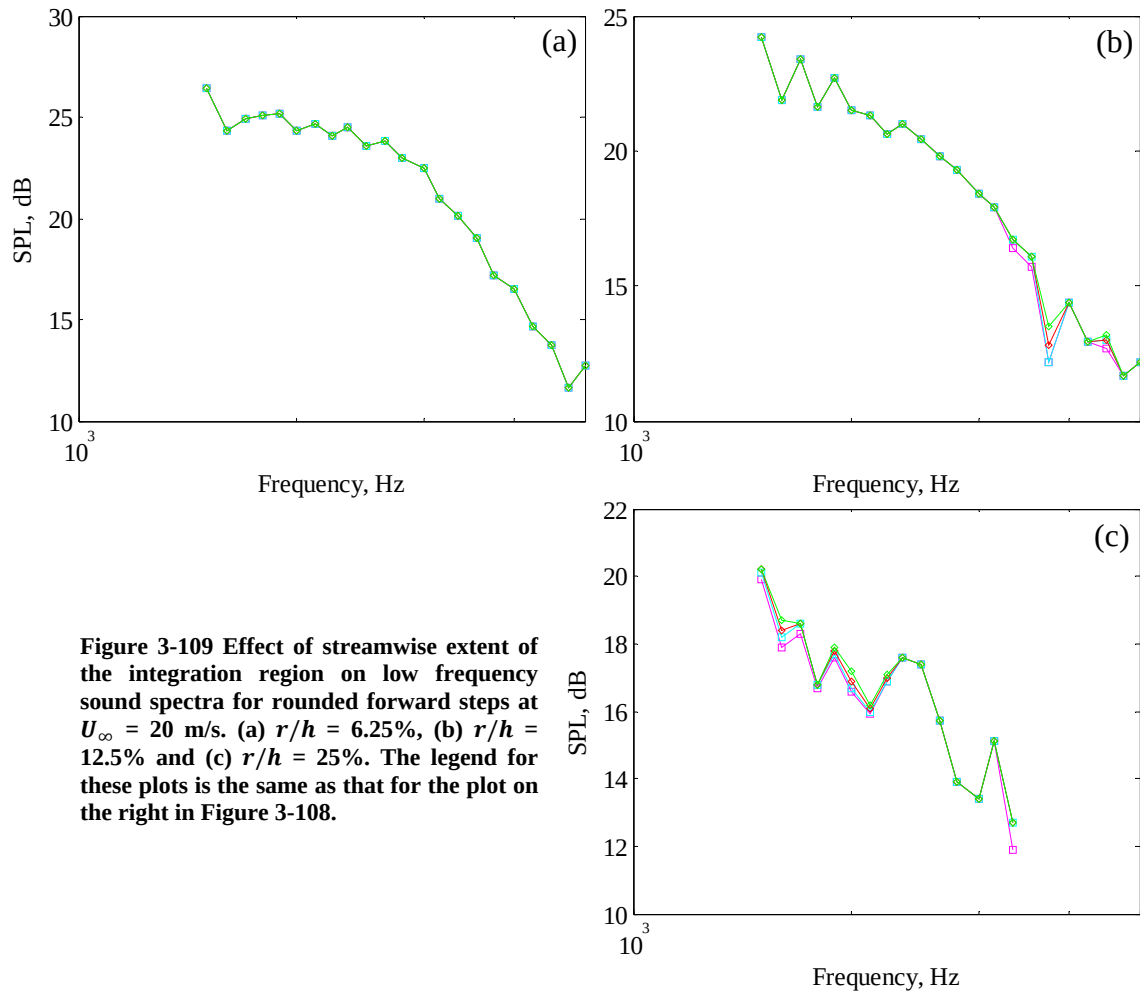


Figure 3-112 shows the high frequency sound spectra obtained using different integration regions for all four rounded forwards steps at $U_{\infty} = 30$ m/s. As before, the frequencies for which step noise is not visible in source maps have not been shown in these plots. Looking at this figure it can be seen that the effect of high frequency noise on the integrated spectra is also apparent for rounded steps and this effect shifts to lower frequencies with increasing rounding. Although not shown, for each rounding, as the velocity increases the noise is shifted to higher frequencies but the discrepancy between different integration regions still can be quite large. As for $r/h = 0\%$ forward step, upon visual inspection the narrowest region here ($-0.1 \text{ m} \leq X \leq 0.1 \text{ m}$) was found to be the most suitable in terms of exclusion of array noise and inclusion of step noise at high frequencies. Therefore, this region was chosen as the high frequency integration region to calculate sound spectra for all rounded forward step. The boundaries of the low and high frequency integration regions that were used to obtain the sound spectra have been shown in Table 3-5.

Table 3-5 Boundaries of integration regions used to obtain sound spectra for rounded forward steps.

Low frequency region			High frequency region		
	Min	Max		Min	Max
$X \text{ (m)}$	-0.5	0.5	$X \text{ (m)}$	-0.1	0.1
$Y \text{ (m)}$	-0.3	0.3	$Y \text{ (m)}$	-0.3	0.3

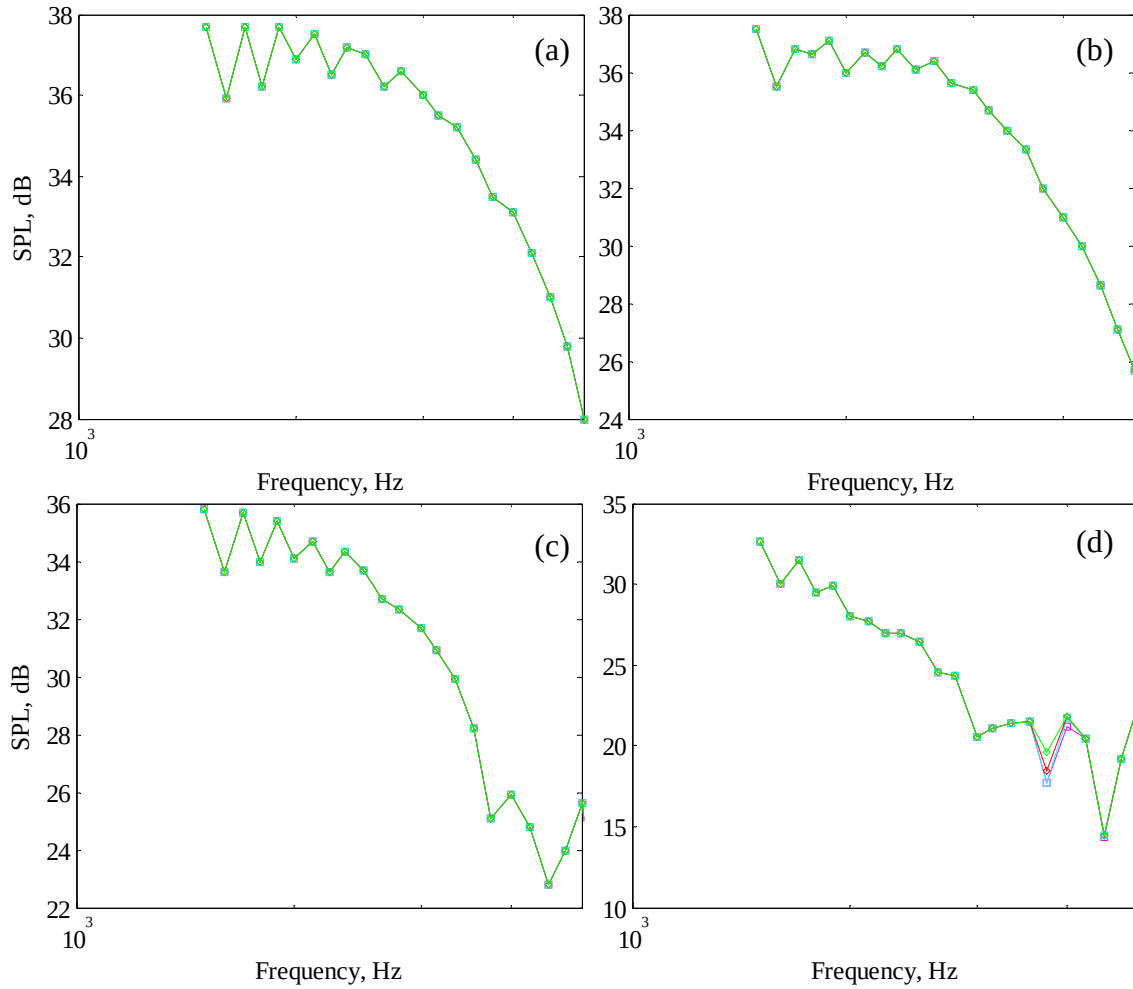
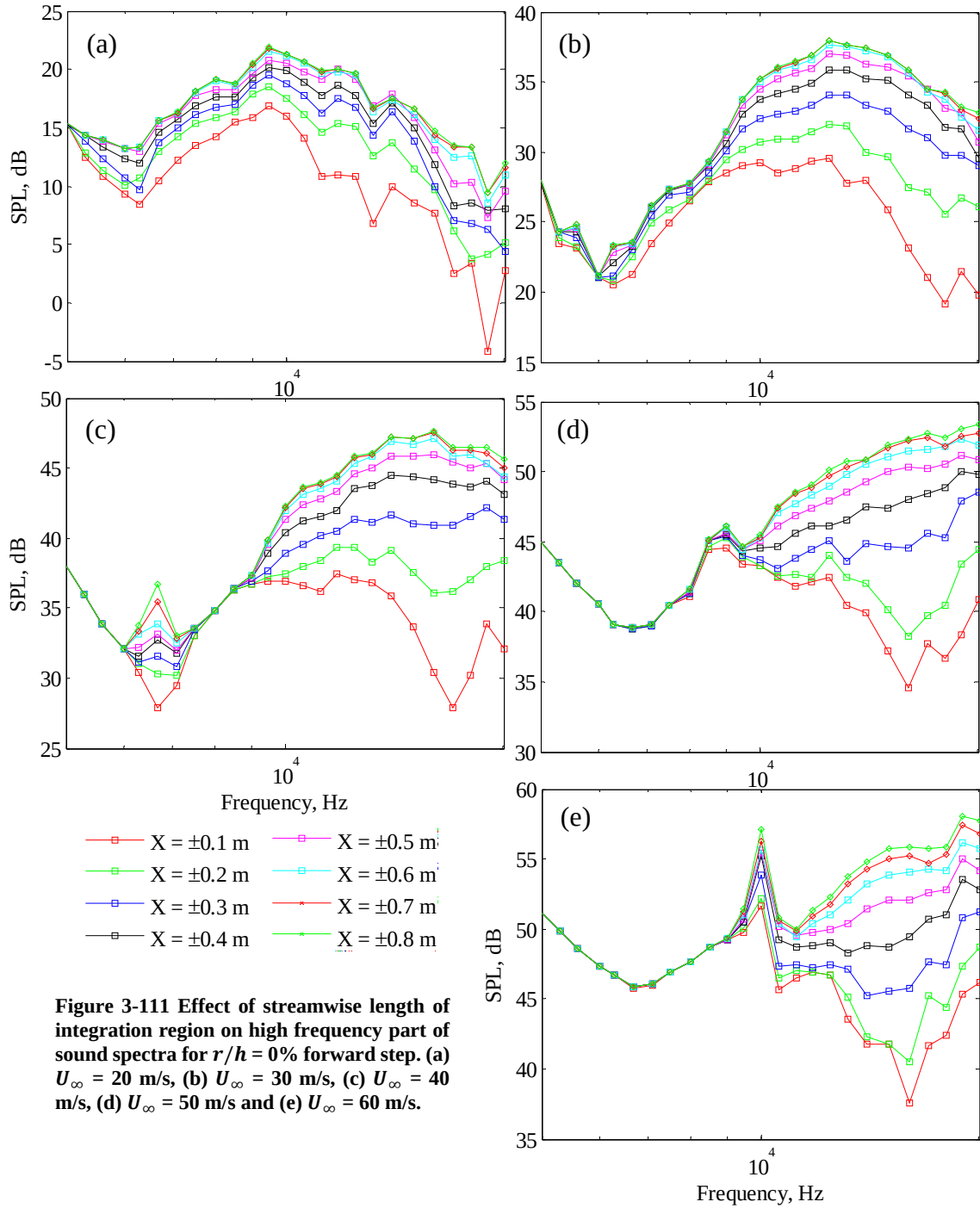


Figure 3-110 Effect of integration region width on low frequency sound spectra calculation for rounded forward steps at $U_{\infty} = 30$ m/s. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$ and (d) $r/h = 25\%$. Legend for each plot is the same as that for the plot on the right in Figure 3-108.

To combine the output of the two integration regions used into a single spectra a cutoff frequency was chosen below which the low frequency integration region output was used and above which the high frequency integration region output was used. This dividing frequency was case specific and was chosen based on where the output of the two integration regions converged. As an example consider Figure 3-113 which shows the spectra calculated from both integration regions for all four rounding of step corner at $U_{\infty} = 30$ m/s. In these figures the frequency axis is limited between 1,000 Hz and 14,000 Hz and the spectral values are only shown for frequencies where the sound source maps showed a clear presence of step noise. This criteria eliminates frequencies below about 1500 Hz and frequencies above about 11,000 Hz for the specific cases shown in these plots. A vertical dashed lines in these figures indicates the frequency value (which is also shown for each case within the plots) at which the output from the integration region begin to diverge from one another. It can be seen that in each case the spectra from both integration region converges around a frequency of 5,000 Hz after which the high frequency region output can be considerably higher than the low frequency region output due to noise. Note that this dividing frequency value between the two outputs is not the same and varies with step rounding with higher rounding resulting in a lower frequency value. This dividing frequency was determined for each case separately and the spectral output from both integration regions was then combined to yield a single sound spectrum for a particular case.

The effect of changing the integration cutoff limit on sound spectra is demonstrated in Figure 3-114 which shows sound spectra at $U_\infty = 30$ m/s for rounded forward steps calculated using six different integration cutoff values. Looking at this figure we can see that in each case raising the integration cutoff limit above 5 dB has negligible effect on the sound spectra. This shows that the integration cutoff limit of 5 dB is sufficient to capture the entire noise from the forward steps and raising this limit is not necessary.



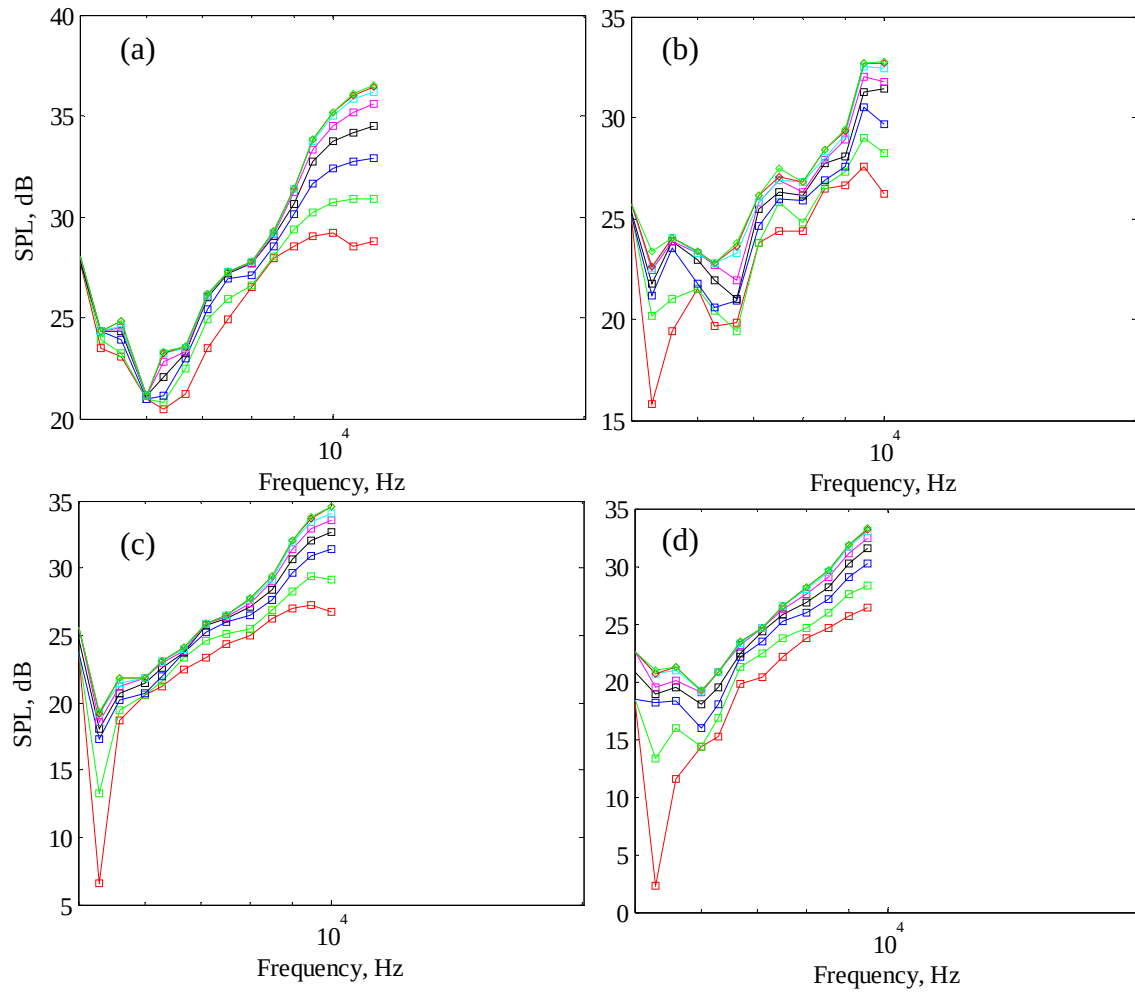


Figure 3-112 Effect of streamwise extent of integration region on high frequency part of spectra from rounded forward steps at $U_\infty = 30$ m/s. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$ and (d) $r/h = 25\%$. Legend for each plot is same as that in Figure 3-111.

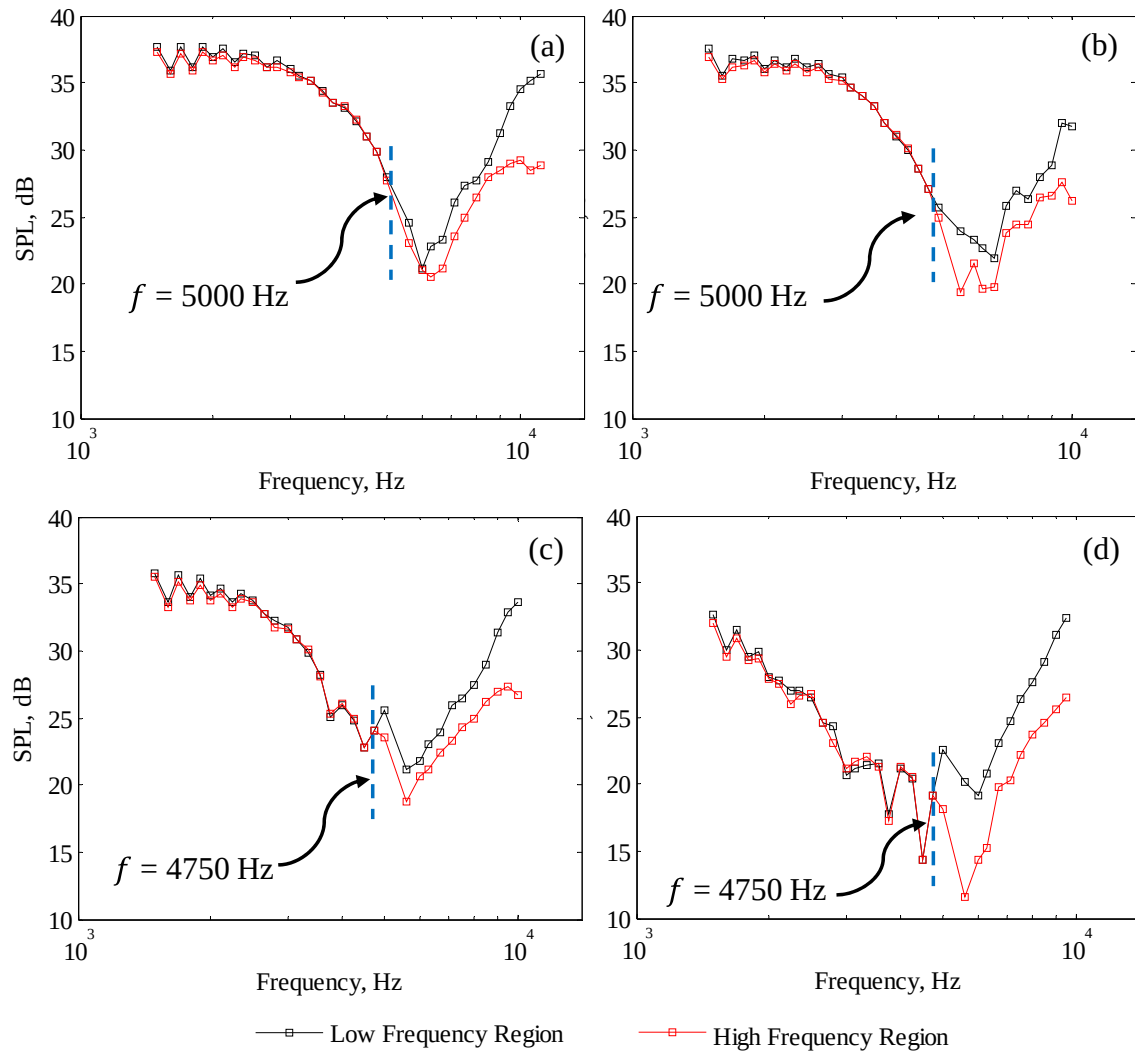


Figure 3-113 Illustration of combining the low frequency and high frequency integration region output to yield sound spectra for rounded forward steps at $U_{\infty} = 30$ m/s. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$ and (d) $r/h = 25\%$.

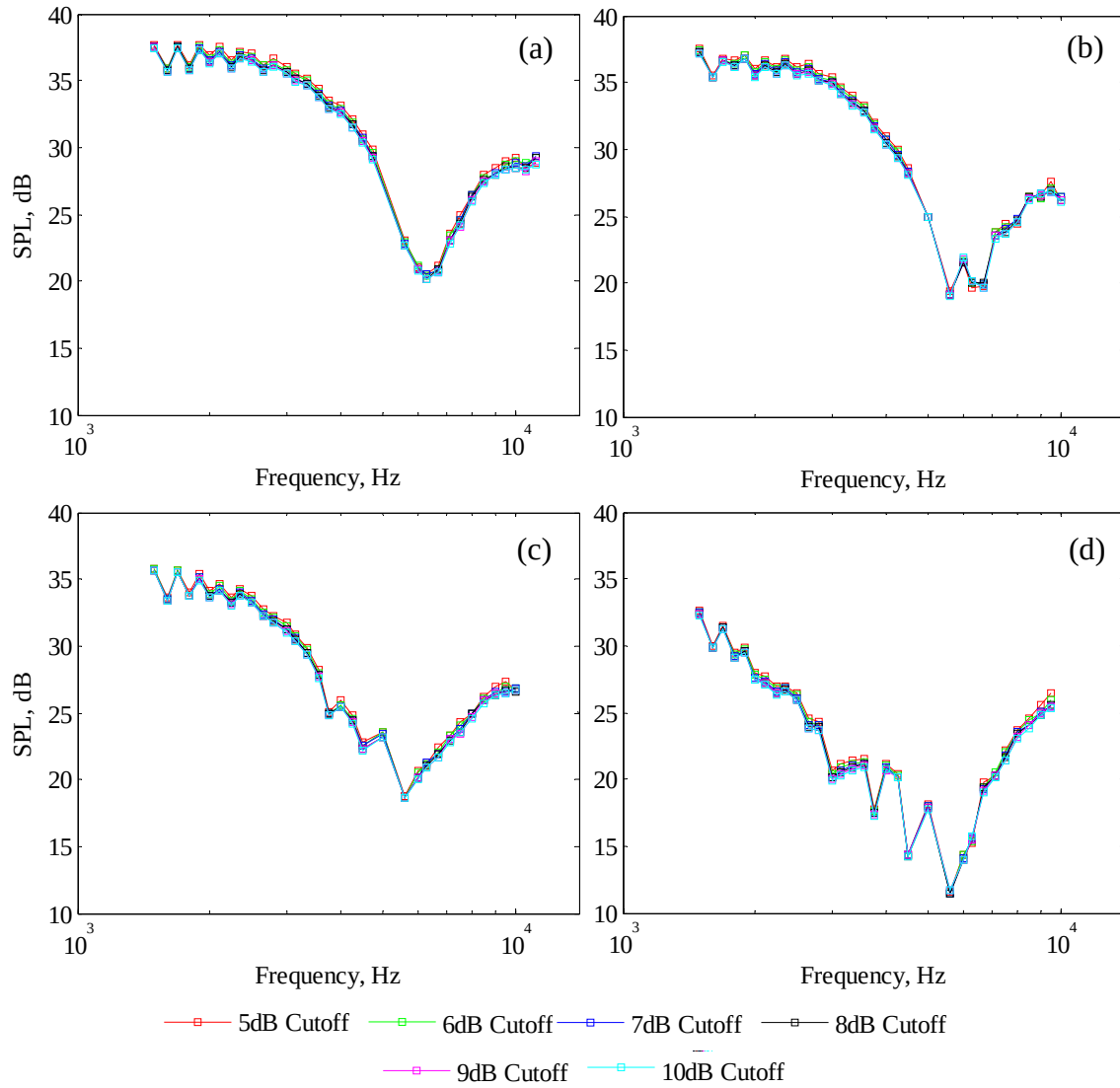


Figure 3-114 Effect of different integration cutoff values used to calculated sound spectra for rounded forward steps at $U_{\infty} = 30$ m/s. (a) $r/h = 0\%$, (b) $r/h = 6.25\%$, (c) $r/h = 12.5\%$ and (d) $r/h = 25\%$.

3.2.6.5 Phased Array and Single Microphone Measurements

In later sections we will compare the array results to single microphone measurements made in the wall-jet facility and to a theory on forward step noise which assumes a point observer located in the far field. The phased array is unlike a single microphone (which closely approximates a point observer) in that its output is a combination of discrete microphones spread over an area. Therefore, a discussion on array output versus a single microphone output is warranted. Two main factors which will affect the interpretation of array results are the observer location and the equivalency between the actual sound producing span of the step and the spanwise boundaries of the integration region. The former is different from a single microphone since the array output is a result of sound averaged over a much larger area. The latter is different due to Point Spread Function (PSF) of the array which can lead to the span chosen for integration of beamformed maps to be different from actual sound producing span of forward steps.

We had earlier defined the observer location of array output as the location of its center with respect to the inside corner of the step ($R = 2.54$ m, $\theta = 95.6^\circ$) since the beamforming procedure was referenced to

the array center. In reality, since the microphones of the array are spread over an area the observer location of the array output would not be equivalent to a single microphone placed at the array center. Therefore, it is necessary to consider how the overall array output is influenced by the different observer location of its component microphones. To get a sense of how the sound may vary as a function of individual microphone location we can assume a source of unit strength with some and calculate the variation of perceived sound levels at each microphone in the array. However, to do so we need a knowledge of the directivity of the step noise source which is an unknown at this point and therefore we need to assume a directivity pattern. Glegg *et al.* (2014)/Bryan *et al.* (2015) who have recently proposed a step noise theory (this theory was discussed in §1.3.1 and will be used to predict forward step noise shortly) found that their predictions of far field sound were in good agreement with measurements made by Catlett *et al.* (2014) in a wall-jet flow. The directivity of step noise in their theory is given by

$$D(f, \theta) = \left| \frac{ix_1}{R} \cos\left(\frac{khx_2}{R}\right) + \frac{x_2}{R} \sin\left(\frac{khx_2}{R}\right) \right|^2 \quad (3-71)$$

where R is the distance to observer from the inside corner of the step, k is the acoustic wavenumber, h is the step height and x_1 and x_2 are the streamwise and wall-normal distance to the observer from inside corner of the step respectively. Note that this directivity is a function of both frequency and the observer location. We can now use equation (3-71) to approximately compute the variation of sound over the aperture of the array due to changing observer location of individual microphones. Figure 3-115 shows a contour map of sound due to a unit strength source placed at the step location as a function of the angular position of microphones in the array and frequency. The contours of $D(f, \theta)$ are shown in dB with the abscissa showing frequency on a log scale and ordinate represents the angular location of each microphone in the array. Note that consistent with the definition of the observer location used in present text the angle θ is defined as the angle which the straight line between the inside corner of the step and center of array microphones makes with the downstream direction. The frequency scale has been restricted between 1,000 Hz and 14,000 Hz since all results from phased array measurements are shown between this range. To provide sufficient resolution of sound variation the contour levels are shown in increments of 1 dB between -20 and 0 dB. The white color portions of the plot represent values which are lower than -20 dB. Further note that it is assumed that the directivity is same along the spanwise direction and only depends on the streamwise location of the observer with respect to the step.

