SIMULTANEOUS DIRECT MEASUREMENTS
OF SKIN FRICTION AND HEAT FLUX
IN A SUPersonic FLOW

by

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(ABSTRACT)

A new gage which can measure skin friction and heat flux simultaneously was designed, constructed, and tested. This gage is the combination of a non-nulling type skin friction balance and a heat flux microsensor. By mounting the heat flux microsensor directly on the surface of the floating element of the skin friction balance, it was possible to perform simultaneous measurements of the skin friction and the heat flux. The total thickness of the heat flux microsensor is less than 2 μm, so the presence of this microsensor creates negligible disruption on the thermal and the mechanical characteristics of the airflow. Tests were conducted in the Virginia Tech supersonic wind tunnel. The nominal Mach number was 2.4, and Reynolds number per meter was $4.87 \times 10^7$ with total pressure of 5.2 atm and total temperature of 300 °K. Results of the tests showed that this new gage was quite reliable and could be used repeatably in the supersonic flow. This gage also has an active heating system inside of the cantilever beam of the skin friction balance so that the
surface temperature of the floating element can be controlled as desired. With these features, the effects of a temperature mismatch between the gage surface and the surrounding wall on the measurements of the skin friction and the heat flux were investigated. An infrared radiometer was used to measure the surface temperature distributions. Without the active heating, the amount of temperature mismatch generated by the gage itself was from 2.5 °K to 4.5 °K. The active heating produced the temperature mismatch of 18.7 °K. The largest temperature mismatch corresponds to the levels typically found in high heat flux cases when it is expressed in dimensionless terms. This temperature mismatch made sizable effects – a 24 % increase in the skin friction measurement and a 580 % increase in the heat flux measurements. These experimental results were compared with the computational results using the Computational Fluid Dynamics code GASP. The input flow conditions were obtained from the boundary layer measurements. The temperature mismatch was input by specifying the density and the pressure at each grid point on the wall. The Baldwin-Lomax algebraic turbulence model was used with the thin layer approximations. The comparison showed that the difference in the skin friction and heat flux was less than 10 % of the measured data when the temperature mismatch was less than 8.5 °K, but the difference was increased as the amount of the temperature mismatch increased. It is presumed that the disagreement between the measurements and the calculations was caused mainly by deficiencies in the turbulence model for this complex, developing viscous flow, because the Baldwin-Lomax model cannot account for the multiple length scale in this flow.
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LIST OF SYMBOLS

ENGLISH

$C_F$  Skin friction coefficient

$C_p$  Specific heat at constant pressure

$D$    Diameter of floating element

$d_{in}$  Inner diameter of cantilever beam

$d_{out}$  Outer diameter of cantilever beam

$E$  Output signal of heat flux microsensor

$E$  Young’s modulus

$e$  Error

$H_W$  Total enthalpy at the wall

$H_\infty$  Freestream total enthalpy

$I$  Moment of inertia

$L$  Length of cantilever beam

$M$  Mach number

$M_\infty$  Freestream Mach number

$P$  Local static pressure

$P$  Load on cantilever beam
\( P_\infty \) Freestream static pressure

\( P_c \) Cone-static pressure

\( P_{t_0} \) Total pressure at settling chamber

\( q \) Dynamic pressure

\( \dot{q} \) Heat flux

\( S \) Sensitivity of heat flux microsensor

\( St \) Stanton number

\( T \) Local static temperature

\( T_w \) Wall temperature

\( T_{aw} \) Adiabatic wall temperature

\( T_e \) Temperature at boundary layer

\( T_{t_0} \) Total temperature at settling chamber

\( T_{t\infty} \) Freestream total temperature

\( T_{tw} \) Total temperature at the wall

\( u \) Velocity component in flow direction

\( U_e \) Velocity at boundary layer

\( U_\infty \) Freestream velocity

\( v \) Velocity component in vertical direction

\( w \) Velocity component in spanwise direction

\( x \) Cartesian coordinate in flow direction
$y$ Cartesian coordinate in vertical direction

$z$ Cartesian coordinate in span direction

**GREEK**

$\delta$ Boundary layer thickness

$\delta$ Deflection at the end of cantilever beam

$\varepsilon$ Strain on cantilever beam

$\rho$ Density

$\sigma$ Stress on cantilever beam

$\gamma$ Ratio of specific heat

$\tau_w$ Wall shear stress
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1. INTRODUCTION

Measurements of the skin friction and heat flux created by air flowing over a solid surface are essential to estimate the performance of an aerodynamic vehicle. In fact, the magnitude of the skin friction and the amount of the heat flux directly affect the design and performance parameters of an aircraft wing, supersonic combustor, etc. The more advanced technology can be developed in these fields, the more advanced design for an aerodynamic vehicle also can be expected. Considerable efforts, therefore, have been made to develop new measurement techniques and instruments.