Looking at Figure 3-115 we first note that the angular location of individual microphones (represented by the ordinate of the plot) can vary over twenty degrees ($\theta = 90.2^\circ - 112^\circ$) across the array. The sound varies throughout the frequency range and at frequencies below about 6,000 Hz near vertical frequency bands with only slight variation (± 1 dB) in sound with observer location. At frequencies between 6,000 Hz and 7,000 Hz however, a large attenuation (12 dB or more) of sound with observer location is seen with attenuation increasing as the observer angle approaches 90° . The reason for this variation is that the directivity function chosen here produces a dip in sound levels at frequencies associated with half acoustic wavelength corresponding to the step height. This dip is much more pronounced in the region directly above the step but becomes weaker as one moves upstream of the step and hence the variation. Note that the measured spectra obtained from integration of beamformed maps discussed in the previous section also show a dip at these frequencies and thus the directivity function chosen here is consistent with measurements. As the frequency is increased beyond 7,000 Hz the directivity pattern begins to repeat and a strong variation of sound with observer location near frequencies corresponding to acoustic wavelength equal to step height are seen.

If the directivity function assumed here for step noise is believed to be correct we can conclude that the changing directivity of individual microphones can be expected to have a significant impact on the integrated spectrum at frequencies where the effects of destructive interference are dominant (between 6,000 – 7,000 Hz and beyond 13,000 Hz). In this frequency band the array output would produce higher levels and a much less pronounced spectral dip than that for a point observer located at the center of the

phased array ($\theta = 95.6^\circ$). At frequencies where these effects are not dominant the directivity of sound field has a small effect (approximately ± 1 dB) on the overall array output.

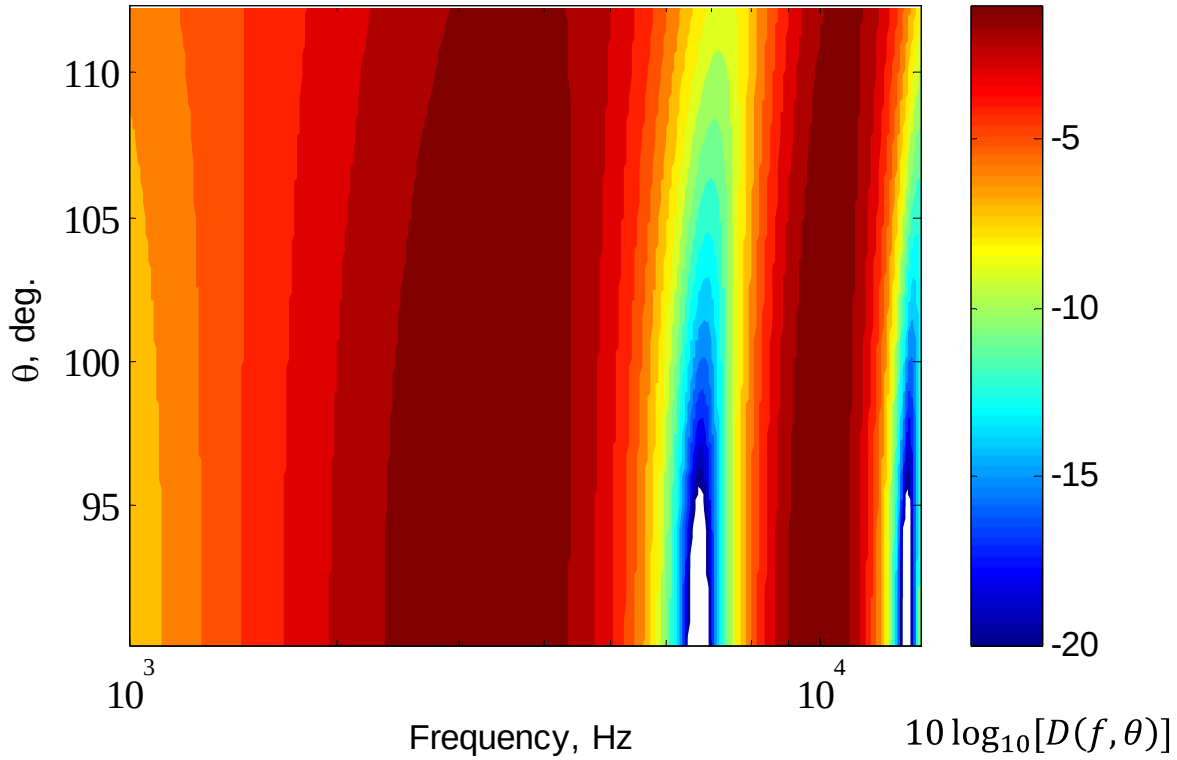


Figure 3-115 Variation of sound from a source of unit strength and directivity proposed by Glegg *et al.* (2014)/Bryan *et al.* (2015) over the aperture of the phased array.

We now consider the equivalency of sound producing span of a forward step and the spanwise width of integration region used to obtain frequency spectrum from beamformed sound source maps. The reason the width of a spanwise integration region cannot be considered equal to the actual effective span of the forward step is because the Point Spread Function (PSF) of the array. This function for the present array was already discussed in detail in §3.1.6.1. As shown there the point spread function varies with frequency and its spatial extent becomes smaller as the frequency increases. For example, at 2,000 Hz the spatial extent of the PSF (defined as 3 dB down from the maximum sound source level) is about 0.68 m while at 10,000 Hz it is only about 0.089 m. Due to this finite spatial extent of PSF any integration region chosen may also include contributions from sources outside it. To quantify this effect we can compare the ratio of array output obtained by integrating two different spanwise regions to the ratio obtained from the same two spanwise regions but with an ideal source distribution i.e. source whose dimension is infinitesimally small. The ratio of far field sound produced by two spanwise regions of width L_1 and L_2 with an ideal source distribution along the spanwise direction (Z) is given by

$$\frac{\Phi_{ppL_2}}{\Phi_{ppL_1}} = \frac{\int_{-L_2/2}^{L_2/2} \frac{dZ}{Z^2 + R^2}}{\int_{-L_1/2}^{L_1/2} \frac{dZ}{Z^2 + R^2}} \quad (3-72)$$

here R is the source to observer distance which is defined as the radial distance from the inside corner of the step to the center of the phased array and is the same for both the spanwise regions ($R = 2.54$ m). Note that this ratio is independent of the frequency and will yield a constant value since it is assumed that the

source in both the spanwise regions has the same strength and directivity. The ratio in Equation (3-74) can now be compared to that obtained by integrating the beamformed source maps with two different spanwise integration regions of width of L_1 and L_2 . For comparison we pick $L_1 = 0.6$ m which corresponds to the spanwise integration region of $-0.3 \text{ m} \leq Y \leq 0.3 \text{ m}$ in the beamformed maps since this is the regions which will be used to present the sound spectra from forward steps in next section (see previous section for more details on how this regions was chosen). For the second region we pick two values of $L_2 = 0.4$ m and 1.0 m which correspond to integration regions of $-0.4 \text{ m} \leq Y \leq 0.4 \text{ m}$ and $-0.5 \text{ m} \leq Y \leq 0.5 \text{ m}$ respectively. These regions are chosen for this study because as will be seen shortly the region with $-0.4 \text{ m} \leq Y \leq 0.4 \text{ m}$ was chosen to compare the phased array results to wall-jet measurements and the region width $-0.5 \text{ m} \leq Y \leq 0.5 \text{ m}$ was chosen to compare phased array results to theory.

Figure 3-116 (a)-(d) shows the comparison of calculated ratios for $L_2 = 0.8$ m, 1.0 m and $L_1 = 0.6$ m with the measured ratios from the spanwise integration regions of the same width at four different free stream velocities. Note that the measured ratios are calculated from by integrating the beamformed source maps according to the procedure described in previous section which excludes integration of any spurious noise sources. For the lowest velocity ($U_\infty = 20$ m/s, Figure 3-116 (a)) we first note that the calculated ratio using source distribution for $L_2 = 1.0$ m is about 1 dB higher than for $L_1 = 0.8$ m which is to be expected due to larger sound producing region for the former. The measured ratios in each case have a good agreement with calculations above about 2,000 Hz but are slightly lower at frequencies below that. This low frequency discrepancy however is small and restricted to within 1 dB for both integration regions. As the velocity increases to 30 m/s (Figure 3-116 (b)) we see higher frequency content in the measured ratios due to improved SNR. The low to mid frequencies for both integration regions show good agreement with the calculation but above approximately 8,000 Hz the measured ratios begin to drop with maximum deviation of about 1.5 dB and 1 dB for $L_2 = 1.0$ m and 0.8 m respectively occurring around 11,000 Hz. Similar trend continues for $U_\infty = 50$ m/s as seen in Figure 3-116 (c) with the measured ratios at higher frequencies showing attenuated levels. For the highest velocities considered here ($U_\infty = 60$ m/s, Figure 3-116 (d)) the agreement between measurement and calculation is excellent up to about 8,000 Hz past which the measured ratios drop as seen for lower velocities.

The discrepancy in ratios at the higher frequency end of the spectrum seen in Figure 3-116 is likely due to the low SNR and the agreement improves with increasing velocity due to enhanced source strength. The lower frequency end of the spectrum (below 2,000 Hz) shows slightly lower value of ratios for measurements in each case and is likely due to the large spatial extent of the array PSF. These results suggest that changing the spanwise integration region of the beamformed source maps provides a good estimate (to within a dB) of changing the actual span of the source region up to about 8,000 Hz.

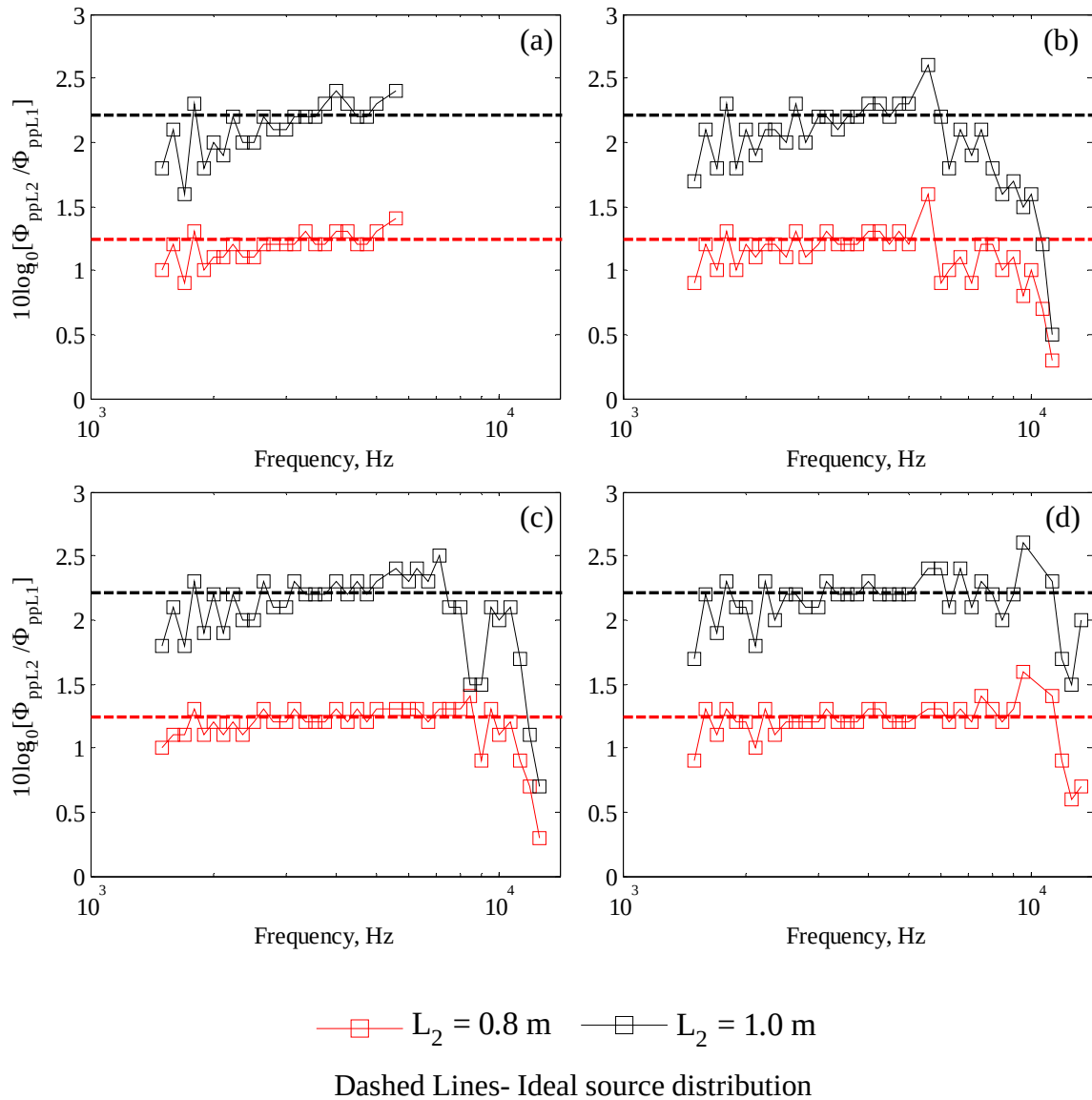


Figure 3-116 Comparison of calculated and measured ratios between sound from two different spanwise regions. (a) $U_\infty = 20 \text{ m/s}$, (b) $U_\infty = 30 \text{ m/s}$, (c) $U_\infty = 50 \text{ m/s}$ and (d) $U_\infty = 60 \text{ m/s}$. Note that $L_1 = 0.6 \text{ m}$ in each case.

3.2.6.6 Integrated Sound Spectra

Figure 3-117 shows the far field sound spectra computed by integrating the sound source maps for $r/h = 0\%$ step at 9 different free stream velocities. Looking at these spectra we can see that increasing the velocity consistently increases the sound power at each frequency without affecting the shape of the spectra. The shape of the spectra itself is reminiscent of the forward step sound spectra measured in the wall-jet facility with the observer located directly above the step i.e. at $\theta = 90^\circ$ (see §2.2.4 for the far field sound results from $r/h = 0\%$ step in wall-jet facility). It was noted in the discussion on results from wall-jet measurements that the far field sound spectra for observer located in the region directly above experiences non-compactness effects which leads to the spectra developing a different shape than those measured upstream and downstream of the step location. Additionally, the spectra also showed a spectral dip at high frequencies which was associated with the destructive interference caused by acoustic reflection from the

surface upstream of the step. The observer location for the spectra shown in Figure 3-117 is $\theta = 95.6^\circ$ and therefore the spectra display the same non-compactness effects with a shape similar to those found in wall-jet measurements and a spectral dip due to destructive interference located around 7000 Hz. The half acoustic wavelength ($\lambda = c/2f$, c being the sound speed) associated with this frequency is 24.6 mm which is approximately the same as the step height ($h = 25.4$ mm) used in the present work. Note that the spectral dip here occurs at a frequency which about half of that is observed in the wall-jet measurements because the present step is twice as tall.

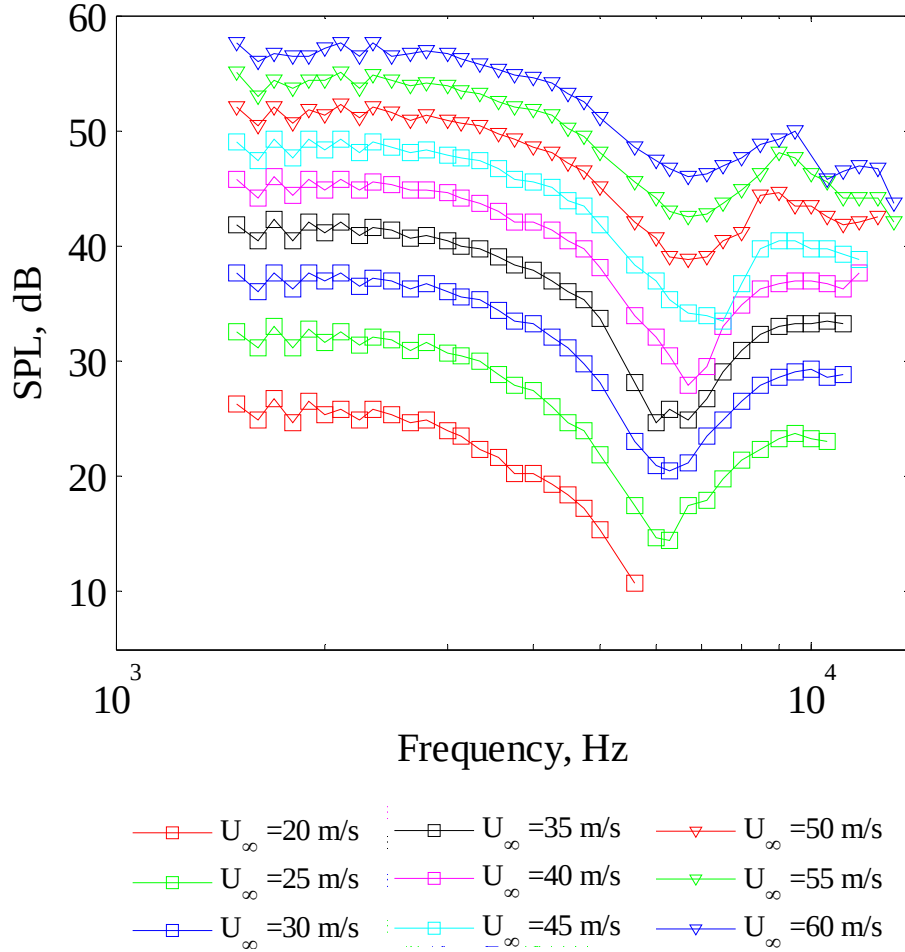


Figure 3-117 Far field sound spectra for $r/h = 0\%$ at nine different free stream velocities.

We can now take the spectra for $r/h = 0\%$ forward step at different velocities shown in Figure 3-117 and scale it on the scaling based on the 5th power of undisturbed edge velocity at the step location that was used to collapse the spectra measured in the wall-jet facility. This scaling is given by

$$\Phi_{pps}(f) = \frac{\Phi_{pp}(f)c^2}{\rho^2 U_\infty^5 h} \quad \text{and} \quad f_s = \frac{fh}{U_\infty} \quad (3-73)$$

where Φ_{pps} and f_s now refer to the scaled far field sound spectral density and frequency levels respectively, c is the local speed of sound, ρ is the density, U_∞ is the undisturbed edge velocity and h is the step height. Figure 3-118 (a) shows the spectra for $r/h = 0\%$ scaled on this scaling for nine different velocities. Looking at the figure it can be seen that this scaling collapses the spectra (to about ± 2.5 dB) only up to a non-

dimensional frequency of about 2. This result is similar to that found in the wall-jet measurements in that the scaling did not collapse the spectra measured in the region directly above the step due to the non-compactness effects. Note that the observer location in present work is $\theta = 95.6^\circ$ which is comparable to $\theta = 90^\circ$ at which measurements were made in the wall-jet facility. We can now also attempt the mixed scaling proposed by Catlett *et al.* (2014) with the spectral level scaling remaining the same as that in the equation above but the frequency axis scaling is now given by

$$f_s = \frac{f\delta}{U_\infty} \quad (3-74)$$

where δ is the undisturbed boundary layer thickness at the location of the vertical face of the step. Note that the undisturbed boundary layer thickness measurements are only available for $U_\infty = 30$ m/s and 60 m/s for which $\delta = 97$ mm and 95 mm respectively. The value of δ for the other velocities were obtained through linear interpolation and are given in the table below. The sound spectra scaled on the mixed scaling are shown in Figure 3-118 (b). As expected this scaling does not collapse the spectra either since the character of the spectra is dominated by the non-compactness effects and a scaling based on the fluid dynamics parameters must not be expected to provide a collapse. This result is also consistent with that seen for the wall-jet measurement results.

Table 3-6 Interpolated boundary layer thicknesses for mixed scaling of far field sound spectra.

U_∞ (m/s)	20	25	30	35	40	45	50	55	60
δ (mm)	97.6	97.3	97	96.7	96.3	96	95.7	95.3	95

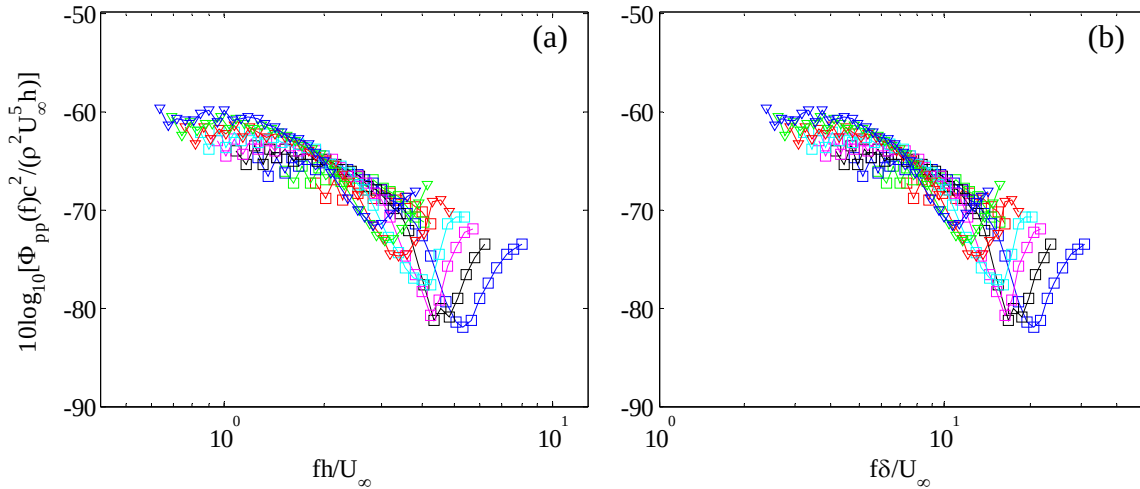


Figure 3-118 Velocity scaling of far field sound spectra for $r/h = 0\%$ forward step. (a) Dipole scaling based on the step height and 5th power of undisturbed edge velocity and (b) Mixed scaling of Catlett *et al.* (2014) with frequency scaled on undisturbed boundary layer thickness and undisturbed edge velocity. Note that for the latter scaling the boundary layer thickness was measured only for $U_\infty = 30$ m/s and 60 m/s, the rest were obtained through linear interpolation. Legend for the figure is the same as that for Figure 3-117.

We now compare the far field sound spectra in present measurements to those obtained in the wall-jet facility. Before we proceed however, we should acknowledge the differences between the measurement conditions in the two facilities. The only case in the present work which is comparable to measurements in the wall-jet facility (based on undisturbed edge velocity) is that for $U_\infty = 20$ m/s. However, edge velocity is not the only parameter which influences the measurements in the two facilities. There are other parameters which must be considered. These are: step height, undisturbed boundary layer thickness,

observer location and the span of the sound producing region of the step. Table 3-7 below summarizes the flow and geometry conditions for measurements in the two facilities with the subscript WJ and ST referring to parameters in wall-jet and present measurements respectively. We will now attempt to match the conditions between the two facilities as reasonably as possible to provide grounds for comparison between the two measurements. We first note that the free stream velocity, step height and boundary layer thickness differences can be accounted for by using the 5th power to the velocity scaling of Catlett *et al.* (2014) which has been shown to collapse the data in the wall-jet measurements for several different step heights and boundary layer thicknesses. This scaling is shown in equations (3-73) and (3-74). Note that we will use the mixed scaling with frequency axis normalized on boundary layer thickness since unlike the flow conditions in present work where using boundary layer thickness or step height yields similar results due to small changes in δ with flow speed, the difference in flow conditions between the two facilities is significant. The angular location of the observer (θ_{WJ} and θ_{ST}) in the two measurements are very similar, so for now we assume negligible impact of that on the measurements. The more significant parameter which is different in the two measurement is the radial distance. To account for this we assume a cylindrical spreading of the sound waves in the two facilities to account for the reflections from the boundaries. Under this assumption the ratio of acoustic intensities (in dB) between the two facilities is given by

$$10 \log_{10} \left(\frac{I_{ST}}{I_{WJ}} \right) = 10 \log_{10} \left(\frac{R_{WJ}}{R_{ST}} \right) = -6.4 \text{ dB} \quad (3-75)$$

This means that 6.4 dB must be either added to the spectrum in present measurements or subtracted from the wall-jet measurements to account for the difference in source to observer distance. Arbitrarily we choose to add 6.4 dB to the present measurements. The last parameter listed in Table 3-7 which must be accounted for is the span of the sound producing region of forwards step. For wall-jet this span (W_{WJ}) is fixed and is equal to the span of the 2D core of the wall-jet which is about 0.8 m. In case of present measurement as listed in the table this span is variable and can be changed by changing the span of the integration region used to obtain the sound spectrum. Therefore, we expand our integration from $Y = \pm 0.3$ m used so far to $Y = \pm 0.4$ m in order to facilitate comparisons with wall-jet measurements. In doing so however, we must remember that an integration span of 0.8 m is not necessarily equal to a sound producing span of the same width because of the finite spatial extent of the point spread function associated with the array. As was shown in §3.2.6.5 this can lead to some uncertainty (1.5-2 dB) in the measurements from the phased array.

If we now apply the scaling based on the 5th power of edge velocity and step height to the two measurements we can perform a comparison between the two that accounts for the step height and the velocity difference. This comparison between the two measurements using mixed scaling of Catlett *et al.* (2014) is shown in Figure 3-119 (a). As seen in the figure, the two scaled spectra have similar shape but the present spectrum is shifted to the right and higher in magnitude. At first, this suggests that the sound source mechanism in two cases is somehow different. However, upon a more careful consideration we note that there are some fundamental differences in the nature of the two flows which must be considered in order to enable a fair comparison. The wall-jet flow is not like the flow in Stability Tunnel in the sense that it does not consist of a free stream. Instead, the flow in the wall-jet reaches a maximum velocity above the wall and the wall-normal distance at which this occurs is defined as the boundary layer thickness δ_{WJ} . This velocity profile in the wall-jet flow is depicted in Figure 3-120 (a). However, as shown in this figure, above the point $y = \delta_{WJ}$ the profile begins to curve back due to absence of free stream flow. This curving back of the profile results in the presence of a large mixing layer defined by interaction between the flow and the stagnant air on top of it. The presence of this mixing layer means that the actual shear layer within which the step is embedded is actually much larger than just the boundary layer thickness δ_{WJ} . This large mixing layer will result in the wall-jet having a much lower momentum than the Stability Tunnel boundary layer with free stream as shown in Figure 3-120 (b). This lower momentum would imply a lower unsteady surface

loading on step surfaces in wall-jet flow which in turn would explain the lower sound levels for the same edge velocity value.

The difference in the two types of flow suggests that we cannot expect the sound to quantitatively scale on the same parameters. Thus, we must logically choose a different length and/or velocity scale in the wall-jet flow which would compare with the conventional boundary layer flow of the Stability Tunnel. A length scale which is usually used to define the mixing layer in wall-jet flow is the half-height $y_{1/2}$ which is defined as the height above the point of maximum velocity where the velocity is half its maximum value. This length is shown in Figure 3-120 (a). It has been found in the past by many studies on undisturbed wall-jet flow (e.g. Wygnanski *et al.* (1992), Grissom (2007)) that normalizing the boundary layer profile on this half-height results in a self-similar profile. For the wall-jet facility used in the present work this half-height was measured by Clark (2014) at the location where the forward step was later installed. Their measurements showed that the half-height at the step location for $U_m = 21.3$ m/s was approximately $y_{1/2} = 111$ mm. Based on this half-height then we can introduce a new scaling for the wall-jet spectra as

$$\Phi_{pps}(f) = \frac{\Phi_{pp}(f)c^2}{\rho^2 U_m^5 h} \quad \text{and} \quad f_s = \frac{f y_{1/2}}{U_m} \quad (3-76)$$

here we have retained the spectral magnitude scaling on 5th power of undisturbed edge velocity but have scaled the frequency on the half-height and edge velocity. The comparison between the wall-jet spectra scaled on this scaling with the Stability Tunnel spectra scaled on the mixed scaling of Catlett *et al.* (2014) has been shown in Figure 3-119 (b). Looking at this figure now we see that using half-height to scale the wall-jet spectra does bring the two spectra closer along the frequency axis but the magnitude of the spectra measured in Stability Tunnel is still higher most likely due to momentum differences of the flow in that facility which is not characterized by $y_{1/2}$.

Besides the flow difference one must also acknowledge that the phased array measurements are fundamentally different than a single microphone measurements made in wall-jet facility. This difference between the two measurements was earlier discussed in §3.2.6.5. As shown there the array measurement is different from a single microphone with regards to the observer directivity and the sound producing span of forward step. The uncertainty in the array measurements due to these factors can also partially contribute to the differences seen in the data from two facilities.

Table 3-7 Flow and geometry parameters for measurements in wall-jet facility and Stability Tunnel.

U_∞ (m/s)	20	U_m (m/s)	21.3
h_{ST} (m)	0.0254	h_{WJ} (m)	0.0127
δ_{ST} (mm)	97	δ_{WJ} (mm)	15.2
R_{ST} (m)	2.54	R_{WJ} (m)	0.58
θ_{ST} (deg.)	95.6	θ_{WJ} (deg.)	90
W_{ST}	Variable	W_{WJ} (m)	0.8

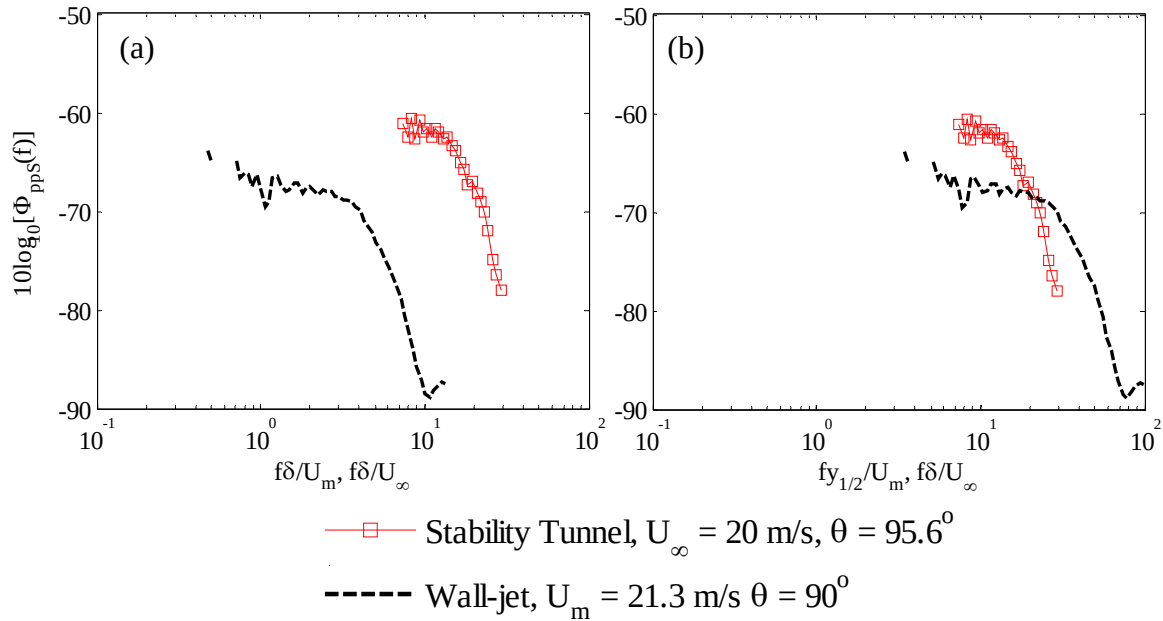


Figure 3-119 Comparison of scaled far field sound spectra between wall-jet and Stability Tunnel measurements at comparable edge velocities and angular observer location. (a) Mixed scaling based on 5th power of edge velocity, step height and boundary layer thickness and (b) Mixed scaling based on 5th power of edge velocity, step height in the wall-jet facility and half-height of mixing layer. 6.4 dB has been added to the Stability Tunnel spectra to account for the difference in source to observer distance assuming cylindrical spreading of acoustic waves.

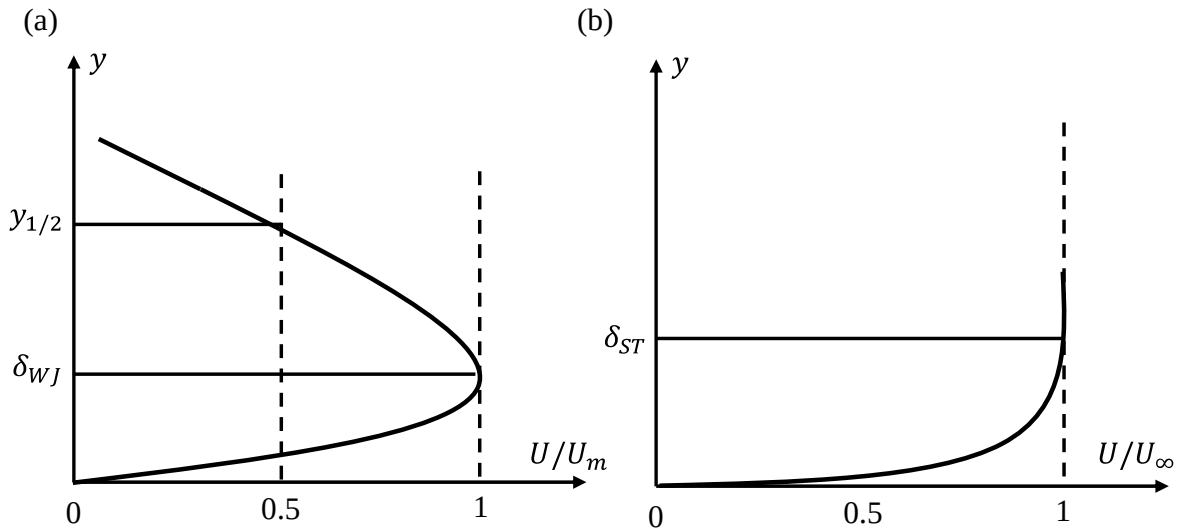


Figure 3-120 Description of boundary layer profiles in the two facilities used to measure far field sound from forward step configuration. (a) Wall-jet mean velocity profile and (b) Stability Wind Tunnel mean velocity profile.

Now, consider the effect of rounding the step corner on the far field sound as shown in Figure 3-121 (a) and (b) for $U_{\infty} = 30 \text{ m/s}$ and 60 m/s respectively. For comparison, the spectra for the same four rounding radii to step height ratios (as SPL in dB) measured in the wall-jet facility at $\theta = 90^{\circ}$ for edge velocity of $U_m = 21.3 \text{ m/s}$ are shown in Figure 3-121 (c). The effect of rounding the corner at $U_{\infty} = 20 \text{ m/s}$ which will be more comparable to the results in the wall-jet facility are not shown because the SNR at that velocity for

higher rounding limits the frequencies for which results are available rendering any comparison difficult. Looking at Figure 3-121 (a) we can see the effects of rounding the step corner on the spectra is qualitatively similar to that in the wall-jet facility. As the step corner is slightly rounded to $r/h = 6.25\%$ the sound levels at very low frequencies are identical to an unrounded step but begin to drop at frequencies above about 3,000 Hz. At frequencies above about 5,000 Hz the null associated with destructive interference begins to affect the spectra. Compared to the wall-jet measurements for the same rounding the spectra here begins to attenuate, compared to an unrounded step, at much lower frequencies. This is because of the step height being larger in the present work which results in the non-compactness effects of the step occurring at frequencies much lower than those in the wall-jet measurements. As the step is rounded further to $r/h = 12.5\%$ the sound levels at lowest frequencies drop by about the same amount as those in the wall-jet facility. However, in the mid frequency range the spectrum shows much more attenuation (about 7 dB at 4,000 Hz) than the wall-jet spectrum (about 2.5 dB at 4,000 Hz). For the highest rounding of $r/h = 25\%$, the spectral attenuation at lowest measureable frequencies is about the same as those in wall-jet facility but can be as large as 15 dB in the mid frequency range compared to about 10 dB seen in the wall-jet facility. Note that above about 3,000 Hz the spectrum for this step develops a jagged appearance which is possibly due to low SNR for this particular rounding. Though the beamformed maps do show presence of sound source at frequencies above 3,000 Hz for this case, their strength is low and the higher frequencies here are likely contaminated by the side lobes and background noise.

The effect of rounding at $U_\infty = 60$ m/s (in Figure 3-121 (b)) is qualitatively similar to that at the lower velocity with the sound levels at lower frequencies dropping slightly with rounding and then attenuating towards the null associated with destructive interference at a much faster rate than seen in the wall-jet measurements. The effects of destructive interference are much more visible in this plot due to high SNR and in each case the trough in the spectra appears around 6700 Hz which is half the frequency of the same feature observed in the wall-jet measurements due to the step in present work being twice as large. In summary, the results of Figure 3-121 show that while at very low frequencies the rounding affects the sound spectra in a similar fashion as those in the wall-jet facility, in mid to high frequency range their behavior is governed by the non-compactness of the step.

Figure 3-122 shows the sound spectra for rounded forward steps scaled on the 5th power of undisturbed edge velocity and the step height. As expected (and also seen for an unrounded forward step) the scaling provides a reasonable collapse at very low frequencies but is ineffective as the frequency increases due to dominant non-compactness effects in the spectra. Although not shown, similar to an unrounded forward step, the mixed scaling of Catlett *et al.* (2014) on the undisturbed boundary layer thickness does not collapse these spectra either.

Figure 3-123 shows the present and wall-jet spectra for rounded steps at comparable undisturbed edge velocities with the present spectra scaled on the mixed scaling of Catlett *et al.* (2014) and the wall-jet spectra scaled according to (3-76) which accounts for the large mixing layer in the wall-jet. Looking at this figure it can be seen that as for the $r/h = 0\%$ comparison (shown in Figure 3-119) the scaling does not collapse the two spectra as a result of flow differences in the two facilities as well as the differences between phased array and single microphone measurements. Note that as the rounding increases the discrepancy between the measurements in the two facilities reduces possibly due to the reduction in source strength with increasing rounding. Further note that the present data for $r/h = 25\%$ has very low SNR (hence limited spectral data points in Figure 3-123 (c)) and therefore its quantitative agreement with the wall-jet data is dubious.

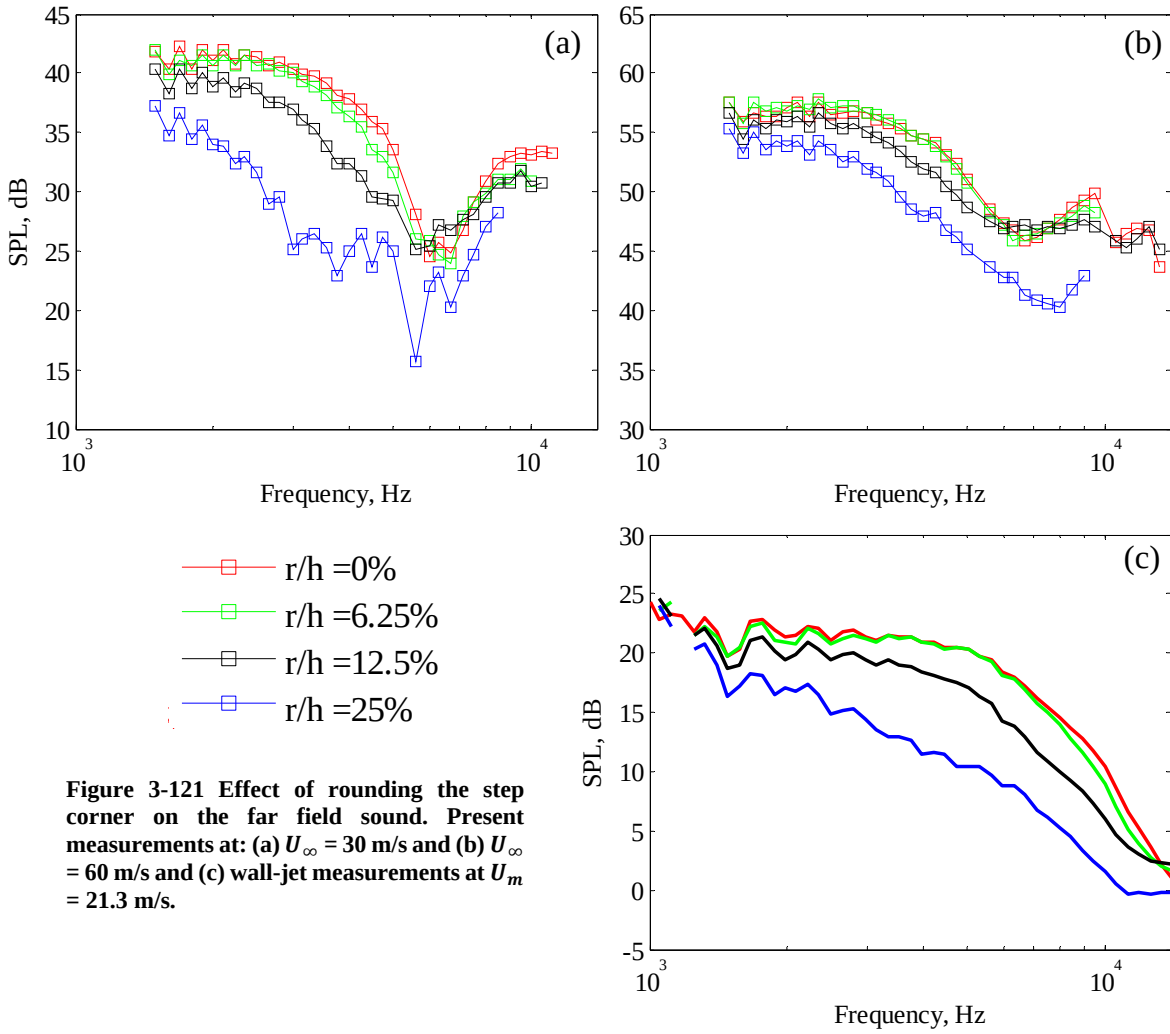
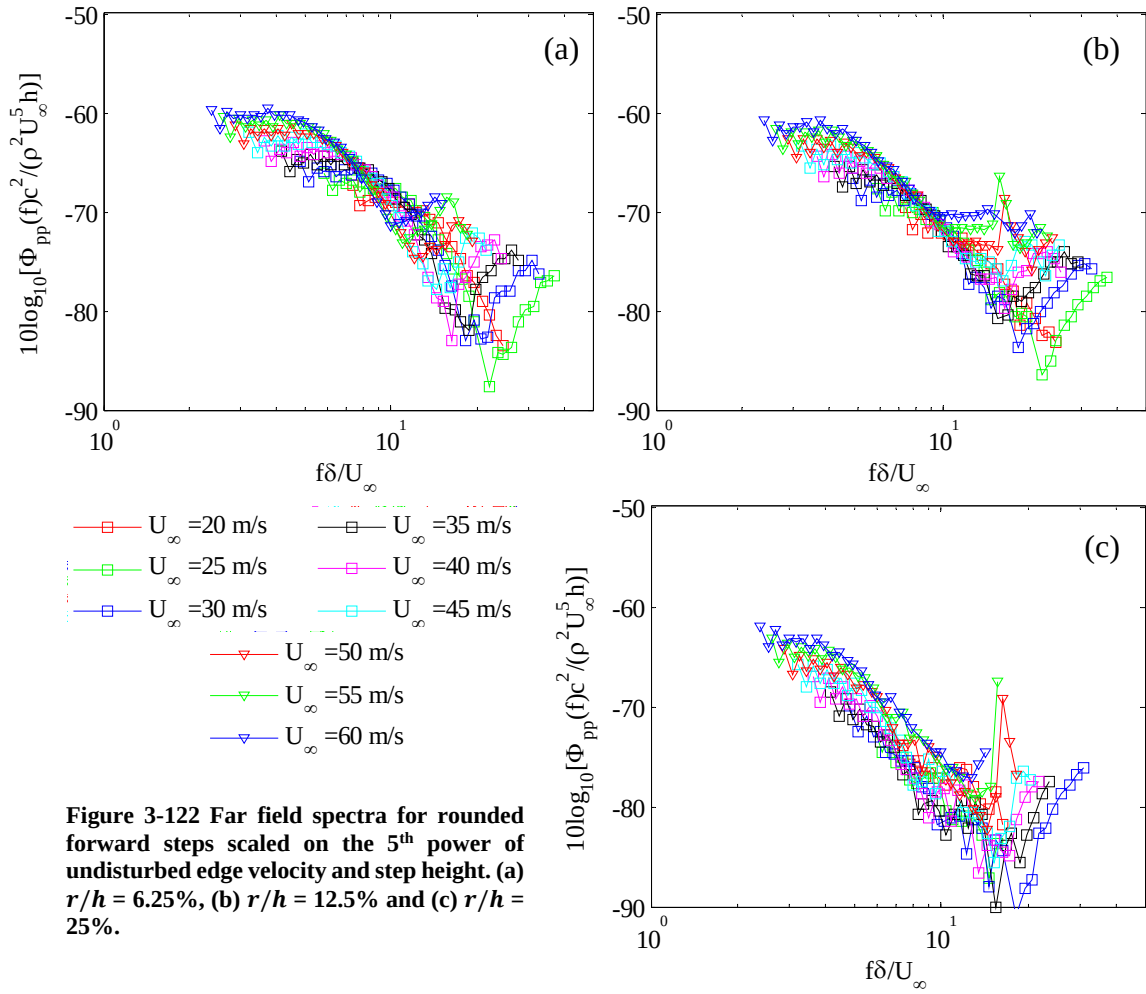
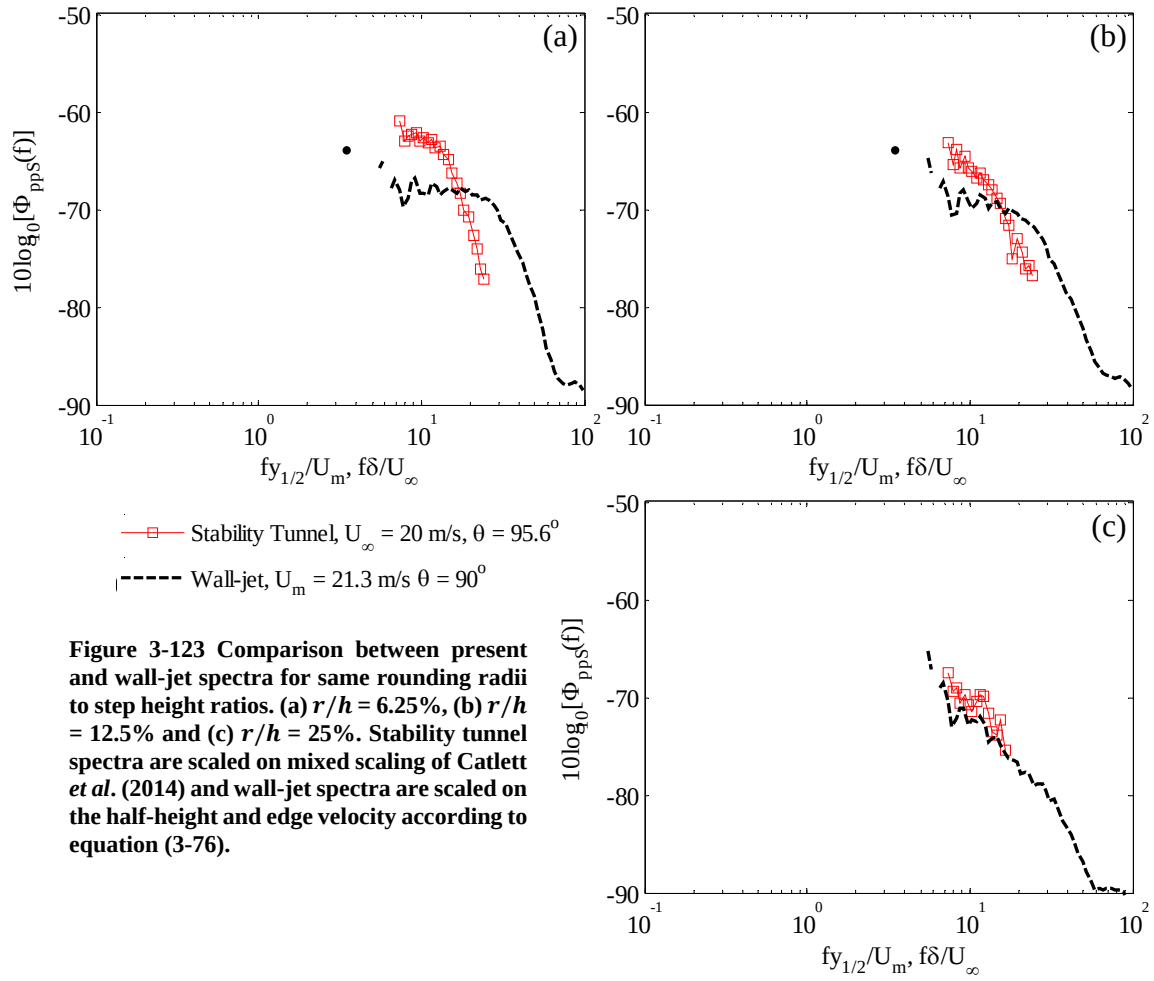


Figure 3-121 Effect of rounding the step corner on the far field sound. Present measurements at: (a) $U_{\infty} = 30$ m/s and (b) $U_{\infty} = 60$ m/s and (c) wall-jet measurements at $U_m = 21.3$ m/s.





3.2.7 Extraction of the Sound Source Spectrum

We will now attempt to extract the sound source spectrum by combining the measurements with insight obtained from the theory of Glegg *et al.* (2014). Recall from §1.2 that in low Mach number flows the contribution from the quadrupole sources i.e. the sources within the boundary layer is negligible and the far field acoustic pressure from Curle's (1955) equation is given by

$$p(\mathbf{x}, \omega) = - \int_S p(\mathbf{y}, \omega) \frac{\partial G(\mathbf{x}, \mathbf{y}, \omega)}{\partial n} dS(\mathbf{y}) \quad (3-77)$$

The above integral contains within it the near field pressure which is a function of the source coordinate $\mathbf{y}$ and frequency ω and the normal derivative of the Green's function associated with a particular geometry which in addition to being a function of $\mathbf{y}$ and ω is also a function of the far field observer coordinate $\mathbf{x}$. For the present problem the near and far field coordinate definition to be used in this section are shown in Figure 3-124. The near field coordinate origin is at the inside corner with y_1 and y_2 representing the streamwise and wall-normal coordinates respectively and y_3 which is the spanwise coordinate is out of the page. Far field observer coordinates x_i are measured in parallel directions.

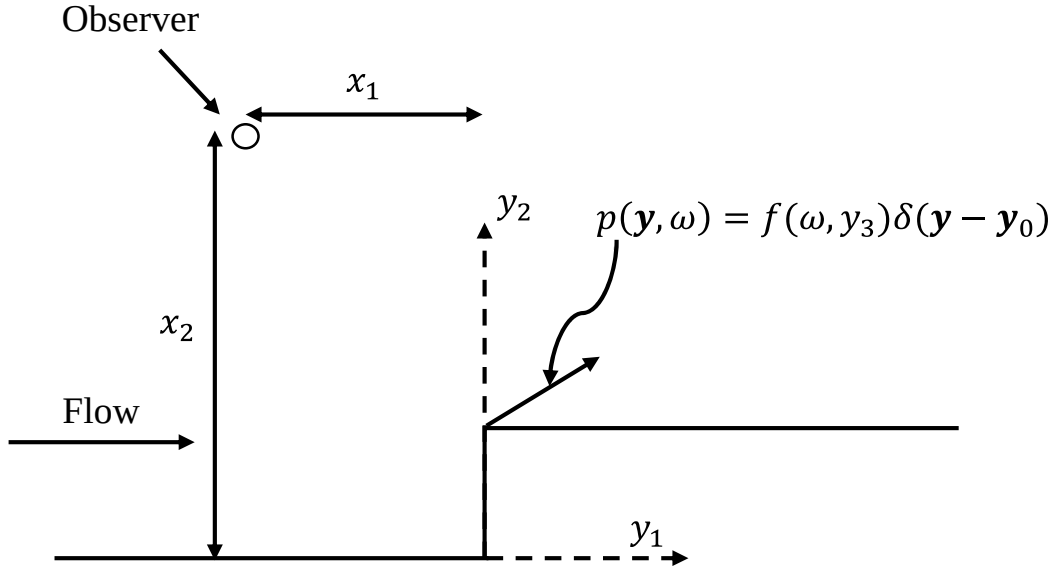


Figure 3-124 Description of problem setup to separate the Green's function from the underlying source spectrum for an unrounded forward step.

Now, as theorized by Glegg *et al.* (2014) and Bryan *et al.* (2015) we assume that the unsteady pressure which is the source of far field sound is focused at the top corner of the step as depicted in Figure 3-124. In the theory this force is a resultant of the forces on the face and downstream surface of the step which are assumed to be the same. Since we are assuming this source force per unit span f to be acting at a point we can write it in terms of Dirac delta function as

$$p(\mathbf{y}, \omega) = f(\omega, y_3) \delta(\mathbf{s} - \mathbf{s}_0) \quad (3-78)$$

where $\mathbf{s}$ is the distance along the step contour and $\mathbf{s}_0$ locates the top corner of the step. Substituting this definition of the source pressure in equation (3-77) we get

$$p(\mathbf{x}, \omega) = - \int_{-\infty}^{\infty} f(\omega, y_3) \delta(\mathbf{s} - \mathbf{s}_0) \frac{\partial G(\mathbf{x}, \mathbf{y}, \omega)}{\partial n} dS(\mathbf{y}) \quad (3-79)$$

According to Bryan *et al.* (2015) the Green's function is independent of y_3 for a two-dimensional step with an observer in the $x_3 = 0$ plane, and so

$$p(\mathbf{x}, \omega) = - \frac{\partial G(\mathbf{x}, y_0, \omega)}{\partial n} \int_{-\infty}^{\infty} f(\omega, y_3) dy_3 \quad (3-80)$$

where $\mathbf{y}_0$ gives the two-dimensional in-plane coordinate (y_1, y_2) of the step corner. Then, taking the spectrum of the above equation we get

$$\frac{1}{T} E[p(\mathbf{x}, \omega) p^*(\mathbf{x}, \omega)] = \left| \frac{\partial G(\mathbf{x}, y_0, \omega)}{\partial n} \right|^2 \frac{1}{T} E \left[\int_{-\infty}^{\infty} f(\omega, y_3) dy_3 \int_{-\infty}^{\infty} f^*(\omega, y_3) dy_3 \right] \quad (3-81)$$

where $*$ represents the complex conjugate of a value, $E[\]$ is the expected value operator and T is the period. Now, the left hand side in the above equation is just the spectral density of the far field pressure, $\Phi_{pp}(\omega)$ and since f is the force per unit span the integral on the right hand side represents the spectral density of spanwise averaged unsteady force which we write as $\phi_{FF}(\omega)$. Then, the equation above is written as

$$\Phi_{pp}(\omega) = \phi_{FF}(\omega) \left| \frac{\partial G(\mathbf{x}, y_0, \omega)}{\partial n} \right|^2 \quad (3-82)$$

where $\phi_{FF}(\omega)$ is the spectral density of the unsteady force. To determine the Green's function we recall from §1.3 that in Glegg *et al.*'s (2014) theory the far field sound is given by

$$\Phi_{pp}(\mathbf{x}, \omega) = \frac{k^2 \phi_{FF}(\omega)}{(2\pi R)^2} \left| \frac{ix_1}{R} \cos\left(\frac{khx_2}{R}\right) + \frac{x_2}{R} \sin\left(\frac{khx_2}{R}\right) \right|^2 \quad (3-83)$$

where the term $\phi_{FF}(f)$ in Glegg *et al.*'s (2014) theory is the resultant of the unsteady loading on the face and the downstream surface placed at the corner of the step, k is the acoustic wavenumber, R is the source to observer distance and x_1 and x_2 are the coordinates as defined in Figure 3-124 above. In the equation above the squared term inside the brackets determines the directionality of the far field sound while the term in the front provides the magnitude which at this point is unknown. Then, setting equations (3-83) and (3-84) equal to each other we realize that

$$\left| \frac{\partial G(\mathbf{x}, y_0, \omega)}{\partial n} \right|^2 = \frac{k^2}{(2\pi R)^2} \left[\frac{ix_1}{R} \cos\left(\frac{khx_2}{R}\right) + \frac{x_2}{R} \sin\left(\frac{khx_2}{R}\right) \right]^2 \quad (3-84)$$

In the two equations above the only unknown quantity now is the spectrum of the unsteady force $\phi_{FF}(\omega)$. This spectrum in the present work was inferred from the far field sound spectra. The non-dimensional form of this curve is shown in Figure 3-125. The normalization used here is the same used for the unsteady surface force in §3.2.4.4. We can now take this non-dimensional source spectrum, dimensionalize it using appropriate variables (free stream velocity and step height) and use it in equation (3-83) to predict the far field sound and then compare the results with measurements.