For the skin friction measurements, Brown and Joubert (1969) classified and reviewed the various methods, and Winter (1977) presented a good discussion for those as outlined by them. A direct measurement of the skin friction is usually preferable to an indirect measurement, because the indirect measurement is based on theoretical analogies requiring some assumptions, even though the direct measurement also must consider many kinds of error sources for its accuracy. Nitsche, Haberland and Thunker (1984) made experimental investigations for various techniques of the indirect measurements. The basic principle of directly measuring the skin friction force is quite straightforward. First, cut a small elemental piece of the wall, and then mount flush the skin friction gage in such a way that its floating element can move freely in the direction of any force acting on it, and measure the skin friction force on the floating element. There are numerous skin friction
balances, and the measuring system is also divided into nulling type and non-nulling type. For both cases, the size of the floating element is a major parameter concerning the sensitivity of the measuring system. A displacement-type balance had a weakness in measuring small forces, but, as mentioned by Winter (1977), it could be overcome by using strain gages, variable geometry electronic valves, etc.

Heat flux measurements had only a very little interest in aerodynamics before about 1950. As the speed of an aerodynamic vehicle, however, became faster and faster, the heat protection of the vehicle became an important subject. Nowadays, the measurement of the heat flux is a major field of aerodynamics because it provides information on the thermodynamic and chemical states of the air flow itself. Thompson (1981) described one-dimensional heat conduction relations and discussed a variety of heat flux gages. The instrumentation principles of different gages are the same. The basic idea is to install a heat flux sensing element in the heated or cooled surface in such a way that its output is proportional to the heat transfer rate to and from the wall. It is known as quite satisfactory to measure the average temperature change in a thin plate for a situation of low heat transfer rate. Thus, many kinds of heat flux gages have been designed under this principle, because it is simple and easy to adapt.

To pursue more accurate measurements and more advanced techniques, it is desirable to measure the skin friction and the heat flux simultaneously in the design process of a new aerodynamic system. Even though one of them can be estimated from the result of the measurement of the other one by some theoretical or analytical methods,
direct measurements of both are more reliable. Of course, they can be measured together by using two different gages, i.e. one skin friction gage and one heat flux gage, but the flow conditions must be kept the same for each gage in this case and it is not so easy in many cases, especially for three-dimensional flows. Here, a new gage which can measure the skin friction and the heat flux simultaneously was designed and tested.

The new gage, moreover, has the capability of actively controlling the surface temperature of its floating element. With these features, it was investigated how much a temperature mismatch between the floating element and the surrounding wall affects the measurements of the skin friction and the heat flux. For a measurement using a plug type gage, the problem is compounded by the need for active heating or cooling of the gage. The plug type gage usually creates a hot spot in high temperature supersonic flows, because its sensing element is relatively thinner than the surrounding wall. This hot of cold spot produces a new thermal boundary layer on the wall, which is different from the existing boundary layer in the flow. When the boundary layer flow interacts with this new boundary layer, it is to face to a condition of discontinuous temperature distribution on the wall. It was already known that this temperature mismatch causes a large amount of error in heat flux measurements as shown by Westkaemper (1961), Hornbaker and Rall (1964), Bachman, Chambers, and Giedt (1965), etc. Surprisingly, however, only a little information exists in the literature concerning the effect on skin friction measurements such as Westkaemper (1963) and Voisinet (1978).