Figure 3-126 (a)-(e) shows the result of predictions using equation (3-83) and the source spectrum shown in Figure 3-125 with the sound measured for $r/h = 0\%$ forward step for five different free stream

velocities. It can be seen that though the spectrum of ϕ_{FF} shown in Figure 3-125 is just an estimate, when combined with the theory of Glegg *et al.* (2014) it predicts the measured far field sound with good accuracy for each velocity. The predictions also estimate the location of the null associated with destructive interference quite well.

We can now take this source spectrum which we know can predict the far field sound well and compare it with the measured unsteady force spectrum which was measured in present work (as discussed in §3.2.4.4). Figure 3-127 (a) and (b) shows a comparison of the inferred source spectrum with the measured unsteady force spectrum on the face of the step at $U_\infty = 30$ m/s and 40 m/s respectively. The frequency axis has been restricted between $1000 \text{ Hz} \leq f \leq 3090 \text{ Hz}$ where the lower limit is set by the frequencies above which the far field sound results were obtained in the present work and the upper limit is set by the frequency up to which the unsteady force array approximates a spanwise infinite uniform sensor. Looking at this figures it can be seen that the measured force is consistently higher by about 20-25 dB throughout the frequency range at both velocities. This quantitative discrepancy is perhaps because the measured spectrum shown here is force per unit area and the proper length scale to derive the true source spectrum from it remains an unknown. Furthermore, there also seems to be a slight difference in the slopes of the measured and the inferred spectra as well with the former having a slightly higher roll-off with frequency. If we now compare the inferred source spectrum to the force spectrum measured 1.3 step heights downstream of the step as shown in Figure 3-128 (a) and (b) for $U_\infty = 30$ m/s and 40 m/s respectively we see that the measured spectra are again higher with a slightly different slope. Recall that the measured spectra in the Figure 3-127 and Figure 3-128 are representative of the unsteady forces on the two surfaces of the step which in Glegg *et al.*'s (2014)/Bryan *et al.*'s (2015) theory are the same. We have already seen in §3.2.4.4 that this might not be the case. The results here further show that the forces on these two surfaces are also slightly different than a source spectrum which accurately predicts the measured sound using the theory. Therefore, the feasibility of the application of measurements to predict the far field sound remains an unknown at this point. We will consider this point in more detail in next section where the measured unsteady forces are directly applied to Glegg *et al.*'s (2014)/Bryan *et al.*'s (2015) theory to predict the far field sound.

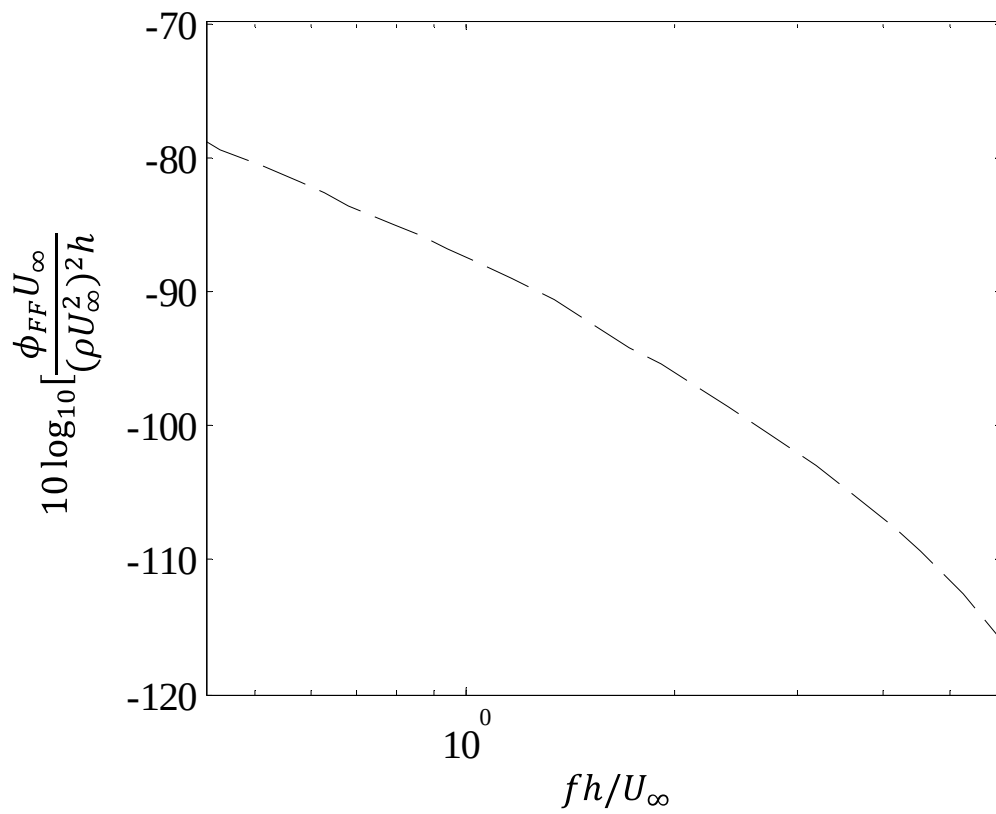


Figure 3-125 Non-dimensional form of the inferred sound source spectrum for an unrounded forward step.

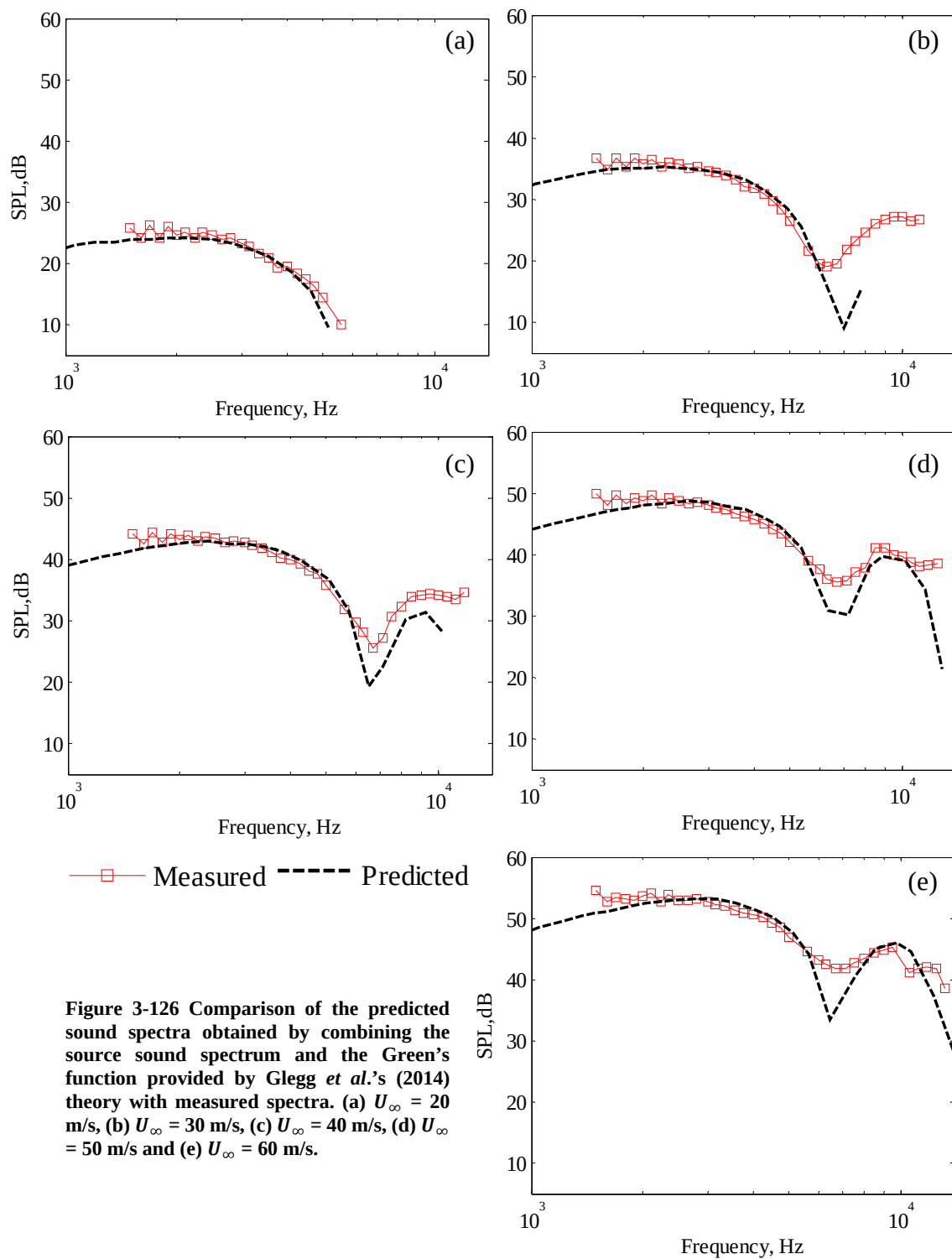


Figure 3-126 Comparison of the predicted sound spectra obtained by combining the source sound spectrum and the Green's function provided by Glegg *et al.*'s (2014) theory with measured spectra. (a) $U_\infty = 20$ m/s, (b) $U_\infty = 30$ m/s, (c) $U_\infty = 40$ m/s, (d) $U_\infty = 50$ m/s and (e) $U_\infty = 60$ m/s.

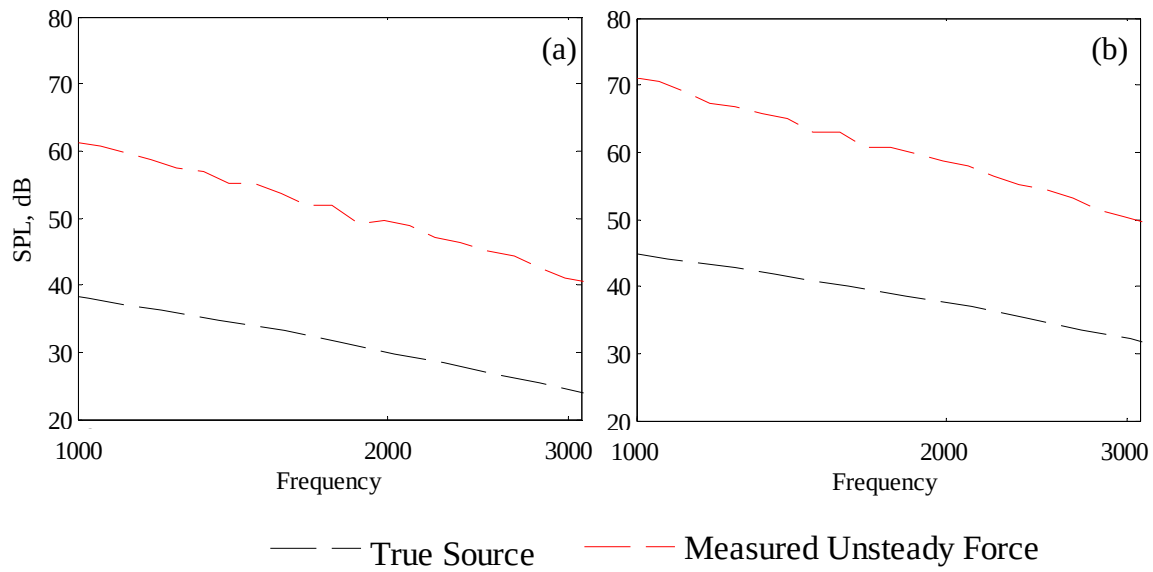


Figure 3-127 Comparison of the true spectrum with the unsteady force spectrum measured on the face of the $r/h = 0\%$ forward step. (a) $U_\infty = 30$ m/s and (b) $U_\infty = 40$ m/s.

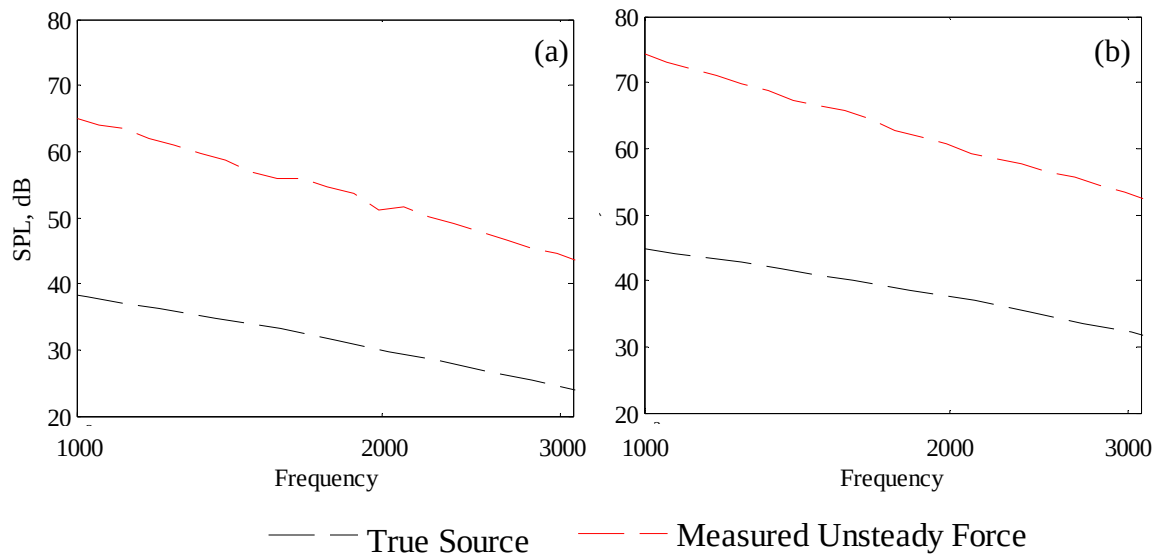


Figure 3-128 Comparison of the true spectrum with the unsteady force spectrum measured downstream of $r/h = 0\%$ forward step at $x/h = 1.3$. (a) $U_\infty = 30$ m/s and (b) $U_\infty = 40$ m/s.

3.2.8 Direct Application of Measurements to Theory of Glegg *et al.* (2014)

We will now apply the results obtained for the spatially averaged unsteady pressure $\bar{\phi}_{pp}$ in §3.2.4 directly to the theory of Glegg *et al.* (2014) (see §1.3.1 for details of the theory). In the previous section we have seen that this theory is able to accurately predict the measured far field sound once the source spectrum is known. However, the source spectrum used in the previous section was inferred from measurements of far field sound and does not rely on measured data. Nonetheless it was shown there (see Figure 3-128) that the source spectrum extracted from the far field sound is qualitatively similar to the measured unsteady force in the vicinity of the forward steps. Furthermore, it was also suggested in previous section that the quantitative disagreement might be a result of an unknown length scale which can be used to convert the area averaged unsteady pressure to unsteady force on the surface. In this section we will directly apply the near field unsteady force measurements to the theory of Glegg *et al.* (2014) and attempt to gain some insight on how the unsteady force might be related to the far field sound. To do so however, we must take into account the fact that the theory assumes that the forces on the two surfaces of the step are the same but the measurements shown in §3.2.4.4 indicate that these forces may not be the same. Therefore, we assume that the force per unit length on the step face and the downstream surface referred to as f_{face} and f_{down} respectively are different and uncorrelated. Recall that if the contribution of the quadrupole sources in the boundary layer to the radiated sound is ignored the far field acoustic pressure in Curle's (1955) theory as the following integral

$$p(\mathbf{x}, \omega) = - \int_S p(\mathbf{y}, \omega) \frac{\partial G(\mathbf{x}, \mathbf{y}, \omega)}{\partial n} dS(\mathbf{y}) \quad (3-85)$$

The solution to the above equation for a forward step assuming that the surface pressure is acoustically compact as given by Glegg *et al.* (2014) and Bryan *et al.* (2015) is

$$p(\mathbf{x}, \omega) = \frac{ke^{ikR}}{2\pi R} \int_{-\infty}^{\infty} \left[\frac{ix_1}{R} \cos\left(\frac{khx_2}{R}\right) f_{face}(y_3, \omega) + \frac{x_2}{R} \sin\left(\frac{khx_2}{R}\right) f_{down}(y_3, \omega) \right] dy_3 \quad (3-86)$$

here $k = \omega/c$ is the acoustic wavenumber, h is the step height, R is the source to observer distance, x_1 and x_2 are the coordinates which were already defined in previous section in Figure 3-124. Note that the distance $R = 2.54$ m for present work (the distance from inside step corner to center of phased array). We can separate the two terms within the integral above as

$$p(\mathbf{x}, \omega) = \frac{ke^{ikR}}{2\pi R} \left[\int_{-\infty}^{\infty} \frac{ix_1}{R} \cos\left(\frac{khx_2}{R}\right) f_{face}(y_3, \omega) dy_3 + \int_{-\infty}^{\infty} \frac{x_2}{R} \sin\left(\frac{khx_2}{R}\right) f_{down}(y_3, \omega) dy_3 \right] \quad (3-87)$$

Since the $\sin$ and $\cos$ terms in the integrals above are independent of the variable of integration we can pull them outside the integral which yields

$$p(\mathbf{x}, \omega) = \frac{ke^{ikR}}{2\pi R} \left[\frac{ix_1}{R} \cos\left(\frac{khx_2}{R}\right) \int_{-\infty}^{\infty} f_{face}(y_3, \omega) dy_3 + \frac{x_2}{R} \sin\left(\frac{khx_2}{R}\right) \int_{-\infty}^{\infty} f_{down}(y_3, \omega) dy_3 \right] \quad (3-88)$$

We are interested in the spectral density of the far field acoustic pressure defined as

$$\begin{aligned}
\Phi_{pp}(\omega) &= \frac{1}{T} E[p(\mathbf{x}, \omega) p^*(\mathbf{x}, \omega)] \\
&= \frac{k^2}{(2\pi R)^2} \left\{ \left| \frac{ix_1}{R} \cos\left(\frac{khx_2}{R}\right) \right|^2 \frac{1}{T} E \left[\int_{-\infty}^{\infty} f_{face}(y_3, \omega) dy_3 \int_{-\infty}^{\infty} f_{face}^*(y_3, \omega) dy_3 \right] \right. \\
&\quad \left. + \left| \frac{x_2}{R} \sin\left(\frac{khx_2}{R}\right) \right|^2 \frac{1}{T} E \left[\int_{-\infty}^{\infty} f_{down}(y_3, \omega) dy_3 \int_{-\infty}^{\infty} f_{down}^*(y_3, \omega) dy_3 \right] \right\}
\end{aligned} \tag{3-89}$$

Now, the two terms within the expected value operator ($E[\]$) represent the spectral density of the unsteady forces on the two surfaces, abbreviating the spectral density of unsteady force on the step face as $\phi_{FF,face}$ and that on the downstream surface as $\phi_{FF,down}$ we get the following expression for the far field sound

$$\Phi_{pp}(\omega) = \frac{k^2}{(2\pi R)^2} \left[\phi_{FF,face}(\omega) \left| \frac{ix_1}{R} \cos\left(\frac{khx_2}{R}\right) \right|^2 + \phi_{FF,down}(\omega) \left| \frac{x_2}{R} \sin\left(\frac{khx_2}{R}\right) \right|^2 \right] \tag{3-90}$$

In the above formulation the two unsteady surface force terms can be obtained from the measurements of unsteady forces discussed in §3.2.4. However, note that the measurements there were presented as spatially averaged unsteady pressure which are equivalent to the unsteady force per unit area. Therefore, we need a spanwise length scale and a streamwise length scale to estimate the unsteady force from the measurements. As a crude approximation we can select the entire step height as the length scale for the step face and the reattachment length as the scale for the downstream surface. For the spanwise scale in each case we choose a unit span. Then, the forces on the two surfaces over a unit span are given by

$$\phi_{FF,face}(\omega) = \bar{\phi}_{pp,face}(\omega) h^2 \tag{3-91}$$

and,

$$\phi_{FF,down}(\omega) = \bar{\phi}_{pp,down}(f) x_r^2 \tag{3-92}$$

here $\bar{\phi}_{pp,down}(f)$ is taken to be the spatially averaged unsteady pressure measured $x/h = 1.3$ downstream of the step, $\bar{\phi}_{pp,face}(f)$ is the spatially averaged unsteady pressure measured on the step face, h is the step height and x_r is the reattachment length. Since we have chosen unit span to scale the spatially averaged unsteady pressure we re-integrate the beamformed maps for $r/h = 0\%$ with a unit span region i.e. $-0.5 \text{ m} \leq Y \leq 0.5 \text{ m}$ to be consistent (see §3.2.6.4 for details on integration of beamformed sound maps).

Figure 3-129 (a) and (b) shows the comparison between measurements and predictions made using equations (3-90)-(3-92) for free stream velocities of 30 m/s and 40 m/s respectively. Note that in using the spatially averaged unsteady pressure to approximate the unsteady force we have removed the restriction that the present microphone sensor approximates and ideal spanwise infinite sensor only up to a frequency of 3090 Hz. Instead, we have used the measurements for predictions throughout the frequency range shown on these plots. Further note that since measurements of x_r are not available for $U_\infty = 40 \text{ m/s}$ we have obtained it through linear interpolation between $U_\infty = 30 \text{ m/s}$ and 60 m/s for which measurements of x_r were made. Looking at these figures we see that the predictions approximate the overall shape of the measured spectra quite well for both velocities including the null due to reflections from the upstream surface of the step. Quantitatively, the predictions are within about 3.5 dB at low to mid frequencies. Around the frequencies where the null appears the predictions are more than 25 dB lower. At frequencies above the spectral dip the predictions begin to rise in the same manner as seen in measurements but are about 7 dB lower.

The quantitative disagreement between the predictions and measurements seen in Figure 3-129 is possibly a combination of array measurement uncertainty and the assumption that the unsteady force within the entire downstream separation bubble is the same. Furthermore, the uncertainty in measurement of the separation bubble length using oil flow visualization (see §3.2.1 for these uncertainties) could also be a contributing factor. The uncertainty in array measurement was earlier discussed in §3.2.6.5. It was shown in that section using the same directivity function used for predictions here that at frequencies close to where the spectral dip appears in the forward step sound spectrum (6,000 Hz – 7,000 Hz) the uncertainty in the array measurements is large (see Figure 3-115) due to the sound being highly directional. This high uncertainty due to the array directivity would explain the higher sound levels and smoother shape of the spectral dip seen in the measurements compared to the predictions. The quantitative disagreement at frequencies beyond those associated with the destructive interference is likely a result of attenuated response of the surface pressure microphone array at these frequencies as well as the uncertainty in the phased array measurements associated with the integration span of unit width used to obtain the frequency spectrum. Recall that to be consistent with the definition of unsteady surface forces we chose a unit span over which the source maps from the phased array were integrated to obtain the sound spectrum. In reality, this integration region is not exactly equal to a sound producing span of unit width. This limitation of the array measurements was also discussed in §3.2.6.5 where it was shown that at frequencies higher than about 8,000 Hz the integration span output can differ from actual sound producing span output by approximately 1.5-2 dB. Lastly, while making quantitative comparisons between the measurements and predictions it must also be remembered that the beamforming procedure used to obtain the frequency spectrum assumes that the observer is located at infinity (far field) and the acoustic waves which are assumed to be spherical arrive at each microphone from the same direction. However, since the array is at a finite distance this assumption may not be true which would lead to individual microphones in the array experiencing different propagation direction. This would result in the array output being slightly higher than for an observer located in the true far field when normalized on the same propagation distance.

Despite of the quantitative differences however, the predictions are remarkably accurate in predicting the overall shape of the sound spectrum. This shows that the theory of Glegg *et al.* (2014) could be a viable method to predict the far field sound from forward steps given that proper length scale needed to obtain the true unsteady force spectrum are known.

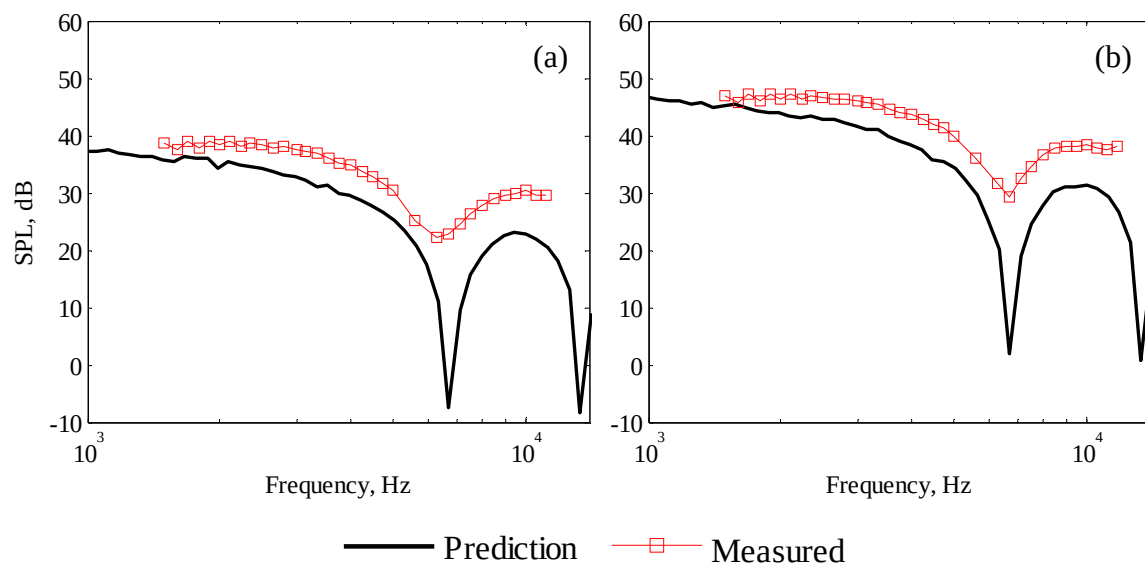


Figure 3-129 Comparison of measured far field sound from unrounded forward step with those predicted by applying the unsteady force measurements on step face and downstream surface to the theory of Glegg *et al.* (2014)/Bryan *et al.* (2015). (a) $U_{\infty} = 30$ m/s and (b) $U_{\infty} = 40$ m/s.