In a supersonic combustor test, accurate measurement of skin friction is essential to
determine the combustion efficiency from pressure measurement at the combustor wall. The available thrust from an engine, moreover, can be significantly limited by skin friction even though the magnitude of the skin friction is relatively small. Direct measurement of skin friction is eagerly desired as complex as the flow is. A SCRAMjet (Supersonic Combustion Ramjet) combustor is a typical example. This combustor has a supersonic, chemically-reacting, high-enthalpy, high-heat flux flow with a three-dimensional, compressible, turbulent boundary layer. For such a case, calculation is very difficult at this time. When a direct measurement of skin friction is conducted, the temperature of the floating element of the skin friction balance must be matched to the temperature of the surrounding wall to prevent a serious error. But, exact temperature matching is difficult, because the surface temperature varies transiently during the measurements. Therefore, the effects of temperature mismatch must be investigated experimentally and be considered in the measurements of skin friction and heat flux. On the other hand, temperature measurement on the surface of the gage and the surrounding wall also must be conducted very carefully and simultaneously. A thermocouple can be easily embedded in the surrounding wall, but it is not so easy to do in the floating element of the skin friction balance without any disruption into the flow. Even though embedding a thermocouple may be possible, its time response is another problem during the measurements in a supersonic wind tunnel, and it can measure only the temperature of the point where it is located on the floating element. In the present study, therefore, an infrared radiometer was used to simultaneously measure the transient surface temperature
distribution on the whole surface area of the floating element and the surrounding wall. The window problem due to pressure difference between inside and outside of the supersonic wind tunnel was solved using a polyethylene film. Also, the experimental results were compared with the computational results using the CFD code GASP.

In summary, the main goal of this research were:

1. Simultaneous direct measurements of skin friction and heat flux
2. Study effects of temperature mismatch on the skin friction and heat flux measurements
3. CFD simulations

The experimental measurements were conducted at the Virginia Tech supersonic wind tunnel. The freestream Mach number was 2.4 and Reynolds number per meter was $4.87 \times 10^7$ with total pressure of 5.2 atm and total temperature of 300 °K.
2. BACKGROUND

2.1 Skin Friction Gage

The skin friction gage with a floating element balance is a well known research tool for direct measurement. It may give the most accurate measurement. There are two types of the floating element balance – nulling type and non-nulling type. Nulling type means that the floating element is held at the initially fixed position by a restoring force which is equal to the shearing force. Thus, the floating element is not deflected by the shearing force. Non-nulling type means that the floating element is set to be deflected by the shearing force. Because of its simple structure and fast time response, the non-nulling type balance has been used more extensively in the boundary layer study.

Since Dhawan (1953) introduced the floating element skin friction balance, many researchers like O'Donnell (1964), O'Donnell and Westkaemper (1965), and Allen (1976) have made systematic investigations of its performance and error sources, particularly the effects of pressure gradient. Schetz and Nerney (1977) made a cantilever balance which was a non-nulling type and used very sensitive crystal strain gages. This balance was designed for the test of porous-wall on an axisymmetric body. The NSWC (Naval Surface Weapons Center) skin friction balance was designed by Voisinet (1978). It was of the self nulling type with a servo-feedback system to re-center the floating element. Allen (1980) introduced a parallel-linkage balance. This improved balance, as he said, successfully
eliminated the error caused by the off-center normal force. It was also virtually insensitive
to gap size and floating element misalignment, but its structure was very complex.

Recently, DeTurris, Schetz, and Helibaum (1990) designed a non-nulling type
cantilever balance for the skin friction measurements in a scramjet combustor. This
balance employed an active cooling system surrounding its cantilever beam housing.
Chadwick (1992) made also a non-nulling type cantilever balance. He used a fused quartz
tube as a cantilever beam with strain gages bonded on it. This balance has an active
cooling system inside of the quartz tube.

In the use of the floating element balance, it is essential that the floating element
balance be made as free of error as possible. So, potential sources of error must be
understood to be eliminated in the design process and in the measurements. Ideally, the
floating element should be mounted flush with the wall on which the measurement is being
taken as shown in Figure 2.1. This would effectively get rid of the error due to protrusion
or recession of the floating element. Allen (1976) showed that small ratio of protrusion to
the diameter of the floating element may result in error forces larger than the pure
shearing force. Generally, it is believed that a small amount of recession of the floating
element relative to the wall is preferable to protrusion. Allen said, however, protrusion and
recession of the same magnitude are equally damaging to the measurement. When a non-
nulling type floating element balance is used, it is also considered that the protrusion from
a slightly tilted floating element by a shearing force will produce the same effect as
protrusion or recession from a nulling type floating element balance. Allen also showed
that the protrusion error is less significant as the gap size is increased. A pressure gradient on the floating element is another potential source of error. The effect of pressure gradient appears mainly as an off-center normal force. Figure 2.2 illustrates the aerodynamic forces acting on the floating element - friction force, lip force, and normal force. These aerodynamic forces can contribute to the output of the floating element balance. According to Allen's study, the normal force contribution is much larger for recession of the floating element than for protrusion of that.

2.2 Heat Flux Gage

When Gardon (1953) introduced his own radiometer, the so-called Gardon or asymptotic calorimeter, he really opened a new generation in the heat flux gage, even though Westkaemper (1961), Hornbaker and Rall (1964), Bachmann, Chambers, and Giedt (1965), Woodruff, Hearne, and Keliher (1967) presented analytical and experimental investigations for the error caused by thermal perturbations in using such a plug-type calorimeter. The Gardon gage operates by establishing a steady state radial temperature difference, which is proportional to heat flux, between the center and edge of the sensing disk (constantan) soldered to a peripheral heat sink (copper). There is a thermocouple junction pair at the center and edge of the sensing disk formed by welding a copper wire to the center of the disk and the other copper wire to the heat sink. Thus, the radial temperature difference can be read directly. Ash and Wright (1971) showed that centerline losses caused significant error in heat flux measurements with the Gardon-type
copper-constantan sensors. Trimmer et al. (1973) discussed some available techniques of the heat flux measurements using several types of heat flux gages in the supersonic wind tunnels of the von Karman Gas Dynamics Facility, and ASTM (1980) established a standard method for the heat flux measurements using the Gardon-type heat flux gage.

In recent years, an advanced heat flux gage was developed by Hager et al. (1989). This is a layered heat flux gage as shown in Figure 2.3. Several thin film layers form a differential thermopile across a thermal resistance layer. The heat flux microsensor of this gage was fabricated directly on the sensing surface so that any additional adhesive, which may be one of the error sources, was not needed. Moreover, its very small thickness of less than 2 μm made no physical surface disruption and gave time response of less than 1 msec. They also measured and analyzed the performance characteristics of this heat flux sensor, and found its weakness for high temperature applications. After many studies, they designed a new high temperature heat flux gage and performed successful tests for both low and high temperature environments in 1991. Figure 2.4 shows the heat flux microsensor and the resistance temperature sensor fabricated on the substrate.

2.3 Effects of Temperature Mismatch

When the air flows over a solid wall where the wall temperature varies, the air confronts a locally different thermal boundary layer. This local thermal boundary layer affects the thermal and mechanical properties of the flow. For an incompressible flow,
Rubesin (1951) investigated theoretically the effect of surface temperature mismatch between the sensing surface of a plug-type heat flux gage and the surrounding wall. Later, Westkaemper (1961) examined this theoretically again and concluded that the error predicted by Rubesin was excessively large, but his result was still valid, i.e. a local discontinuity in surface temperature was a potential source of error of a large magnitude in the heat flux measurements using a plug type calorimeter.

In 1949, Chapman and Rubesin analyzed the general effects of variable surface temperature on the heat transfer coefficient, total heat flux, and flow separation in a compressible laminar boundary layer. They pointed out that the film coefficient and the Nusselt number were not appropriate for the flows with variable surface temperature distribution. This means that the Stanton number is a reasonable choice to analyze the heat flux on the surface of variable temperature. They also derived equations relating the skin friction and the heat flux to illustrate that the skin friction and heat flux analogy, the so-called Reynolds analogy, was inappropriate for the flows with variable surface temperature distribution. Eckert (1955) discussed the relation between the skin friction and the heat flux by adopting the reference temperature concept for both laminar and turbulent boundary layers in high velocity flows.