Even though the exact length scale needed to obtain the true force per unit span spectrum on either surface remains speculative we can gain more insight on the contribution of the forces on the two surfaces of the step to the far field sound by considering their effects separately on the sound predictions. To do so we first eliminate the force measured on the step face and assume that the force per unit span is the same everywhere within the downstream separation bubble. In this case the far field sound spectrum is given by

$$\Phi_{pp}(\omega) = \frac{k^2}{(2\pi R)^2} \left[\phi_{FF,down}(\omega) \left| \frac{x_2}{R} \sin\left(\frac{khx_2}{R}\right) \right|^2 \right] \quad (3-93)$$

Note that the far field sound now has a sine directionality which is associated with a normal-dipole consistent with the assumption that the force on the downstream surface is solely responsible for sound generation. The predictions obtained using the above equation for $U_\infty = 30$ m/s and 40 m/s are shown in Figure 3-130 (a) and (b) respectively. The predictions of Figure 3-129 made by including forces on both surfaces have also been repeated in these figures for the sake of comparison. We see that excluding the unsteady force on the step face has negligible effect on the predictions and they are still in good qualitative agreement with the measurements. This suggests that the force on the step face does not play an important role in sound generation at this observer location. The reason for this is that the force on step face represents

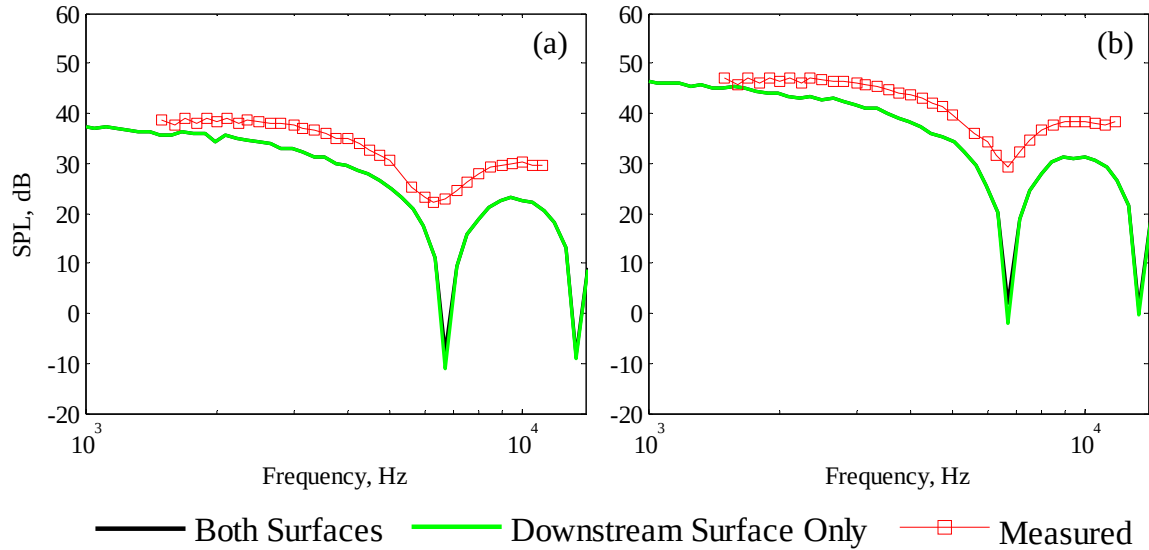


Figure 3-130 Comparison of measured far field sound from unrounded forward step with predictions made using theory of Glegg *et al.* (2014)/Bryan *et al.* (2015) by assuming the unsteady force on the downstream surface of the step is the only contribution to the overall sound source. Also shown are the predictions made assuming unsteady force on both the step face and downstream surface contributes to the far field sound.

a drag dipole which has a cosine directionality associated with it (as seen in equation (3-90)) and so the contribution from this source would become negligible in the region directly above the step due to the radial angle of the observer approaching 90°. Thus, the fact that the force on the downstream surface accounts for most of the predicted sound does not exclude the possibility that the force on the step face would begin to play a role in sound generation further upstream or downstream of the step location. Qualitatively, the results of Figure 3-130 are in agreement with observation of Slomski (2011) who also found that in the region directly above the step the downstream surface of the step up to 5 step heights accounts for most of the noise being generated.

We can now eliminate the force on the downstream surface and assume the entire force spectrum is given by the measured spatially averaged unsteady pressure on the step face scaled by the step height. The far field sound spectrum is then given by

$$\Phi_{pp}(\omega) = \frac{k^2}{(2\pi R)^2} \left[\phi_{FF,face}(\omega) \left| \frac{ix_1}{R} \cos\left(\frac{kx_2}{R}\right) \right|^2 \right] \quad (3-94)$$

Note now that the far field sound has a cosine directionality which is consistent with the direction of force on the step face. The predictions obtained by using the above expression are shown in Figure 3-131. As expected, these predictions are much lower (~30 dB or more) compared to the measured spectrum. Also note that the predictions approximate the shape of the measured spectrum only at very low frequencies and have two nulls which are located at frequencies which correspond to half acoustic wavelengths equal to twice the step height and half the step height.

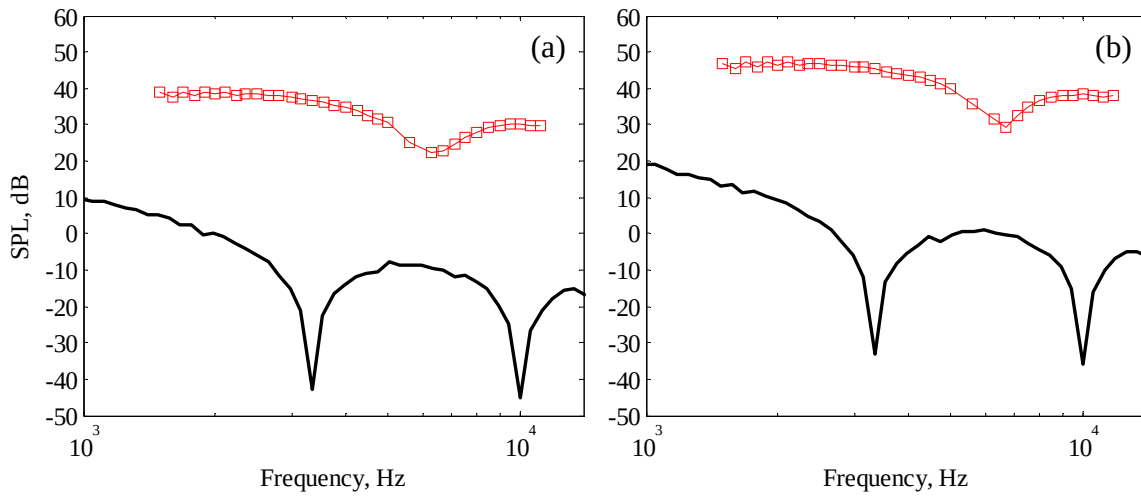


Figure 3-131 Comparison of measured far field sound from unrounded forward step with predictions made using theory of Glegg *et al.* (2014)/Bryan *et al.* (2015) by assuming the unsteady force on the step face is the only contribution to the overall sound source. Legend same as Figure 3-129.

3.2.9 Results from Flow over Circular Embossment

We will now begin discussion of near and far field measurements made for flow over circular embossment. Recall that the height of the embossment (h_D) is 25.4 mm and is same as the height of the forward step used in the present work. The radius of the embossment is $r_D = 457.2$ mm and the aspect ratio defined as the height to diameter ratio ($h_D/2r_D$) is 0.027. In the following sections we will first consider some key features of the flow over the embossment through oil flow visualization measurements. This will be followed by a discussion on unsteady surface forces measured on the face and downstream surface of the embossment using a linear microphone array. We will then look at the far field sound measurements for the embossment made using 117 microphone phased array. Finally, we will consider the relationship between the sound from two-dimensional forward step and three-dimensional circular embossment.

3.2.10 Flow over Circular Embossment

The mean characteristics of flow over the circular embossment were explored using oil flow visualization which was performed using the same method as for forward facing steps. The details of the flow visualization setup therefore is the same as described in §3.1.3 for forward steps. The flow visualization was performed for one flow speed of $U_\infty = 60$ m/s which corresponds to a Reynolds number based on embossment height of approximately $Re_h = 92,700$.

Figure 3-132 shows some key features of flow over circular embossment obtained using oil flow visualization. The flow approaching the embossment separates in the same manner as for a forward step except this separation region appears to conform to the curved leading edge of the geometry. The spanwise extent of this separated flow region is limited to certain angle on either side of the spanwise center beyond which the flow remain attached and the streamlines curve around the embossment. The separated flow region on the top surface and subsequent reattachment is also curved and has a limited spanwise extent similar to the flow upstream. Note that in the image below there is a perpendicular line which is the seam of the contact paper used for flow visualization and must not be confused with the flow reattachment line which is upstream of it. The flow around the spanwise edges of the embossment remains attached and the streamlines begin to straighten due to convection from mean flow. At the trailing edge of the embossment the flow separates again as it would from the corner of a backward step but similar to the leading edge separated flow this flow region is also curved. Additionally there is a small region near the spanwise center of the embossment where a significant accumulation of the oil flow mixture can be seen. Bryan (2015) who performed RANS simulations over a circular embossment with similar aspect ratio ($h_D/2r_d = 0.025$) and Reynolds number ($Re_h = 94,440$ m/s) observed a similar feature and have attributed this to streamlines released from the leading edge which spiral around the spanwise edges and then bifurcate leaving some into the mean flow and some into the wake of the embossment. Their simulations also show a leading edge separation and reattachment region which is similar to a forward facing step but is curved as revealed by the flow visualization in Figure 3-132.

The mean flow features in Figure 3-132 suggest that the separation/reattachment regions at the leading edge and trailing edge of the embossment behave like regions found in forward step and backward step flows respectively. Since it is well established that the forward step is much louder than a backward step of the same height (see Farabee and Zoccola (1998) and Catlett *et al.* (2014) for example) we would expect the leading edge of the embossment to be a dominant noise source. Therefore similarities between the separated flow at the leading edge of the embossment and flow over a forward step are worth examining. Figure 3-133 shows the separation and reattachment lengths at the leading edge and spanwise center of the embossment through an image analysis procedure similar to that used for forward steps (see §3.1.3 for details on this procedure). The uncertainties associated with these lengths which arise due to uncertainty in both image clarity and the unsteadiness of the two separation bubbles have also been shown on the figure. Note that the uncertainty in upstream separation length is primarily due to oil streaks crossing into the separated flow region while the uncertainty in downstream reattachment length is primarily due to unsteadiness of the flow reattachment which causes the reattachment point to fluctuate in the streamwise

direction. Thus, for the latter case the value reported as reattachment length is the streamwise center of the observed reattachment zone (similar procedure was also adopted for forward facing step case) and the uncertainty is the approximate width of this zone on either side of this center.

The separation length upstream of the embossment at its spanwise center is approximately $x_s = 1.9h \pm 0.6h$ which is slightly larger than that for forward step of the same height ($x_s = 1.5h \pm 0.34h$) as discussed in §3.2.1. However, accounting for the higher uncertainty associated with the present calculation it is difficult to definitively draw this conclusion. The reattachment length downstream of the embossment is approximately $x_R = 2.5h \pm 0.5h$ which is slightly smaller than that for the forward step ($x_r = 3.2h \pm 0.43h$) of the same height and same Reynolds number reported in §3.2.1. This result is consistent with RANS simulations of Bryan (2015) who have found the reattachment length for an embossment to be slightly shorter than the forward step of the same height.

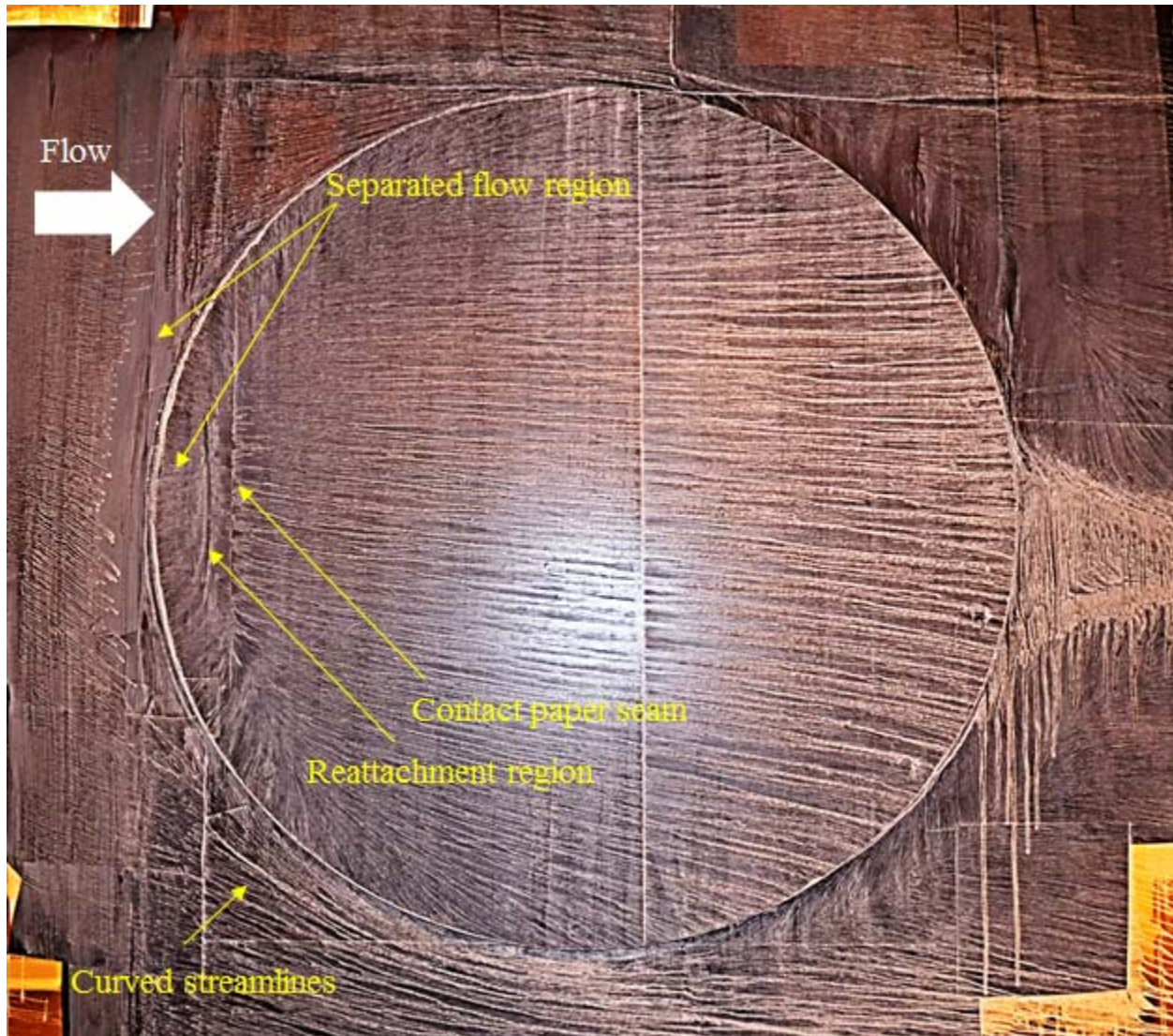


Figure 3-132 Mean flow features for three-dimensional circular embossment obtained using oil flow visualization at $U_\infty = 60$ m/s.

Figure 3-133 also shows an approximate radial range of the separation bubble along the leading edge of the embossment. This range was calculated by considering the approximate location along the spanwise direction at which the flow visualization images show attached streamlines upstream of the

embossment and it is then assumed that the downstream reattachment region also has the same radial range. This was done because it was easier to identify the upstream separated flow region than the reversed flow region downstream of the embossment. This approach shows that the separated flow region spans approximately 42 degrees on either side of the spanwise center of the embossment (assuming spanwise symmetry). This result suggests that if the separation bubbles on the leading edge of the embossment are the dominant noise source then the spanwise extent of this source must be limited. This point will be further explored in the next section where the beamformed sound source maps from the embossment will be presented.

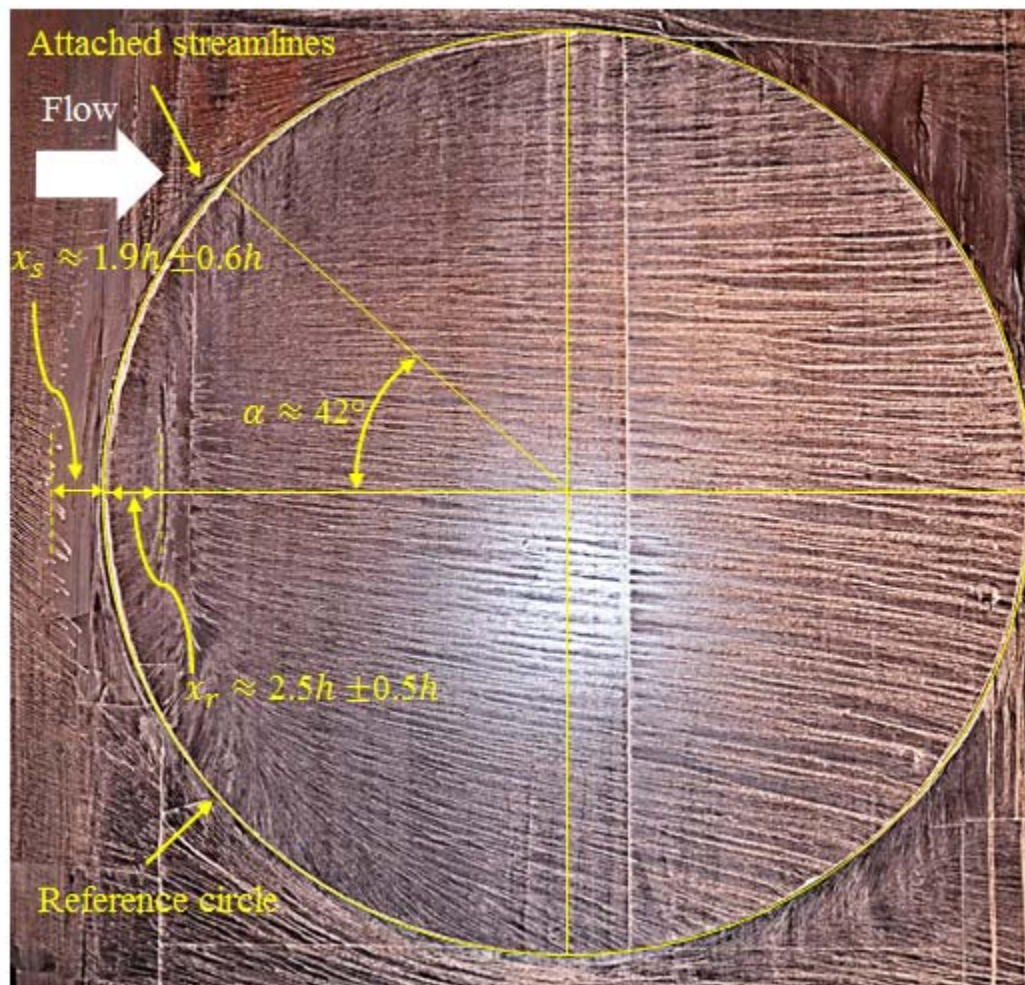


Figure 3-133 Leading edge separation and reattachment region for flow over circular embossment at $U_{\infty} = 60$ m/s.

3.2.1 Unsteady Surface Forces on Circular Embossment

Let us now consider the spatially averaged unsteady pressure which is equivalent to the unsteady force on the face and downstream surface of the circular embossment. As shown in §3.1.9 these measurements were made using an array of 7 B&K 4190 microphones which have a sensitive diameter of 11.1 mm. Figure 3-134 (a) and (b) shows the microphone array configuration on the top surface and face of the embossment respectively. The downstream array (Figure 3-134 (a)) is such that the center of each of its element is 1 step height downstream of the top corner of the embossment and the separation between each element (center to center) is $d = 14.9$ mm. Each element is spaced apart by 1.8° so that elements at the spanwise extremities of the array subtend an angle of 5.4° to the center of the embossment. The array on the face of the embossment (Figure 3-134 (b)) also has 7 microphones but the spacing between each

element (center to center) is slightly larger ($d = 50.8$ mm). The microphones at the spanwise extremities of this array subtends an angle of 19.2° to the center of the embossment. Each microphone in the array is placed $0.38h$ above the inside corner of the embossment. Note that the radial angles of the microphone centers are defined positive clockwise such that $\alpha = 0^\circ$ is the microphone which is aligned with the spanwise center of the embossment as shown in the figure.

The analysis to compare the discrete array response to a spanwise infinite strip sensor with uniform response done in §3.2.4.3 for the 24 microphone linear array was extended to both the array configurations shown in Figure 3-134. The analysis showed that this array (in both its downstream and face configurations) approximates a 7 mm wide spanwise infinite strip up to 2890 Hz (± 2 dB). Recall that the linear array used for forward steps approximated a uniform sensor of the same width but up to a slightly higher frequency (3100 Hz). It must be noted that in the present analysis of array response we have assumed the flow over the array to be spanwise homogenous. The feasibility of this assumption can be assessed by looking at the spanwise variation of the surface pressure spectra measured by individual microphones in the array. Figure 3-135 (a) and (b) shows the autospectral densities measured by individual microphones at $U_\infty = 30$ m/s for the top surface and the face array respectively. We see here that there is not much spanwise variation in the pressure spectra in either case possibly because of small spanwise extent of the microphone array. Note that we have not shown the spectra for the microphone at $\alpha = -1.8^\circ$ in Figure 3-135 (a) due to spurious response of the microphone in this particular case.

Figure 3-136 shows the unsteady forces on the two surfaces of the embossment scaled on the free stream velocity, dynamic pressure and the embossment height. Recall that this is the same scaling used to collapse the unsteady forces for forward steps (see Figure 3-87) and is given by

$$\bar{\phi}_{pps}(f_s) = \frac{\bar{\phi}_{pp}(f)U_\infty}{(\rho U_\infty)^2 h} \text{ and } f_s = fh/U_\infty \quad (3-95)$$

We see that this scaling also collapses the unsteady forces measured on the two surfaces of the embossment quite well. Furthermore, if we compare the overall shape of these spectra to those for forward step in Figure 3-87 we see a resemblance between the two which is reminiscent of the similarity seen in the far field sound spectra for the two geometries.

We can further consider the similarity between the two and three-dimensional geometries by directly comparing the unsteady forces. To do this, the unsteady force for the forward step was recalculated by spatially averaging the pressure signal from the same number of microphones as used in the present array. Note that the separation between the array microphones for the forward step is $d = 14.9$ mm which is the same as that for the embossment array used on the downstream surface and so it approximates the same spanwise infinite uniform sensor up to the same frequency i.e. 2890 Hz. Figure 3-137 (a) and (b) shows the comparison of unsteady forces between the two-dimensional forward step and three-dimensional circular embossment measured on the downstream surface and the face of each geometry respectively. We see that the unsteady force spectra for each of the geometry have a remarkable similarity with the spectra on the face (Figure 3-137 (b)) showing quantitative agreement as well. The spectra measured on the downstream surface (Figure 3-137 (a)) for the embossment has the same shape as forward step but it is about 2 dB higher at lower frequencies and about 4 dB higher at higher frequencies. This discrepancy might be because the measurements for the forward step were made further downstream on the step surface ($x/h = 1.3$) than the present measurements ($x/h = 1$). Additionally, as suggested by the flow visualization results in the previous section the separation bubble might be smaller for the embossment compared to the forward step. This means that in terms of reattachment length the measurement location for embossment is much closer to the top corner and this might be the reason for higher levels of unsteady force seen here. Despite of these quantitative differences the equivalency of the unsteady forces in two-dimensional and the three-dimensional discontinuities correlates well with the equivalency seen in the far field sound from the two geometries.

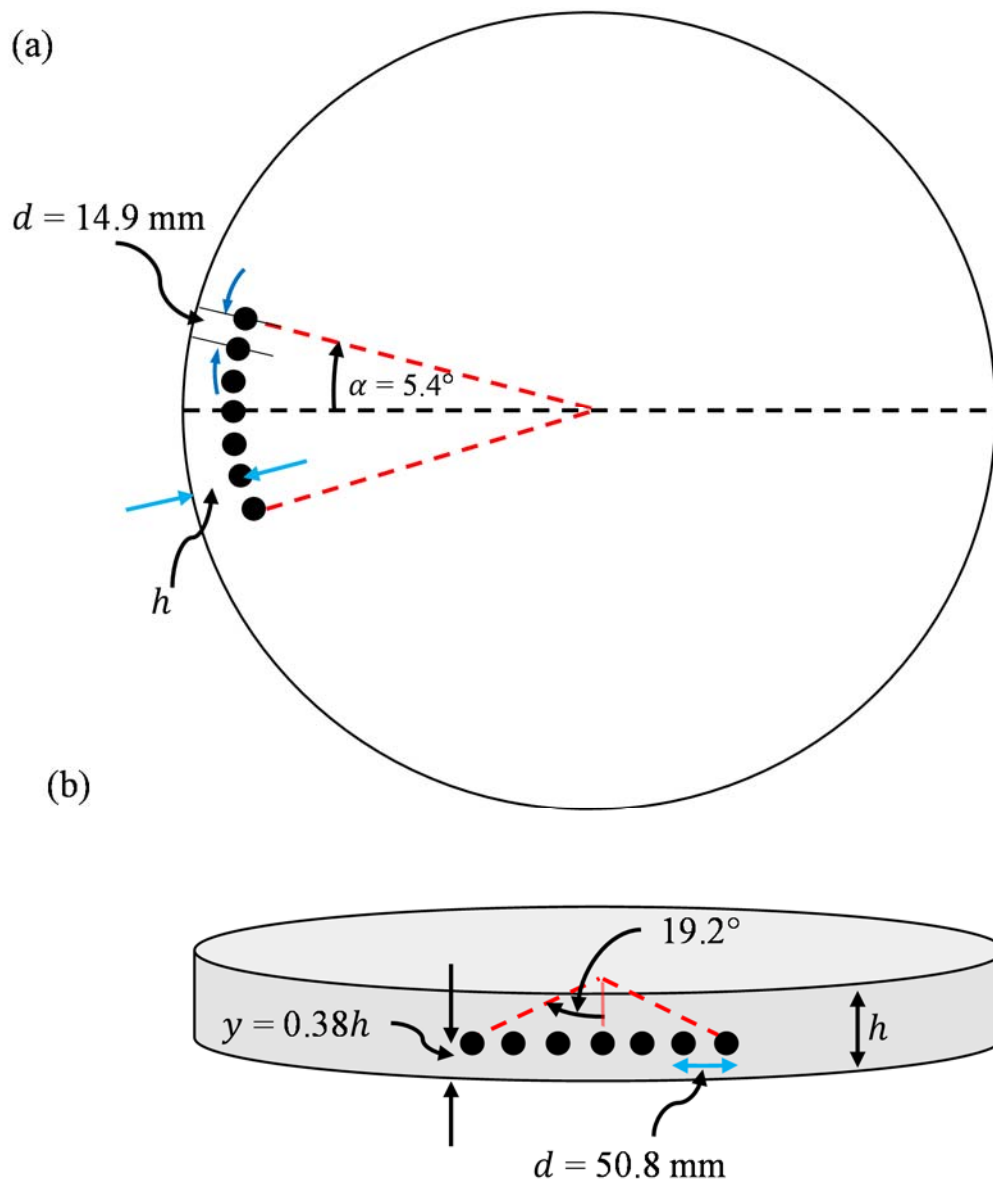


Figure 3-134 Description of 7 microphone linear array used to measure unsteady surface forces on circular embossment. (a) Array on the downstream surface of the embossment and (b) Array on the face of the embossment.