Consider now the effects of a temperature mismatch on skin friction measurements. The first experimental study was presented by Westkaemper (1963). He used a floating element-type balance with a 2.54 cm diameter copper disk. This balance was mounted through a flat plate in a wind tunnel with a nominal Mach number of 5.0. The flat plate
was maintained at a constant temperature by water cooling, and the disk temperature was controlled by ejecting a few drops of water on its surface. The test results under these particular conditions showed that the effect of moderate temperature mismatch was very strong on the heat flux, but was less than 2 percent of the skin friction force measured with no temperature mismatch. Voisinet (1978) conducted a wind tunnel test at Mach 2.9 and 4.9. The basic concept of temperature control was same as that by Westkaemper. The temperature of the surrounding wall was kept constant, while the temperature of the floating element was cooled by clamping a cryogenically cooled manifold around the element and then releasing it just before the data acquisition. Liquid nitrogen was used as the coolant. This experimental study pointed out that the percentage error in the skin friction measurement could be significant in the flow of high Mach number and/or low Reynolds number where the magnitude of the measured skin friction itself was small.
3. DESIGN OF THE NEW GAGE

3.1 General Design Attributes

A cross-sectional view of the new gage is shown in Figure 3.1. This new gage is a combination of a skin friction balance and a heat flux microsensor.

The skin friction balance is a non-nulling cantilever type floating element balance. A gage connector is soldered on top of the cantilevered tube, and a floating element is soldered on top of the gage connector. Since the principle of this design is to measure the wall shear force caused by air flowing over the upper surface of the floating element, the element must be mounted flush in the plane of the wall to confirm exposure only to a shear force. Then, the floating element is attached to the free end of a cantilevered tube, and it can make a deflection freely to that cantilevered tube according to a load caused by a wall shear force. This deflection gives a tensile force to one side of the cantilevered tube and a compression force to the opposite side of it. These two forces appear as measurable strains on the surfaces near the other end of the tube. Thus, there are strain gages bonded in orthogonal positions around the circumference of the tube.

The active heating system has an axisymmetric co-axial flow pattern in the cantilevered tube. This is why the cantilever beam is tubular, and this tubular form is also easy to be machined. The heating water comes in through the stainless steel tube which is installed just below the floating element and inside the cantilevered tube, and then goes out
through the co-axial gap between the stainless steel tube and the cantilevered tube.

The heat flux microsensor is fabricated directly on the upper surface of the floating element. Its total thickness is less than 2 μm. The presence of this microsensor, therefore, creates negligible disruption on the thermal and mechanical characteristics of the air flow as mentioned by Hager et al. (1991). In fact, the microsensor consists of two parts, heat flux sensor and the resistance temperature sensor. Two feed-through leads from each of them bring the signals out through the lower surface of the floating element, and there is a platinum wire connection to each of the feed-through pins.

The combined balance system is enclosed by a gage housing. There is a series of small set screws around the circumference of the lower end of the gage housing into which the base of the cantilevered tube fits. By adjusting these set screws, the gage head can be moved a small amount in any direction to align the floating element to the center position within the gage housing. The whole balance system can be mounted through the side wall of the wind tunnel using screws through a flange of the gage housing.

3.2 Skin Friction Balance

The floating element is the only component which is directly exposed to the air flow. Since there are the heat flux and the resistance temperature sensors on it, the diameter of the floating element must be decided considering the sizes of these sensors. Hager et al. (1991) used a 25 mm diameter, 6.25 mm thick aluminum nitride substrate for their heat flux gages, but the size of the floating element must be as small as possible for the
measurement of local skin friction force. In this study, the diameter of the floating element is 20.83 mm. This is the smallest size onto which the heat flux microsensor in its current form can be fabricated. Its thickness also must be considered with both the feed-through pins and the effect of the lip force caused by the pressure gradient around the floating element. According to Allen (1980), a reduction in lip size, i.e., the thickness of the floating element, decreases the magnitude of the lip force, and the effect of the gap size between the floating element and the gage housing also can be eliminated. For the new gage, however, the floating element needs some thickness to provide the feed-through pins of the heat flux gage with the minimal contact area of the conical heads. Therefore, the aluminum nitride substrate of 0.508 mm thick was decided, so the ratio of the lip to the diameter was 0.0244 which was within the constraint described by Allen. Even though this small thickness gave only small contact area to the feed-through pins, it was possible to fix the pins through the substrate by using small push nuts and silver epoxy at the backside.