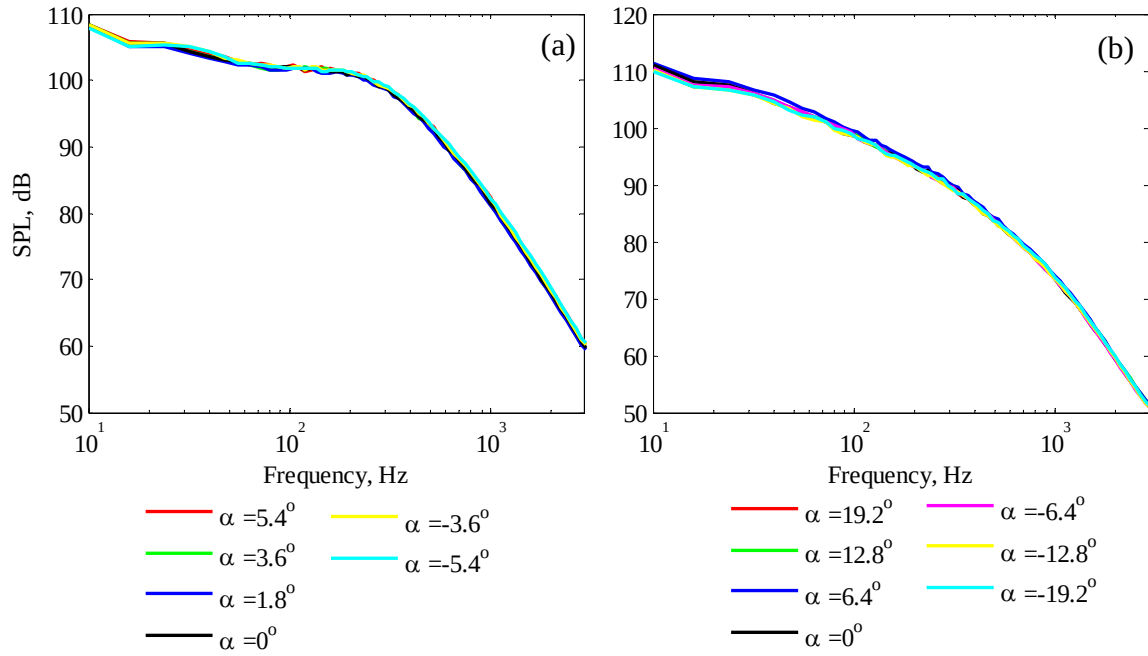


Figure 3-135 Autospectral densities measured by individual microphones in the array on circular embossment at $U_\infty = 30$ m/s. (a) Downstream surface array and (b) Face array.

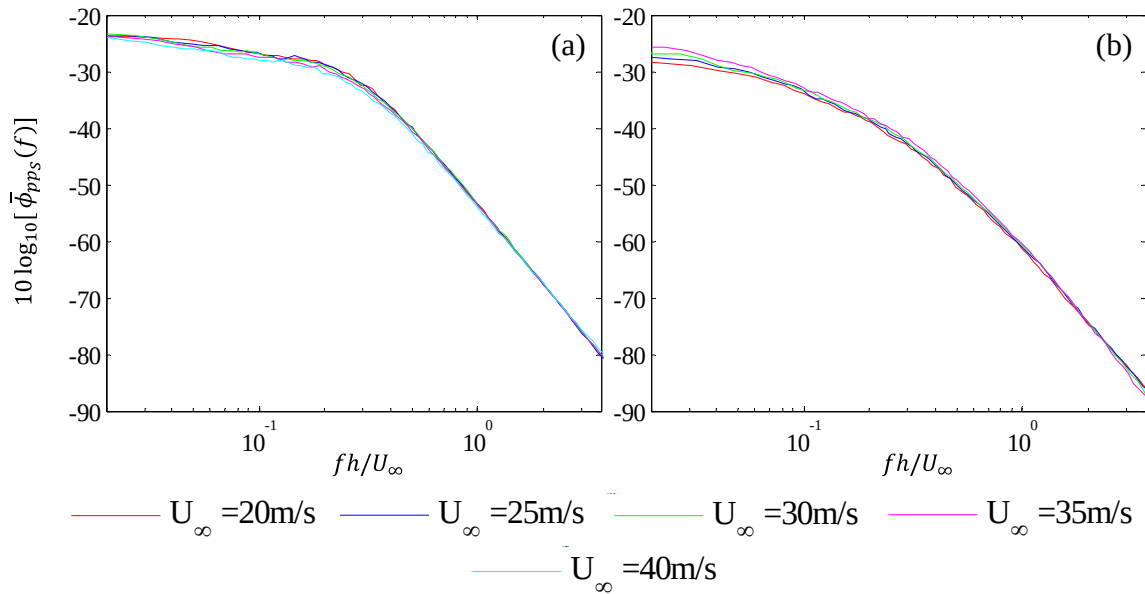


Figure 3-136 Unsteady force spectra for circular embossment scaled on the free stream velocity and step height. (a) Downstream surface and (b) Face.

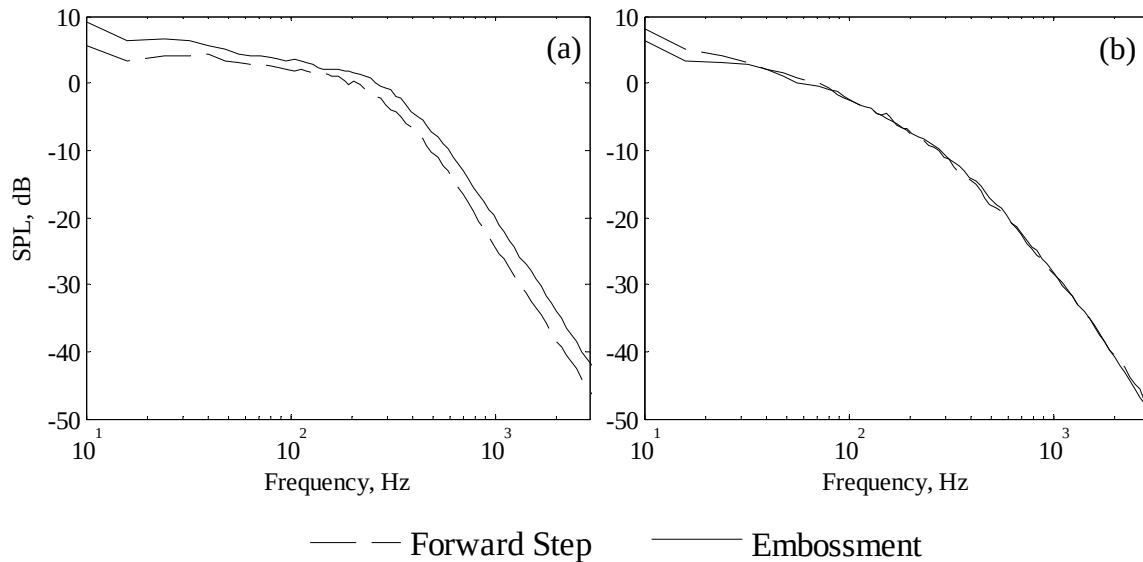


Figure 3-137 Comparison between unsteady surface forces on the two surfaces of the forward step and the circular embossment at $U_{\infty} = 30$ m/s. (a) Downstream surface and (b) Face. Note that the unsteady forces for the forward step were derived by spatially averaging the pressure signal from the same number of microphones as used in the embossment array.

3.2.2 Far Field Sound from Circular Embossment

The far field sound from the wall mounted circular embossment of height 25.4 mm and diameter 914.4 mm was measured using the 117 microphone phased array installed in the same configuration and location in the port side anechoic chamber. The array data was processed and beamformed in the same manner as that for the forward steps (as explained in §3.1.6). The deconvolution of the beamformed output was performed using the CLEAN-er procedure which is based on spatial coherency. This procedure is same as that used for the forward steps except the boundaries of the region of interest used to reject the facility noise are different. This is because unlike forward steps the embossment geometry is three-dimensional and has a finite length (equal to the diameter of the embossment) in both the spanwise and streamwise directions. In the spanwise direction the boundaries of deconvolution region are the same as that forward steps ($-0.75 \text{ m} \leq Y \leq 0.75 \text{ m}$) and far exceed the spanwise dimension of the embossment. In the streamwise direction, the upstream boundary of the deconvolution region is the same as that for forward steps ($X = -0.6 \text{ m}$). However, in the downstream direction the boundary is defined at $X = 1.2 \text{ m}$ and exceeds the trailing edge of the embossment by about 11 embossment heights. This ensures that any contamination of the noise from the trailing edge portion of the embossment by the facility noise is rejected.

Figure 3-138 shows the sound source map for the circular embossment in flow at $U_{\infty} = 30$ m/s for a single frequency of 2,000 Hz. Also shown on the map is the location of the circular embossment and the boundaries of the deconvolution region. Looking at the figure it can be seen that the beamforming and the subsequent deconvolution procedure isolate the noise from the circular embossment which is mainly focused at the leading edge of the embossment with the maximum sound level within the map occurring around the spanwise center of this edge. This behavior of the sound source can be expected because of two reasons. Firstly, as shown by previous studies (e.g. Catlett *et al.* (2014), Farabee and Casarella (1986)) the noise from forward steps can be an order of magnitude higher than a backward step of the same height. This was also demonstrated in the present work when the results from measurements on flow over two-dimensional gaps (see §2.2.5) showed that the radiated sound was dominated by the forward step component of the gap. Secondly, the oil flow visualization results in the previous section have shown that the spanwise extent of the leading edge separation bubble which is similar to that for a forward step case is

limited. The sound source map in Figure 3-138 also shows a limited spanwise extent of the noise source and this suggests that the leading edge separation bubble might be responsible for entire sound generation in flow over circular embossment.

The finite spanwise extent of the noise source in Figure 3-138 can also be explained using sweep independence principle. Recall that the measurements in the wall-jet facility on swept forward steps (see §2.2.6) showed that as a forward step is swept with respect to the flow the radiated sound consistently drops with increasing sweep angle. The results also showed that assuming a spanwise homogeneity, the sound from swept forward steps scale with the component of undisturbed edge velocity perpendicular to the vertical face of the step. We can consider the leading edge portion of the circular embossment to be a composed of swept forward steps with sweep angle increasing from $\alpha = 0^\circ$ (parallel the streamwise velocity vector) to $\alpha = \pm 90^\circ$ (perpendicular to the streamwise velocity vector) on either side of spanwise center of the embossment. Then, extending the results from swept steps in wall-jet facility one can see that as one moves away from the spanwise center of the embossment the step-normal component of the velocity would drop due to cosine directionality and this would qualitatively explain the source behavior seen in Figure 3-138. Based upon a visual observation of the source map the sound levels are reduced by about 7-8 dB on either side of the spanwise center of the embossment at an angle which is approximately 45° . Based on the 5th power of velocity scaling which was used to collapse the swept steps sound spectra, the sound for a 45° swept step must be reduced by 7.5 dB compared to an un-swept step. This approximate calculation suggests that the sound from the circular embossment can be related to the sound from two-dimensional forward step via the sweep independence principle demonstrated in the wall-jet measurements. This point will be further explored in the discussion to follow.

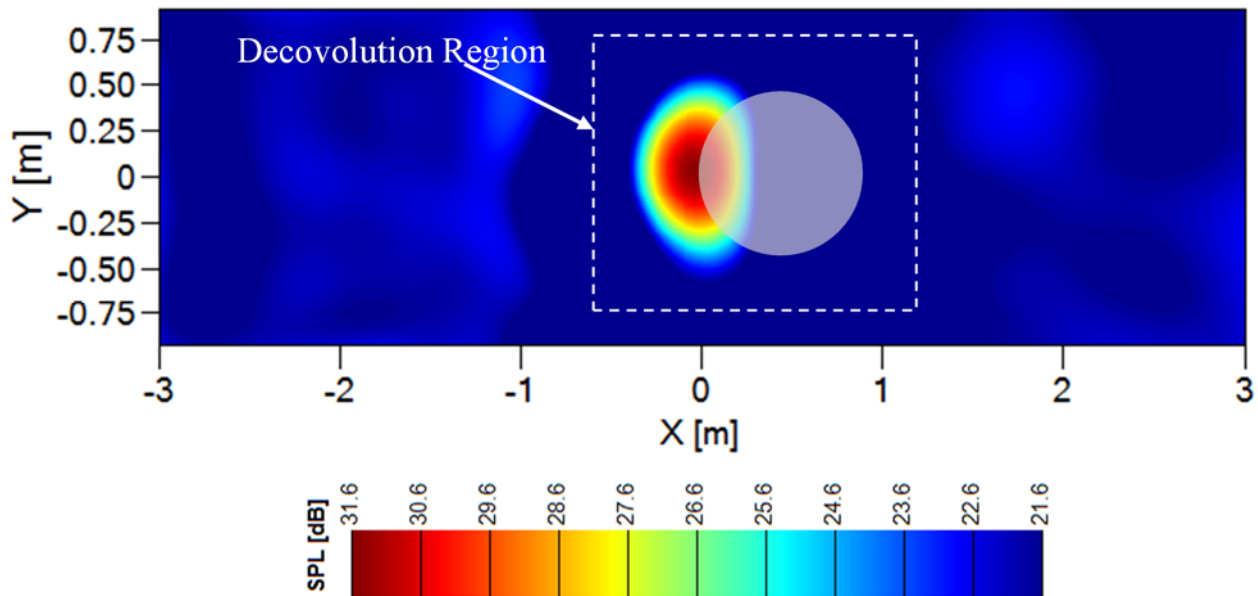


Figure 3-138 Background rejected sound source map at a frequency of 2000 Hz for wall mounted circular embossment in flow at $U_\infty = 30$ m/s. Also shown is the region of interest chosen for deconvolution and background rejection.

The sound source maps for circular embossment at a frequency of 2,000 Hz for four additional free stream velocities have been shown in Figure 3-139. The contour scale of the maps has been locked to the maximum and minimum sound levels seen for the highest velocity case shown in the figure ($U_\infty = 60$ m/s, Figure 3-139 (d)) so that the relative effect of increasing the velocity on the generated source maps can be seen. Looking at the figure it can be seen that the sound levels consistently increase with increasing free stream velocity with the source in each case focused around the leading edge of the embossment.

Figure 3-140 shows the source maps at $U_\infty = 60$ m/s for six additional frequencies. The contour level representation of sound follows the same format used in Figure 3-100 for forward steps with the maximum and minimum sound levels within each map shown on top of the corresponding map. Looking at the figure it can be seen that as the frequency increases the sound intensity levels drop and the spatial extent of the source shrinks due to reduced spot size of the array. However, the overall shape of the source stays the same. At very high frequencies (11,200 Hz and 13,200 Hz) a noisy spot, similar to that seen for forward steps, develops immediately upstream of the embossment due to low SNR at these frequencies. Additionally, a spurious noise spot occurs at high frequencies (highlighted in the map for 6,000 Hz) immediately below the main lobe associated with the embossment noise. This spot is believed to be due to a measurement error and is not associated with the embossment noise. Upon careful analysis of source maps it was found that this noisy spot appears in source maps for the three highest velocity ($U_\infty = 50$ m/s, 55 m/s and 60 m/s) at which measurements were made in present work with its strength increasing as velocity increases. It was also found that this spot is separate from the circular embossment noise and occurs at frequencies above about 6,000 Hz. The spot, however, does not influence the main sound lobe being emitted by the embossment. This noisy spot, along with the array reflection upstream of the embossment, can affect the integration of the source maps to yield the sound spectrum, the implications of which will be discussed in the following section.

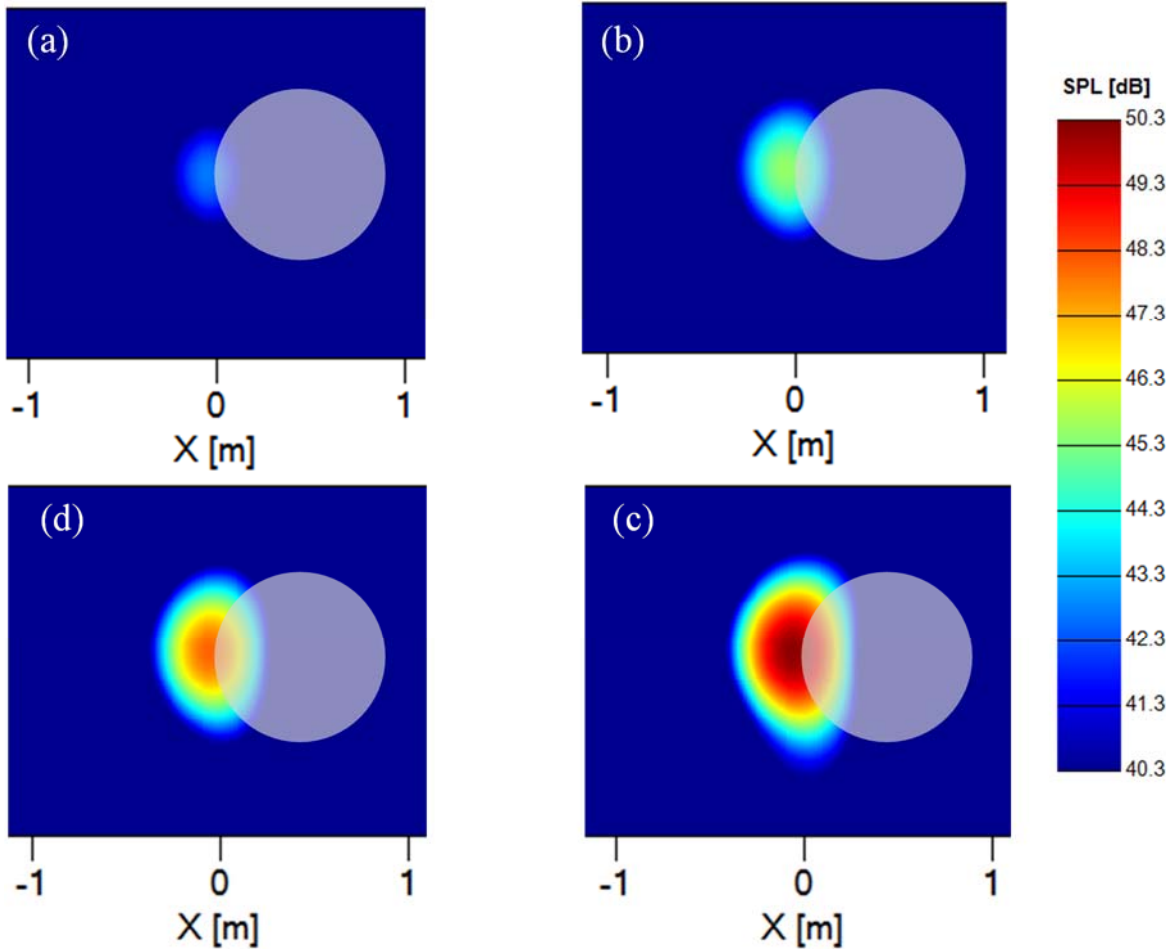


Figure 3-139 Sound source maps for circular embossment at a frequency of 2,000 Hz for four different free stream velocities. (a) $U_\infty = 45$ m/s, (b) $U_\infty = 50$ m/s, (c) $U_\infty = 55$ m/s and (d) $U_\infty = 60$ m/s.

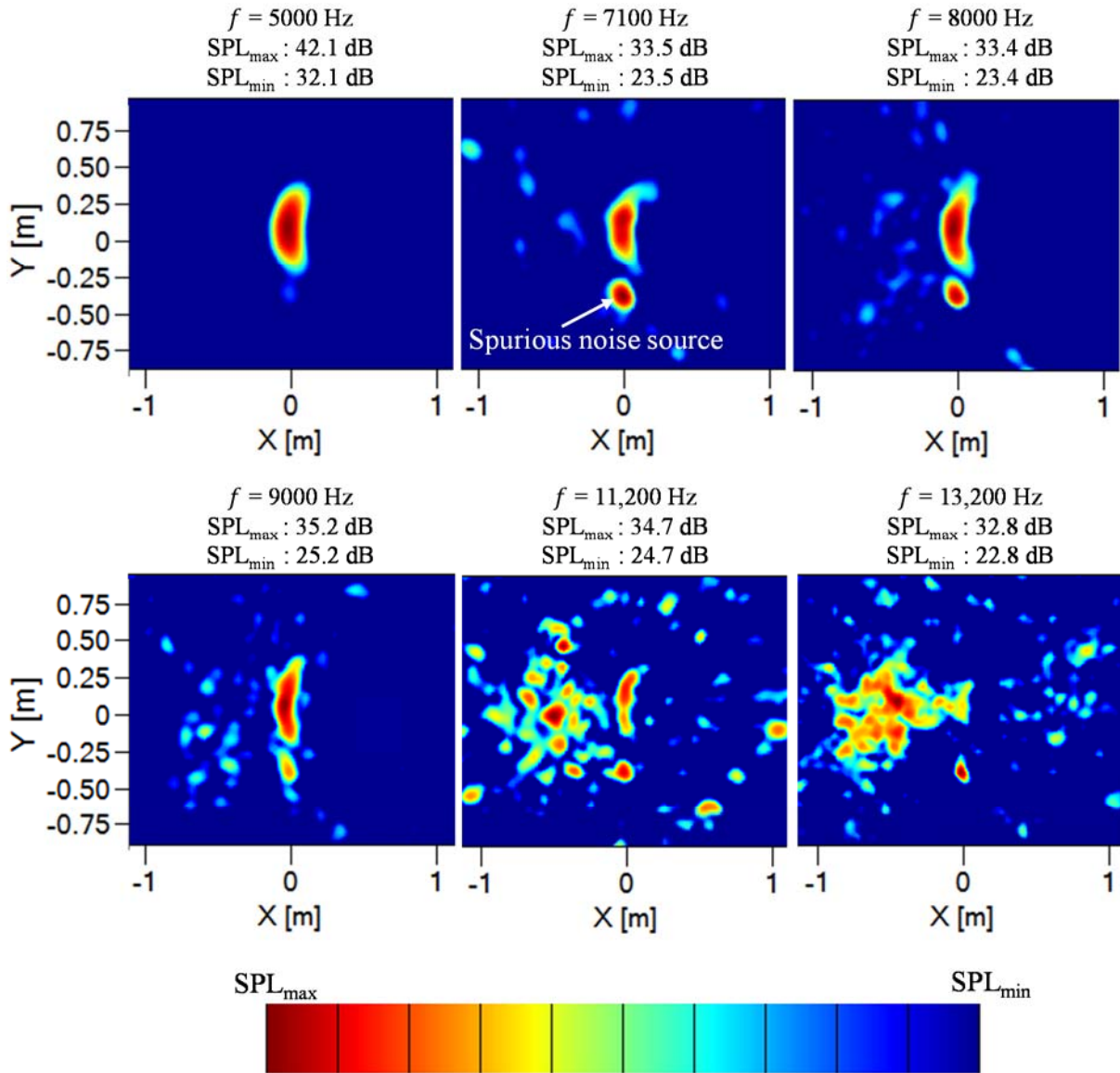


Figure 3-140 Sound source maps for flow over circular embossment at $U_\infty = 30$ m/s for six different frequencies. The contour level representation follows the format used in Figure 3-100 for forward facing step.

3.2.2.1 Integrated Sound Spectra for Wall Mounted Circular Embossment

The methodology to integrate the sound source maps for circular embossment to yield the sound spectrum follows the same as that used for forward steps. Each source map was analyzed individually and the integration region was chosen to include noise source from the embossment and exclude any spurious noise source. As a first iteration consider the effect of using six different integration regions on the sound spectra from circular embossment at five different velocities as shown in Figure 3-141. Each integration region used to generate these spectra has the same spanwise width of $-0.6 \text{ m} \leq Y \leq 0.6 \text{ m}$. This span width is sufficient to cover the entire embossment area whose span is about $Y = \pm 0.45 \text{ m}$. The downstream boundary of each integration region is $X = 1.1 \text{ m}$ which is more than 7 embossment heights downstream of the trailing edge of the embossment and is within the boundaries of the deconvolution region chosen to reject the facility noise. The upstream boundary of the integration region is the parameter which is varied in these spectra. The upstream boundaries are varied from 0.5 m upstream to 0.1 m upstream of

the leading edge of the embossment. Note that the spectra are only shown for frequencies at which the embossment noise is clearly visible in the source maps.

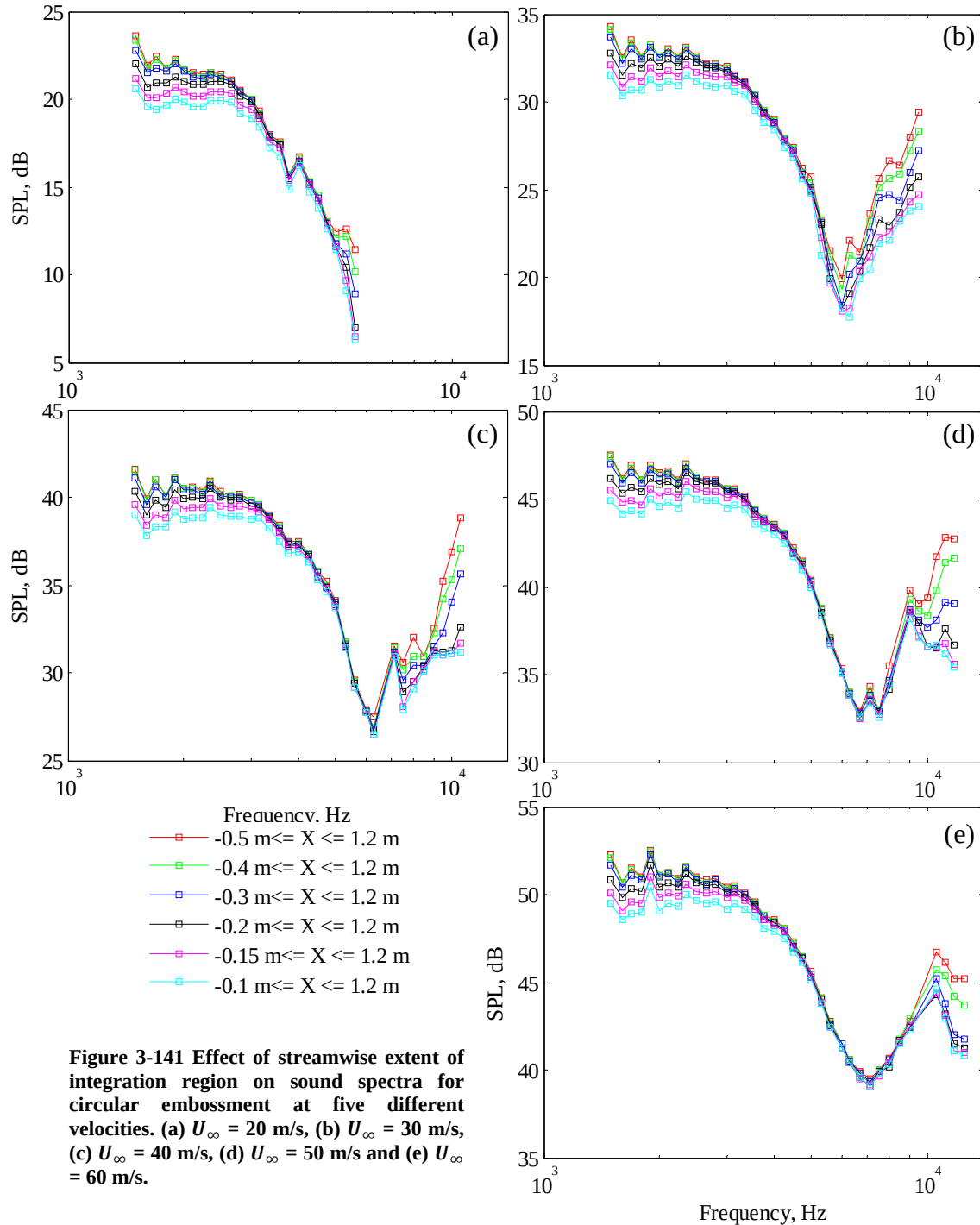


Figure 3-141 Effect of streamwise extent of integration region on sound spectra for circular embossment at five different velocities. (a) $U_\infty = 20$ m/s, (b) $U_\infty = 30$ m/s, (c) $U_\infty = 40$ m/s, (d) $U_\infty = 50$ m/s and (e) $U_\infty = 60$ m/s.

Looking at each of the spectra in Figure 3-141 we can see that as expected (and seen for forward steps results) the choice of integration region affects both the low and the high frequencies while the mid frequencies remain unaffected. On the low frequency side, the two widest integration regions ($X = -0.4$ m and 0.4 m) yield the same sound levels. Therefore, choosing the upstream boundary of the integration region

to be $X = -0.5$ m (which was also used for forward steps) is sufficient for low frequency convergence of the integrated spectra. On the high frequency side, it can be seen that the sound levels increase consistently as the integration region is extended further upstream. This effect, as seen in source maps in Figure 3-140 for higher frequencies, is due to the occurrence of array reflection immediately upstream of the embossment and so similar to the approach taken for forward steps must be avoided during integration. As seen in the spectra of Figure 3-141 narrowing the integration region to $X = -0.1$ m can lead to a reduction of sound levels between 8-10 dB at highest frequencies and seems like a reasonable choice to avoid the array reflection in integration process. However, upon visual observation of source maps it was determined that using this region also excluded some noise emitted from the embossment as well. Therefore, the upstream boundary of the integration region was chosen to be $X = -0.15$ m which as seen in the spectra only affects the noise levels slightly (~ 0.5 dB) at high frequencies compared to $X = -0.1$ m region.

Besides avoiding array reflection in the integration of source maps another additional complication which was not present in forward steps is the presence of a spurious noise source in certain maps at higher frequencies as discussed in the previous section and shown in Figure 3-140. Choosing the span of integration region to be $Y = \pm 0.6$ m as done to generate spectra in Figure 3-141 also includes this spurious noise source as well. As opposed to forward step which are two-dimensional, changing the span of the integration region to avoid this noise source for circular embossment can adversely affect the results since it is a three-dimensional geometry. Fortunately, the location of this spurious noise source is such that it is separable from the embossment noise. As an example, consider the source maps at 6,000 Hz at $U_\infty = 55$ m/s and 60 m/s as shown in Figure 3-142 (a) and (b) respectively. As shown in these maps the noise source from the spanwise extent of the noise from embossment is limited to approximately $-0.3 \text{ m} \leq Y \leq 0.3 \text{ m}$ while the noise spot is located around $Y = -0.4$ m. The figures also shows the integration region in each case chosen to yield the higher frequency end of the sound spectra. The boundaries of this integration regions are $-0.15 \text{ m} \leq X \leq 1.2 \text{ m}$ in the streamwise direction and $-0.3 \text{ m} \leq Y \leq 0.3 \text{ m}$ in the spanwise direction. As seen in the figure choosing this integration region avoids the spurious noise source but still includes the noise radiated by the circular embossment. The integration region chosen to yield the low frequency end of the spectra is shown in source maps at 2,000 Hz in Figure 3-142 (c) and (d) for $U_\infty = 55$ m/s and 60 m/s respectively. The boundaries of this integration region is $-0.5 \text{ m} \leq X \leq 1.2 \text{ m}$ in the streamwise direction and $-0.6 \text{ m} \leq Y \leq 0.6 \text{ m}$ in the spanwise direction. As seen in the figure choosing this integration region includes the entire noise being radiated by the embossment and therefore is considered an appropriate choice to yield the low frequency end of the sound spectrum. The finalized integration boundaries selected to yield the low and high frequency end of sound spectra for circular embossment have been summarized in Table 3-8.

Figure 3-143 (a) and (b) show the sound spectra calculated using low and high frequency integration regions at $U_\infty = 30$ m/s and 60 m/s respectively. Looking at the figure it can be seen that the output from the two integration regions differs at low and high frequencies but converges in the mid frequency region (similar to that seen for forward steps). The highest frequency at which these spectra begin to diverge from one another was chosen to be the transit point from the low to high frequency integration region. As shown in the figure these transit points vary with velocity with the transit frequency increasing as the velocity increases.

Figure 3-144 (a) and (b) shows the effect of changing the integration cutoff values on the sound spectra for circular embossment at $U_\infty = 30$ m/s and 60 m/s respectively. The spectra shown have been obtained by combining the low and high frequency region and sound levels at frequencies at which embossment noise is not visible in the source maps (except for the null associated with destructive interference occurring around 6,000 Hz) have not been shown in the spectra. Looking at Figure 3-144 (a) we can see that raising the integration cutoff value above 5 dB has no bearing on the spectra at any frequencies shown in the plot. This suggests that choosing 5 dB as integration cutoff value should be sufficient. However, if we now look at Figure 3-144 (b) which shows the spectra at $U_\infty = 60$ m/s we see that while at low and mid frequencies the integration cutoff value does not influence the spectra, at higher

frequencies (around 10,000 Hz) the sound increases with increasing cutoff value. The reason for this increase in spectral values is that at high frequencies where the array SNR is low, including noise levels above a certain threshold can include the side lobes which occur due to spatial aliasing in the array response. Upon observing the source maps it was revealed that increasing the integration cutoff above 5 dB includes some noise sources which cannot be associated with embossment noise. Therefore, integration cutoff value of 5 dB was found to be the most suitable to calculate sound spectra from circular embossment.

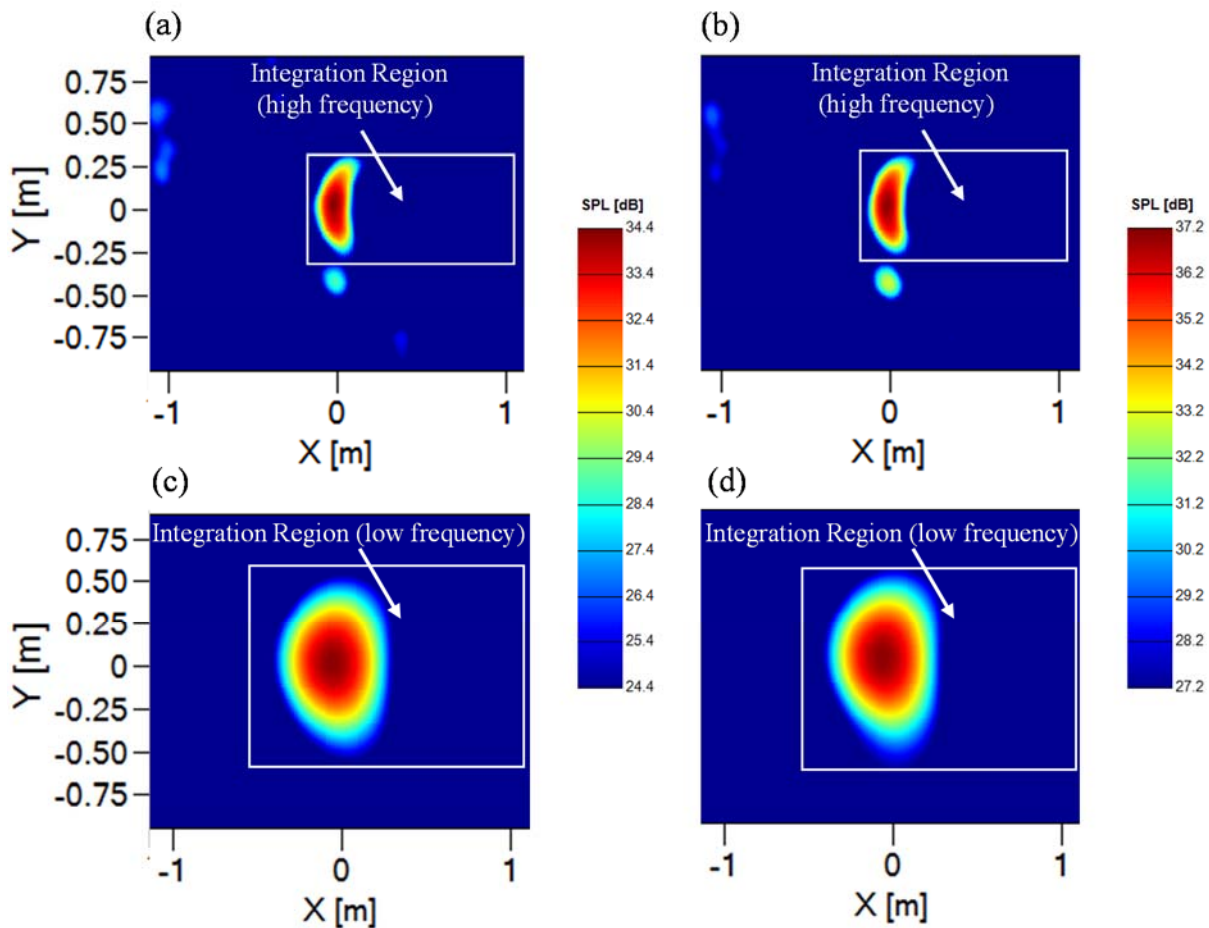


Figure 3-142 Integration regions selected to yield the sound spectra from circular embossment. High frequency integration regions at (a) $f = 6,000$ Hz, $U_{\infty} = 55$ m/s and (b) $f = 6,000$ Hz, $U_{\infty} = 60$ m/s. Low frequency integration region at (c) $f = 2,000$ Hz, $U_{\infty} = 55$ m/s and (d) $f = 2,000$ Hz, $U_{\infty} = 60$ m/s.

Table 3-8 Boundaries of integration region used to yield sound spectra for circular embossment

	X_{min} (m)	X_{max} (m)	Y_{min} (m)	Y_{max} (m)
Low frequency region	-0.5	1.1	-0.6	0.6
High frequency region	-0.15	1.1	-0.3	0.3

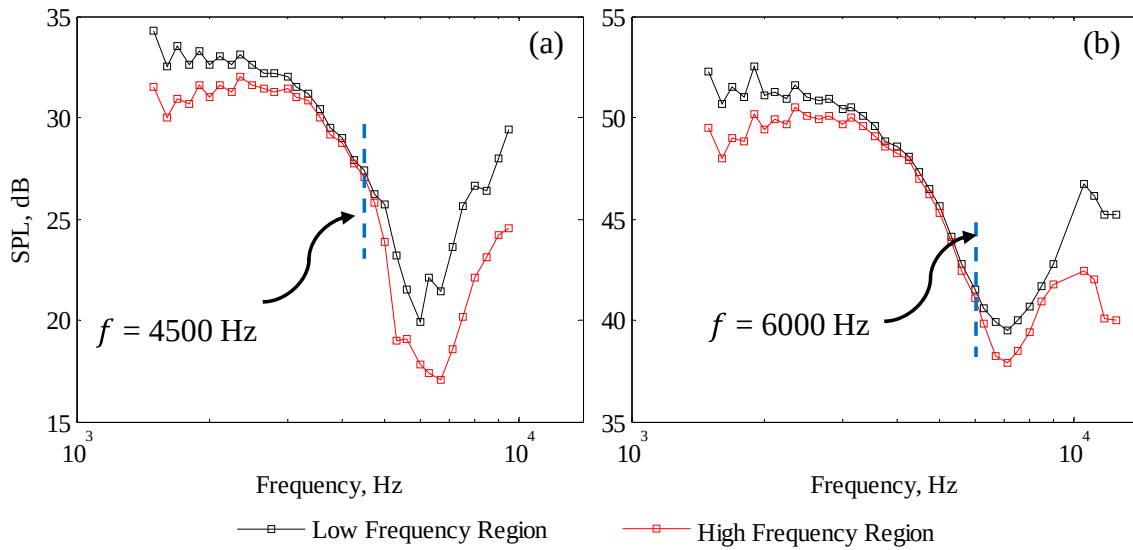


Figure 3-143 Sound spectra calculated from integration of source maps with separate low and high frequency regions for circular embossment at (a) $U_{\infty} = 30$ m/s and (b) $U_{\infty} = 60$ m/s. Vertical dashed lines in the figure indicate the frequencies which were chosen to transition from low frequency to high frequency integration region.

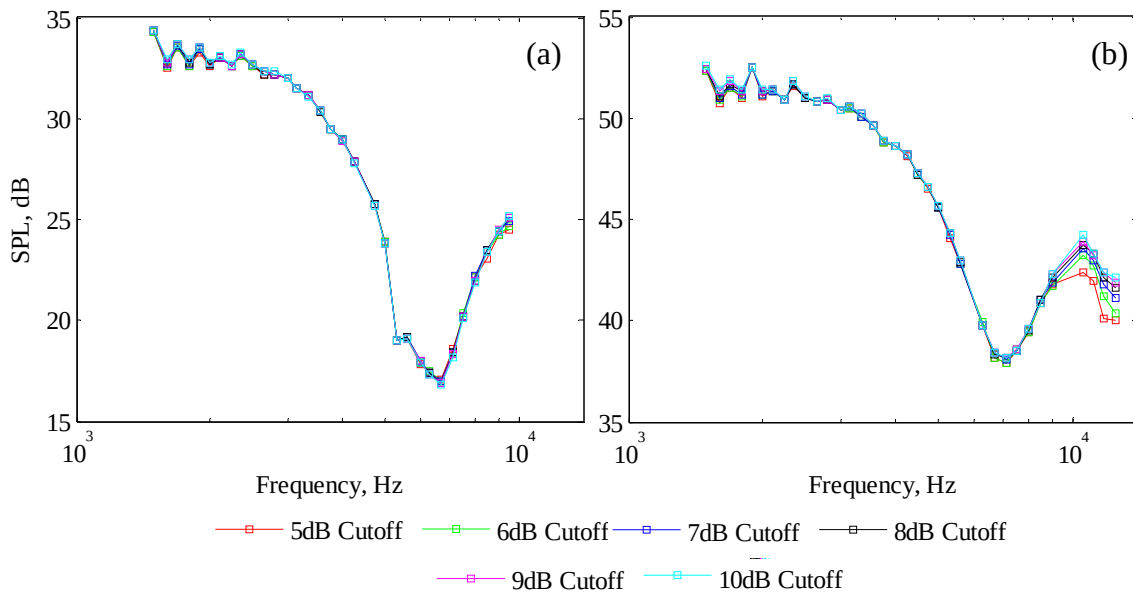


Figure 3-144 Effect of integration cutoff values on sound spectra from circular embossment at (a) $U_{\infty} = 30$ m/s and (b) $U_{\infty} = 60$ m/s.

Figure 3-145 shows the integrated sound spectra for circular embossment at nine different free stream velocities. The spectra show no qualitative change with increasing velocity and also show the presence of spectral dip around 7,000 Hz in each case which was also seen in forward step noise and is associated with destructive interference. Perhaps the most remarkable aspect of these spectra is their qualitative similarity with the spectra for an unrounded forward step (see Figure 3-117). This similarity is further illustrated in Figure 3-146 which plots the sound spectra from flow over unrounded forward step along with the spectra for circular embossment at five different velocities. Different velocities are represented by separate color schemes and square symbols and circular symbols are used to represent the

spectra from circular embossment and unrounded forward step respectively. Looking at these spectra it can be seen that throughout most of the frequency range shown here the forward step is slightly louder than the circular embossment. Furthermore, the difference in sound between the two geometries at a particular velocity is mostly constant (2.5-3.5 dB) throughout most of the frequency range shown on these plots. This consistent noise attenuation between the forward step and circular embossment reminds one of the measurements made in the wall-jet facility on swept forward facing steps (see §2.2.6) where it was shown that sweeping a forward step with respect to the oncoming velocity vector lead to a consistent attenuation in the spectra. Qualitatively, however, there was no change in the spectra with increasing sweep angle. This similarity between the noise radiated by swept steps and the circular embossment suggests that there could be a relationship between the forward step noise and the noise from a circular embossment. The exploration of this possible relationship will be the focus of the discussion in the next section.

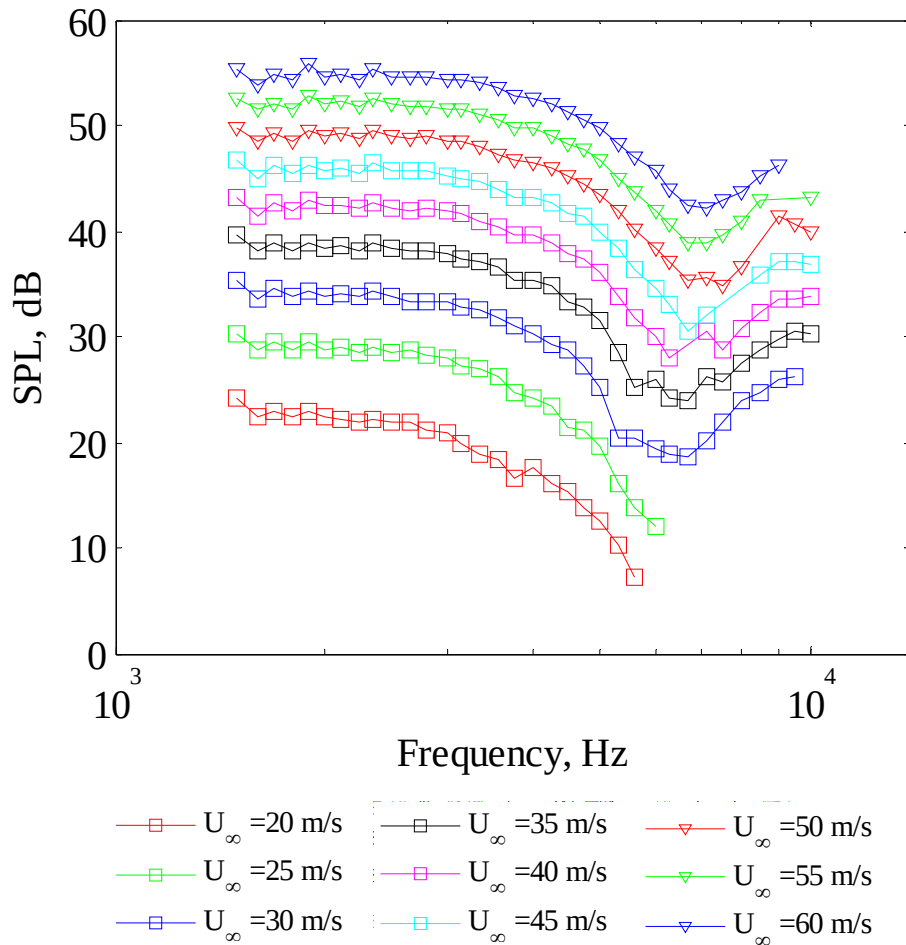


Figure 3-145 Sound spectra for circular embossment at nine different free stream velocities.

Figure 3-147 shows the scaling based on 5th power of the free stream velocity and the embossment height applied to the spectra of Figure 3-145. As expected, this scaling does not collapse the spectra for the embossment due to non-compactness effects that influence the far field sound at this observer location. The null associated with the destructive interference occurs at near constant frequency (related to sound speed and embossment height) and applying a velocity scaling to each spectra shifts each spectra along the abscissa of the plot leading the spectra to not collapse. This result is the same as that for forward steps in both the present measurement and in the wall-jet measurements and suggests that the acoustic phenomenon

might be the dominant mechanism—at least at higher frequencies—which governs the behavior of far field sound at this observer location.

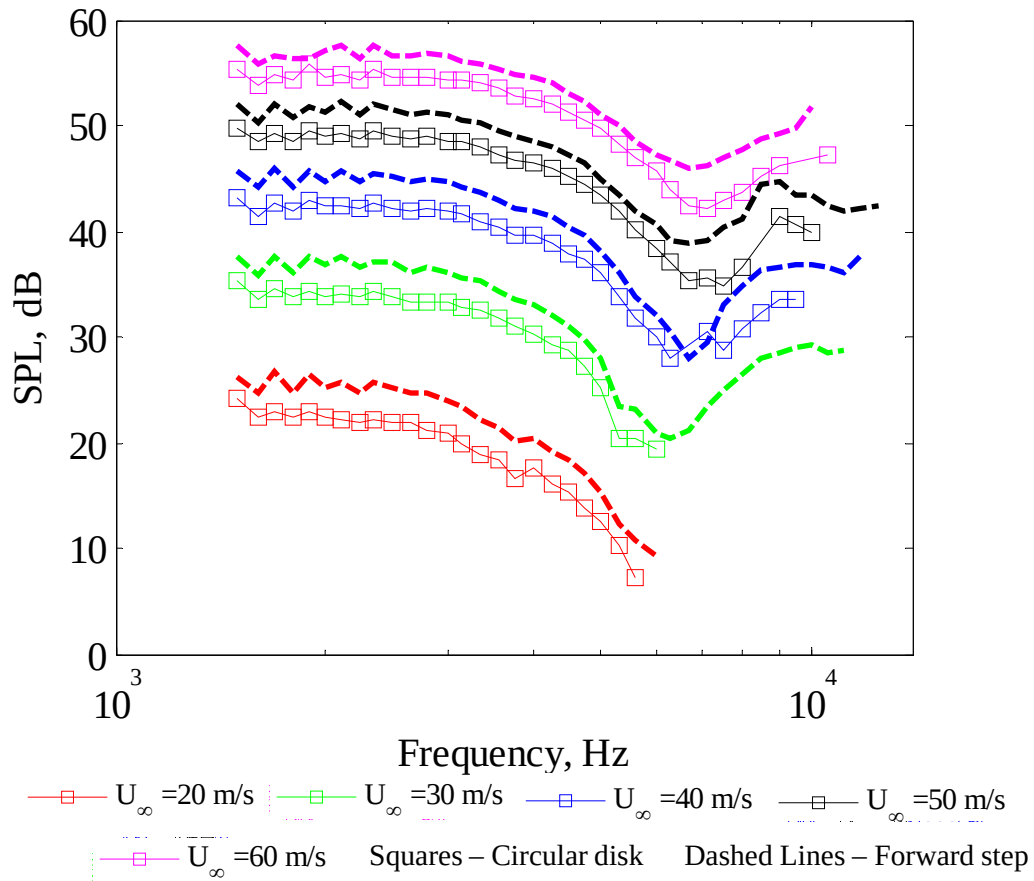


Figure 3-146 Comparison of forward step sound spectra with those for circular embossment at five different velocities.

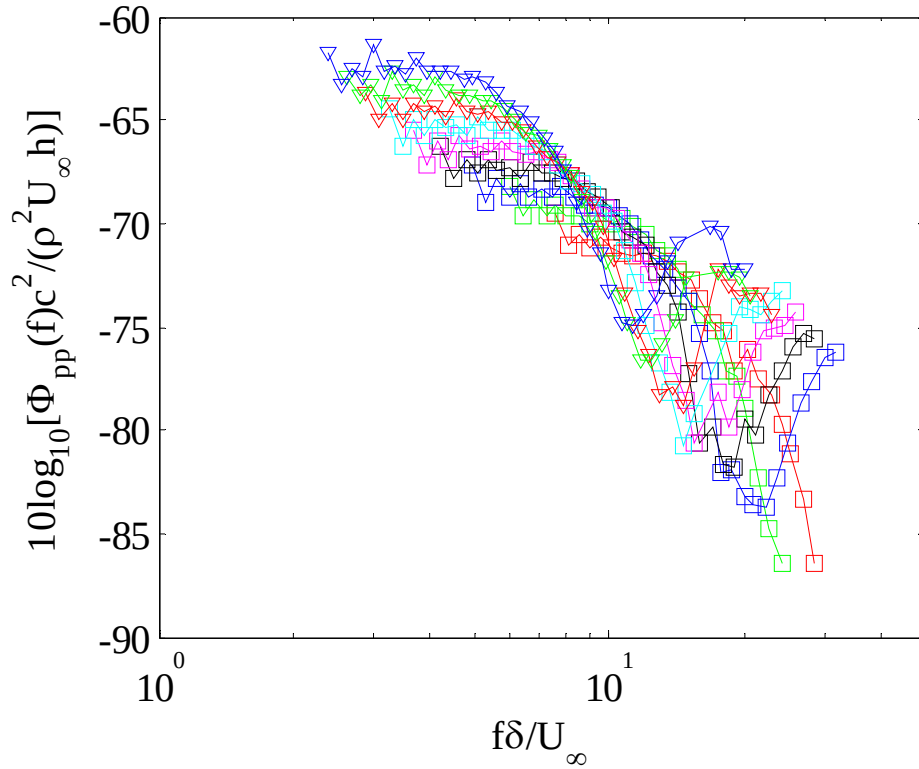


Figure 3-147 Dipole scaling of far field sound spectra based on 5th power of free stream velocity and step height for circular embossment. Legend for the figure is same as that for Figure 3-145.

3.2.3 Relationship between Far Field Sound from Forward Step and Circular Embossment

As highlighted in the previous section, the first impression one gets upon looking at the sound spectra for forward step and the circular embossment is that a possible relationship might exist between the two due to the close qualitative similarity. Furthermore, the sound source maps for the circular embossment show that the noise is entirely focused on the leading edge of the embossment and drops as one moves away from the spanwise center of the embossment in either direction. This motivates us to attempt applying the sweep independence principle discussed in §2.2.6 to establish a possible relationship between the forward step and circular embossment by assuming the leading edge of the latter to be composed of an assembly of swept steps.

It must be recalled that the sweep independence principle proposed by Hao and Wang (2013) states that: “Assuming a spanwise homogeneous flow, the far field sound from swept forward steps scales on the step-normal component of the undisturbed edge velocity.” We now proceed to apply this principle to relate the forward step noise to the noise from the three-dimensional circular embossment of the same height and a finite span.

To quantify the differences between the forward step and the embossment we assume a source distribution model as shown in Figure 3-148 below. Shown in this figure is an overlaid plan view of the embossment and of the forward step (represented by the vertical line that extends to infinity at the leading edge of the embossment). The coordinate system used for the forward step is the same as used to represent the beamformed sound source maps with the origin lying at the inside corner of the step and spanwise center of the test section. Note that the embossment was mounted such that its leading edge at the spanwise center lies at the streamwise location of the vertical face of the forward step. The embossment radius is $r_D = 0.457$ and its diameter (or span) is represented by W_D . The angle made by a point on the circumference of the

embossment relative to the upstream direction is designated as α and is defined positive clockwise. The distance between any point on the leading edge circumference of the embossment to the phased array center (i.e. observer location) is designated $r'(\alpha)$ as shown in the figure and is given by

$$r'^2(\alpha) = 2.525^2 + [r_d(1 - \cos \alpha) + 0.25]^2 + (r_d \sin \alpha + 0.086)^2 \quad (3-96)$$

where the constants in the equation (each a distance in meters) are present because the phased array center does not lie in the same plane as the spanwise center of the test section. Similarly, the distance between any point along the span of the two-dimensional step and the phased array center is designated by $r_s(Y)$ and is given by

$$r_s^2 = 2.525^2 + Y^2 + 0.25^2 \quad (3-97)$$

Now to establish a relationship between the sound from the two geometries we assume a source distribution along the leading edge circumference of the embossment and along the step span. Figure 3-148 shows a point source located at a position Y along the span of the two-dimensional step whose distance from the observer is $r_s(Y)$ as defined above. The figure also shows a source located on the leading edge of the embossment at a radial angle α and its distance to the observer is $r'(\alpha)$ also defined above. The velocity vector normal to the two-dimensional step is always U_∞ while the velocity vector normal to the step formed by the embossment is given by $U_n(\alpha) = U_\infty \cos \alpha$. Now the total mean square acoustic pressure received by the observer from the source distribution along the span of the step which is equal to the diameter of the embossment is given by the following integral

$$I_F(f) = \left| \frac{ix_1}{R} \cos\left(\frac{khx_2}{R}\right) + \frac{x_2}{R} \sin\left(\frac{khx_2}{R}\right) \right|^2 \int_{-\frac{W_D}{2}}^{\frac{W_D}{2}} \frac{dQ_F}{r_s^2(Y)} dY \quad (3-98)$$

where the term outside the integral which determines the directivity of the forward step noise is the same term used in predictions of forward step noise in §3.2.8 using the theory proposed by Glegg *et al.* (2014)/Bryan *et al.* (2015). Recall from §3.2.8 that in this term k is the acoustic wavenumber, h is the step height, R is the radial distance to phased array center ($R = 2.54$ m) from inside corner of the step and x_1 and x_2 are the location of array center from inside corner of the step in streamwise and wall-normal directions respectively (see Figure 3-124 for a description of these terms). Note that this term is independent of the spanwise observer location (x_3) and therefore appears outside the integral. Also note that this term is a function of frequency (due to the presence of acoustic wavenumber k in it) which leads to the total mean square acoustic pressure I_F also being a function of frequency. The term Q_F in equation (3-102) is the power per unit frequency of the source located along the span at some distance Y from the origin.

Similarly, the total mean square acoustic pressure received by the observer from the source distribution along the leading edge of the embossment is given by

$$I_D(f) = \int_{-\frac{\pi}{2}}^{\frac{\pi}{2}} \frac{dQ_D}{r'^2(\alpha)} |\beta_n|^2 r_D d\alpha \quad (3-99)$$

where β_n is a term which describes the directivity of each source distributed along the leading edge of the embossment. This term as given by Bryan (2015) is

$$\beta_n = \left(\frac{r_D - x_1 \cos(\alpha) - x_3 \sin(\alpha)}{r'(\alpha)} \right) \cos\left(\frac{khx_2}{r'(\alpha)}\right) - i \left(\frac{x_2}{r'(\alpha)} \right) \sin\left(\frac{khx_2}{r'(\alpha)}\right) \quad (3-100)$$

where r_D is the radius of the embossment, $r'(\alpha)$ as defined in equation (3-96) is the radial distance to observer from each source and the rest of the terms are the same defined for the forward step directivity function. Note that for the embossment the far field coordinates (x_1 , x_2 and x_3) are the distances to the phased array center from the point located at inside corner of the vertical face of the embossment at its spanwise center. Further note that unlike forward step, in the case of embossment the directivity is a function of the observer location along the spanwise direction (x_3) defined as the distance between the spanwise center of the embossment and phased array center. This distance in present measurements is $x_3 = 0.082$ m. To choose the source strength dQ_F we recall that the sound from the forward step was found to scale on the 5th power of edge velocity in the wall-jet measurements as long as the non-compactness effects were not dominant. Thus, based on this information we select the following form of the source for forward step

$$dQ_F = \frac{\rho^2 U_\infty^5 h}{c^2} \quad (3-101)$$

We also recall that in the case of sound from swept forward steps in wall-jet measurements the sound was found to scale with the velocity component normal to the face of the swept step. If we now imagine the leading edge of the circular embossment to be composed of infinite number of swept steps with sweep angles between $-\pi/2$ and $\pi/2$ then the form of the source power dQ_D for the embossment is given by

$$dQ_D = \frac{\rho^2 U_n^5(\alpha) h}{c^2} \quad (3-102)$$

where $U_n(\alpha)$ is now the step-normal component of the velocity at each point along the leading edge of the embossment. Now to compare the sound from step and the embossment we form a ratio between the total mean square acoustic pressure from the two source distributions as

$$\frac{I_F}{I_D}(f) = \frac{\left| \frac{ix_1}{R} \cos\left(\frac{kx_2}{R}\right) + \frac{x_2}{R} \sin\left(\frac{kx_2}{R}\right) \right|^2 \int_{-\frac{W_D}{2}}^{\frac{W_D}{2}} \frac{dQ_F}{r_S^2(Y)} dY}{\int_{-\frac{\pi}{2}}^{\frac{\pi}{2}} \frac{dQ_D}{r'^2(\alpha)} |\beta_n|^2 r_D d\alpha} \quad (3-103)$$

substituting the equations for dQ_F and dQ_D and eliminating the common terms between the two we get

$$\frac{I_F}{I_D}(f) = \frac{\left| \frac{ix_1}{R} \cos\left(\frac{kx_2}{R}\right) + \frac{x_2}{R} \sin\left(\frac{kx_2}{R}\right) \right|^2 \int_{-\frac{W_D}{2}}^{\frac{W_D}{2}} \frac{1}{r_S^2(Y)} dY}{\int_{-\frac{\pi}{2}}^{\frac{\pi}{2}} \frac{\cos^5 \alpha}{r'^2(\alpha)} |\beta_n|^2 r_D d\alpha} \quad (3-104)$$

The above integrals can now be evaluated to yield a numerical value as a function of frequency since the only variable is the variable of integration in each case. The ratio I_F/I_D would then provide a scaling factor which could be used to relate the forward step sound spectrum to the embossment spectrum. These integrals were evaluated numerically with $dY = 10^{-5}$ m and $d\alpha = 10^{-4}$ °. These resolutions were found to be more than sufficient to ensure convergence of the integrals. Figure 3-149 shows this scaling factor plotted in dB as a function of frequency. This scaling factor suggests that at lower frequencies (less than about 5,000 Hz) the sound from forward step is approximately 2.75 dB higher than the embossment noise and there is small variation in the slope of the spectrum as evident by the near flat shape of the scaling factor. At higher frequencies (greater than 6,000 Hz) where the non-compactness effects become dominant

the quantitative difference between the noise from the two geometries remains within 3.5 dB. However, the scaling factor is no longer flat due to the geometries of the two shapes playing an important role at these frequencies. We can now apply the scaling factor of Figure 3-149 to predict the embossment sound using the measured sound from forward step as

$$\Phi_{PP_D}(f) = \frac{\Phi_{PP_F}(f)}{I_F/I_D(f)} \quad (3-105)$$

where $\Phi_{PP_D}(f)$ is the predicted embossment sound spectrum and $\Phi_{PP_F}(f)$ is the actual measured forward step sound spectrum. Figure 3-150 shows the comparison of this predicted spectra with those measured for circular embossment at five different velocities. Looking at the figure it can be seen that equation (3-105) can be used to predict the sound from the circular embossment accurately (within 1 dB) for higher velocities ($U_\infty \geq 40$ m/s). For lower velocities there is a 1.5-2 dB discrepancy between the predictions and measurements likely due to higher uncertainty in measurements of both the forward step noise and the embossment noise.

These results suggest that the two-dimensional forward spectrum can be used to approximate the sound spectrum from a circular embossment reasonably well by simply using the source model described here. This is potentially a very useful result since this means that the sound from a three-dimensional geometry where the flow separation is the dominant source mechanism can be approximated using the sound from a similar two-dimensional geometry.

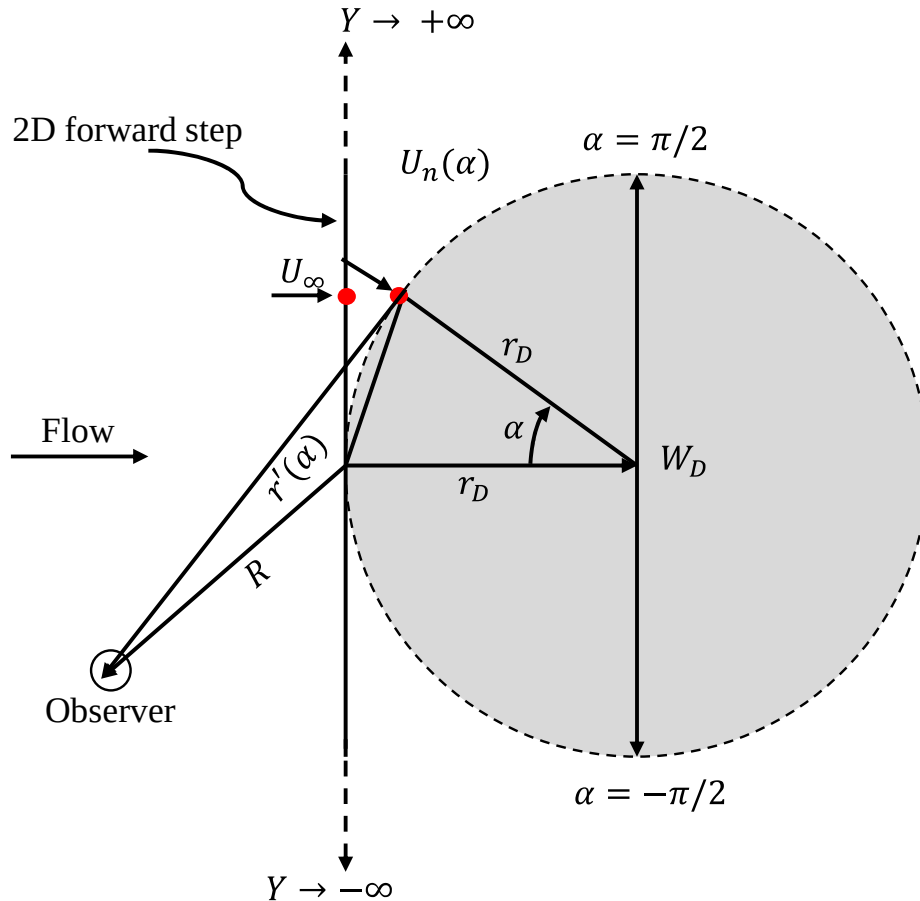


Figure 3-148 Illustration of the problem setup to establish relationship between noise from 2D forward step and circular embossment.

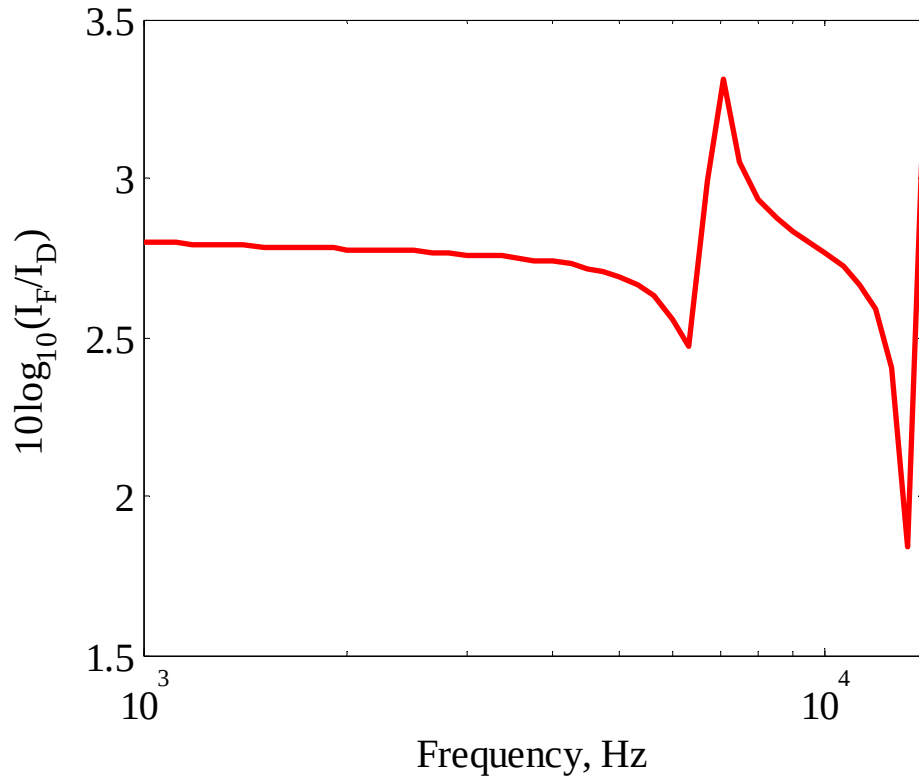


Figure 3-149 Calculated ratio of mean square pressure between forward step and circular embossment in dB.

In approximating the sound from circular embossment using forward step noise we assumed that the entire leading edge of the embossment was the sound source. However, as seen in the beamformed sound source maps previously this is not the case. Instead the source region appears to be focused along an arc with a radial range much less than $\alpha = \pm 90^\circ$. We will now explore the effect of changing the source distribution region along the leading edge of the circular embossment on the scaling factor needed to approximate the entire sound from the embossment. To perform this analysis we can integrate equation (3-104) with different upper and lower bounds of the angle α which determines the boundaries of region in which the sound source is assumed to be present. Figure 3-151 shows the scaling ratio I_F/I_D (in dB) as a function of angle α which was used to define the boundaries of the source region on the leading edge of the embossment. Looking at the figure it can be seen that at values of $\alpha \leq 50^\circ$ we get higher values for the scaling factor due to these values not accounting for the entire source region and therefore producing lower levels for the sound from the embossment. For $\alpha \geq 60^\circ$ the scaling factor is independent of the angular extent of region included in the calculation of the scaling factor. This suggests that including the region beyond $\alpha = 60^\circ$ has negligible impact on the overall sound produced by the embossment. This result is consistent with the source region observed in the beamformed source maps.

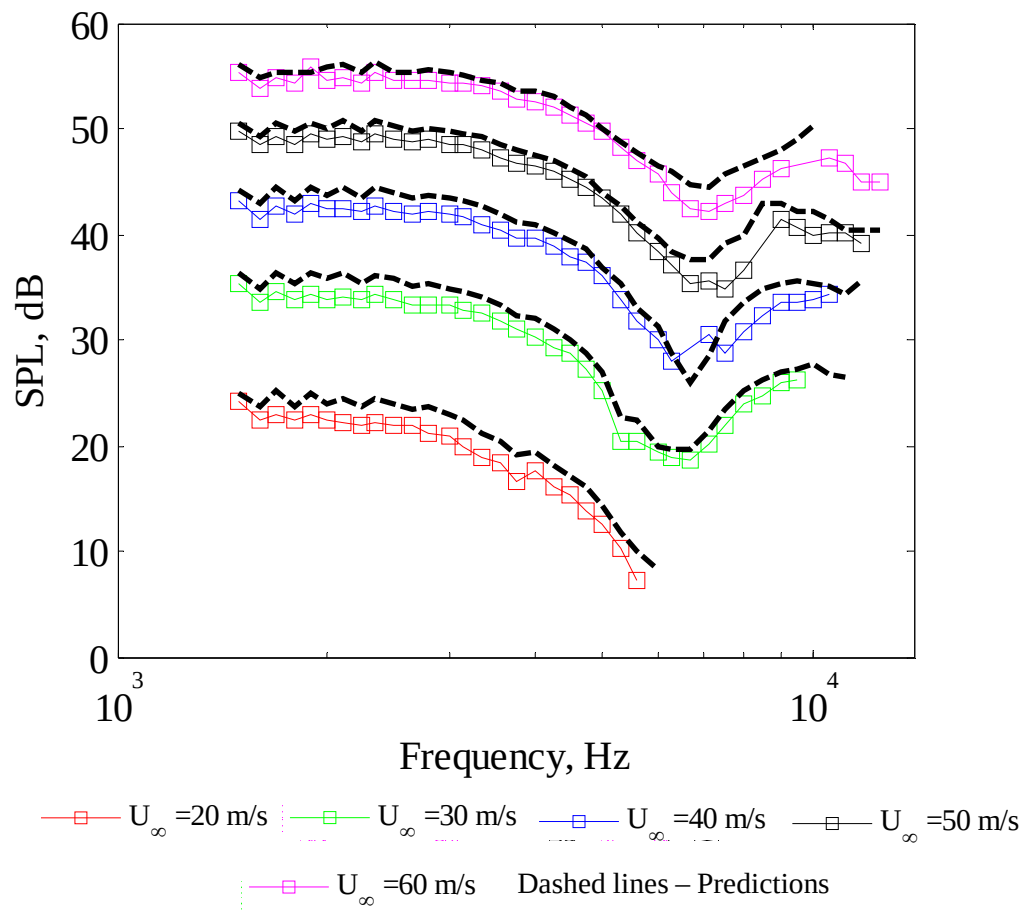


Figure 3-150 Comparison of measured circular embossment spectra with those predicted using the measured forward facing step spectra

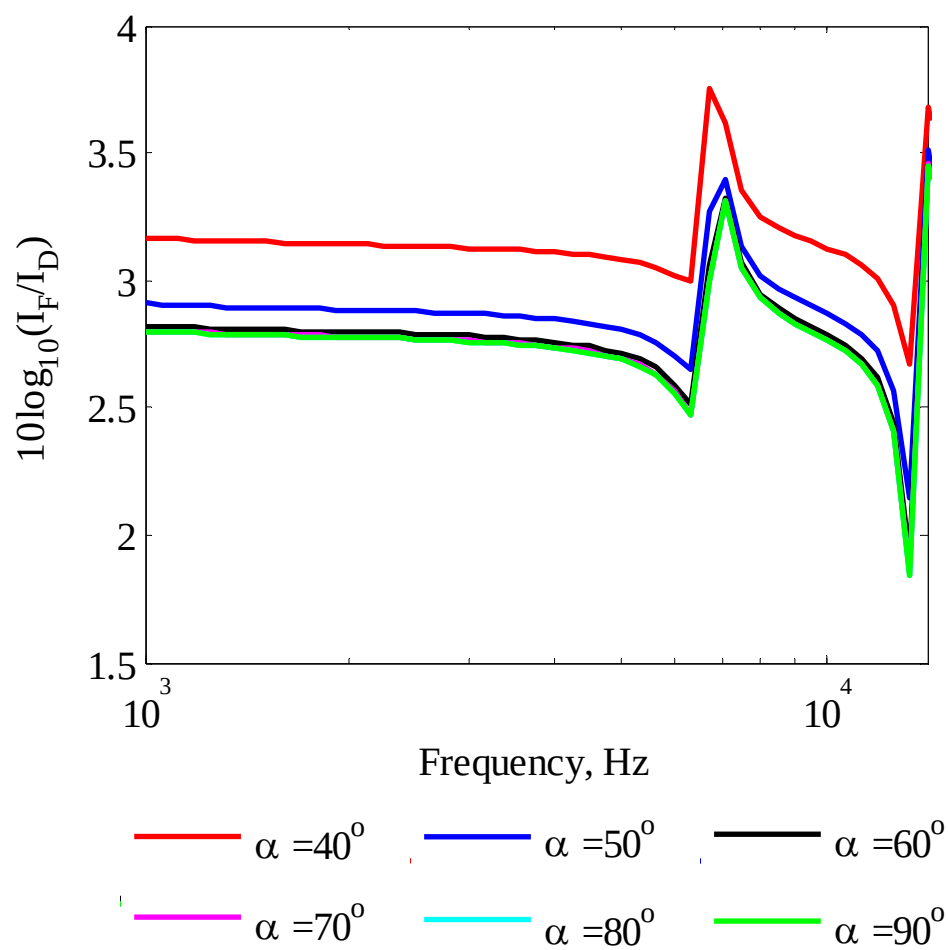


Figure 3-151 Ratio between mean square acoustic pressure induced by forward step and circular embossment as a function of radial region ($\pm\alpha$) within which source distribution is assumed.

4 Conclusions

Far field sound radiation and the responsible source mechanisms in turbulent boundary layer flows over two and three-dimensional surface discontinuities have been studied experimentally. Measurements were performed in two different types of boundary layers: a wall-jet flow and a conventional high Reynolds number boundary layer. The overall results show that the sound from an unrounded forward step exhibits non-compact behavior at frequencies where the acoustic wavelength becomes comparable to step height. Rounding of the step corner leads to a reduction of sound which starts at the highest frequencies with a slight rounding and then spreads throughout the frequency range as the rounding increases. Rounding the step corner has no influence on the directivity of the sound field. Rounding the upstream corner of a two-dimensional gap has no effect on the far field sound while rounding the downstream corner affects the sound in the same manner as a forward step alone configuration. The resultant of unsteady forces measured on the face of the forward step and within the downstream separation placed on the top corner of the step combined with a suitable Green's function is shown to qualitatively describe the far field sound seen in measurements. This supports the hypothesis of Glegg *et al.* (2014) that the unsteady loading on the face and downstream surface of a forward step are collectively responsible for the far field sound. The results also show that the far field sound from a two-dimensional forward step can be used to accurately predict sound from a fully three-dimensional geometry by employing an acoustic sweep independence principle.

Following are the detailed conclusions from present study.

4.1 Surface Discontinuities in Wall-Jet Boundary Layer

In the wall-jet flow, measurements of far field sound were made on flow over two-dimensional forward facing steps with and without rounding of the external corner, two-dimensional gaps with rounding of both the backward step corner and the forward step corner and three-dimensional swept forward facing steps with and without rounding of the corner. The discontinuity height (h) to undisturbed boundary layer thickness (δ) ratios for two and three-dimensional cases were approximately between 77.9% - 83.5%. The Reynolds numbers based on the discontinuity height (Re_h) were between 8,300 - 16,800 and the flow Mach number (M) was between 0.03 - 0.06. For the two-dimensional forward facing steps, six different rounding of the external corner with radius to step height ratios (r/h) of 0%, 6.25%, 12.5%, 25%, 50% and 100% were considered. Two different two-dimensional gap configurations were considered. The first was an unrounded forward step - rounded backward step configuration with rounding radii of $r_1/h = 0\%$, 12.5%, 25% and 50%. For these gaps, four different gap width of $w = 1.13h$, $2h$, $4h$ and $8h$ were also studied. The second gap configuration was an unrounded backward step – rounded forward step configuration with $r_2/h = 0\%$, 6.25%, 12.5%, 25%, 50% and 100%. For this gap configuration only a single gap width of $w = 4h$ was considered. On the three-dimensional side, far field sound from swept forward facing steps with sweep angles of 0° , 5° , 15° and 30° with respect to the incoming streamwise velocity vector was measured. For the swept steps, four corner rounding with $r/h = 0\%$, 12.5%, 25% and 50% were also considered.

4.1.1 Forward Facing Steps with Rounded Corners

- Far field sound from an unrounded forward step is broadband which shows effects of non-compactness in the region directly above the step due to destructive interference caused by acoustic reflections from surface upstream of the step.
- The spectra from unrounded steps scale on the mixed scaling proposed by Catlett *et al.* (2014) except for cases where the non-compactness effects are dominant.
- Present results for sound from unrounded forward steps are in agreement with Catlett *et al.* (2014) once the differences in flow conditions are accounted for via a simple spanwise source distribution model.

- As the step corner is rounded slightly to $r/h = 6.25\%$ sound reduction is seen only at the highest frequencies. Rounding the corner further leads to reduction throughout the frequency range with the spectra for $r/h = 25\%$ showing more than 10 dB sound reduction at its peak. The sound from the two highest rounding of $r/h = 50\%$ and 100% is mostly the background noise scattered by the geometry.
- The directivity of sound is nearly independent of corner rounding and steps with $r/h \leq 25\%$ also show the effect of non-compactness.
- The sound from rounded steps with $r/h \leq 25\%$ also scales on the mixed scaling of Catlett *et al.* (2014).

4.1.2 Two-Dimensional Gaps with Rounded Corners

- For unrounded gap with gap width of $w = 1.13h$ the spectra at lower frequencies show a different character than the spectra for a forward facing step alone configuration. The higher frequencies for this gap configuration are attenuated compared to a forward step and show scalloping behavior indicative of acoustic reflections from the gap. As the gap width is increased to $w = 2h$ and $4h$ the sound at lower frequencies becomes quantitatively and qualitatively similar to a forward step alone configuration but the higher frequencies still show attenuation and scalloping behavior. For $w = 8h$ the sound resembles that from a forward step alone configuration throughout the frequency range.
- The unrounded gap spectra also scale on mixed scaling of Catlett *et al.* (2014) except in cases where the non-compactness effects are dominant.
- Rounding of the backward step corner for any gap width has negligible influence on far field sound and its directivity.
- The sound is affected by the rounding of the forward steps corner of the gap in much the same way as it does for a forward step alone configuration with the same rounding radii with the exception that for lower rounding ($\leq 12.5\%$ of step height) more attenuation of sound is seen in case of gaps and for higher rounding ($\geq 25\%$ of step height) less attenuation of sound is seen in gap noise.
- The effect of non-compactness are also visible in sound from these gaps. The directivity of the sound from these gaps appears to be similar to those for a forward step alone configuration with the same rounding radii.

4.1.3 Swept Forward Facing Steps with Rounded Corners

- Increasing the sweep angle leads to a reduction in sound levels throughout the frequency range without affecting the character of the sound spectrum up to $r/h = 25\%$. No sound above the background noise is seen at either sweep angle for the highest rounding ($r/h = 50\%$) considered in the present work.
- The sound spectrum produced by an unrounded swept step shows approximate sweep independence up to a sweep angle of 30° once spanwise inhomogeneity is accounted for. This result is in agreement with past results from computations of Hao and Wang (2013).
- Sweep independence is also seen up to sweep angles up to 30° for rounded swept steps with $r/h \leq 25\%$.

4.2 Surface Discontinuities in High Reynolds Number Boundary Layers

The flow of a conventional high Reynolds number boundary layer over two and three-dimensional discontinuities was studied through measurements of both far field sound and near field quantities in order to investigate the fundamental sound source involved in these flows. The Reynolds numbers based on the discontinuity height were between $Re_h = 30,900 - 92,700$. The Mach numbers were between $M = 0.06 - 0.17$. The two-dimensional surface discontinuities were forward facing steps with a step height to undisturbed boundary layer thickness ratio of approximately 26%. Four rounding of the external step corner

with $r/h = 0\%$, 6.25% , 12.5% and 25% were considered. The three-dimensional surface discontinuity studied was a low aspect ratio wall mounted circular embossment with diameter to height ratio of 36. The height of the embossment was the same as the height of forward steps.

4.2.1 Forward Facing Steps with Rounded Corners

4.2.1.1 Near Field Flow Structure

- Mean pressure measurements and oil flow visualizations show that while the flow behavior upstream of the rounded forward steps is nearly independent on corner rounding the flow structure downstream depends strongly on it. Downstream of the step, the mean surface pressure recovery towards the undisturbed boundary layer values is quicker for higher rounding radii which is well correlated with the shrinking of separation bubble with increasing rounding seen in flow visualizations.
- Point surface pressure fluctuation measurements also show that the behavior of the upstream separation bubble is independent of rounding. Downstream of the step, the shrinking of the separation bubble leads to a hastened recovery of pressure fluctuations with increasing rounding. However, the peak values of fluctuating pressure increase with rounding closer to the top corner of the step.
- On the vertical face of the step, the pressure fluctuations also increase with rounding as one moves closer to the top corner of the step. The similar behavior of the pressure fluctuations on the face and top surface of the step, close to the sharp corner, indicates that they might be correlated which would be in agreement with the theory of Glegg *et al.* (2014).
- Spanwise and streamwise correlations of pressure fluctuations obtained from a linear array of large-diameter surface-pressure microphones show little variation in spanwise coherency within the extent of the downstream separation bubble regardless of rounding. On the step face, the correlations are independent of corner rounding.
- The unsteady forces measured within the downstream separation bubble for rounded steps show similar behavior and those measured in the lower half of the step face appear to be independent of corner rounding.

4.2.1.2 Far Field Sound

- Far field sound from an unrounded forward step shows similar qualitative behavior to that in a wall-jet boundary layer flow. Quantitatively, for the same undisturbed edge velocity and similar observer location the sound levels are consistently higher because of the presence of a large mixing layer which bounds the boundary layer in the wall-jet flow.
- The effects of non-compactness due to destructive interference caused by acoustic reflections from the upstream surface are also visible but occur at half the frequency of the wall-jet measurements due to the step being twice as tall.
- Qualitatively and quantitatively rounding of the step corner has the same effect on the far field sound as seen in the wall-jet flow.
- The scaling of the sound spectra for each rounded forward step on the 5th power of the undisturbed flow velocity only collapses the data at lower frequencies due to the dominant effects of non-compactness at higher frequencies. This result is also consistent with those obtained in the wall-jet flow.
- The far field sound from rounded forward steps was successfully shown to be a combination of a single source spectrum that scales on the 5th power of undisturbed edge velocity and step height, and the Green's function derived by Glegg *et al.* (2014).
- This source spectrum has been shown to have the same form as the resultant of the unsteady forces measured on the face and downstream surface of the step supporting Glegg *et al.*'s (2014) hypothesis that the overall sound being produced by forward steps is being governed by unsteady forces on both surfaces.

- A direct application of unsteady force measurements to Glegg *et al.*'s (2014) theory shows good qualitative agreement between predictions and measurements showing that the knowledge of exact unsteady loading on the two surfaces of the step along with the theory can be used to describe the sound from forward facing step.

4.2.2 Three-Dimensional Circular Embossment

- The far field sound from the three-dimensional geometry was found to have the same qualitative character as the sound from a two-dimensional forward facing step of the same height.
- The beamformed sound source maps show that the sound source is concentrated at the leading edge of the embossment between a region whose spanwise boundaries subtend an angle of approximately 60° to the center of the embossment.
- The sound from the embossment can be predicted (to within ± 1 dB) using the measurements of sound from a two-dimensional forward facing step of the same span as the embossment by employing a spanwise source distribution model based on the acoustic sweep independence principle.

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