The gage connector is shown in Figure 3.2. Since copper conducts heat very well, it is a good material for the gage connector and the heating system which will be described later. To isolate the gage connector from the feed-through pins, the size of the outer diameter of the connector was decided so small that it could be fixed within the square area made by the four pins. However, the attachment of the floating element (aluminum nitride) and the gage connector (copper) was a big issue. Any kinds of screws could not be used because there would be two sensors on the floating element, and screws might
disrupt the air flow. Epoxy also could not be used since its hardness goes down when the temperature is below 15 °C. A technique was finally developed to solder them. To be soldered, the aluminum nitride substrate needed to have a metal surface. One side of the substrate was, therefore, metalized before the microfabrication of the sensors on the other side. The metalization procedures were as follows:

1. Apply a silver paint to a very clean surface of the substrate
2. Air dry for 30 minutes
3. Heat in a furnace (Blue M, Model POM 206B-1) at 175 °C for 30 minutes
4. Heat in a furnace (BTU Eng. Corp., Model TQ 41436 NY) at 860 °C with the furnace belt speed of 5.08 cm/min

The substrate could have a 0.121 mm thick metal surface. Then, the copper gage connector was soldered easily to the aluminum nitride floating element using a silver paste (Du Pont, soldering and welding composition). Figure 3.3 shows the soldered floating element and gage connector.

The cantilever beam is a very important parameter in the design of the skin friction balance. The performance characteristics such as time response and sensitivity are dependent on the geometry and material of the cantilever beam. For the new gage, the beam has a circular tube shape, because the heating system requires an axisymmetric, co-axial flow of heating fluid. To be fitted into the gage connector, the outer diameter is 6.045 mm and the inner diameter is 3.988 mm. One tip is fitted into the gage connector
and the other tip is fitted into the tube base. Thus, the tube length between the connector and the base is 6.713 cm. The material and geometry of the tube can be determined by considering a measurable strain at the end of the tube while the remaining part must be stiff enough to keep the deflection of the tube small at the other end. Since the skin friction balance of the new gage is a non-nulling type, the deflection of the tube must be kept as small as possible to prevent an error due to a protrusion of the floating element. From the skin friction measurements by Chadwick (1992), the order of magnitude of the shearing force could be estimated. So, the following parametric study was undertaken to choose the cantilever beam material. Hooke's law gives the relation between stress and strain as

\[ \varepsilon = \frac{\sigma}{E} \]

where \( E \) denotes the Young's modulus and \( \sigma \) is the stress applied to the beam. This stress can be expressed as

\[ \sigma = \frac{P L d_{\text{out}}}{2 I} \]

where \( P \) is the load applied at the end of the beam, \( L \) is the length of the beam, and \( d_{\text{out}} \) is the outer diameter of the beam. Then, the resulting strain at the base of the cantilever beam is
\[ 
\varepsilon = \frac{P L d_{out}}{2EI} 
\]

The moment of inertia for a tubular beam is given as

\[ 
I = \frac{\pi}{64} \left( d_{out}^4 - d_{in}^4 \right) 
\]

where \(d_{in}\) is the inner diameter of the beam. Also, the deflection at the end of the beam is given as

\[ 
\delta = \frac{PL^3}{3EI} 
\]

With the above equations, the tube material and geometry could be determined. A fused quartz was initially selected because of its good stiffness and low thermal conductivity. To fit the fused quartz tube into the copper gage connector by soldering, metal bands with the width of 5 mm were made at both of the tips by following the same procedures as the case of the floating element. Then, this fused quartz tube was fitted into the gage connector by silver soldering. The tube was still strong enough against a tensile or a compression force in the direction of its axis, but it was broken by a small torsional force at the soldering location. This weakness might be caused by solder. When the solder was
cooled, it grasped and pressed the surface of the fused quartz tube. Moreover, the fused quartz tube was already pressed when the metal bands were made on it. Brass was, therefore, chosen instead of the fused quartz. The Young’s modulus of brass is higher than that of the fused quartz so that the output signal of the brass tube may be smaller, but handling the brass tube is relatively much easier. Figure 3.4 shows the brass tube soldered with one tip to the gage connector and the other tip to the tube base. The outer diameter and inner diameter of the brass tube are 0.605 cm and 0.399 cm, respectively. The total length from the base to the floating element is 7.856 cm. With this geometry, the maximum deflection at the end of the beam is only 3.4 μm which yields a protrusion of the floating element of only 0.45 μm into the flow. Also, very small coefficient of thermal expansion of brass gives only 0.0006 μm increase in the protrusion of the floating element when the active heating is applied. Therefore, the flow disruption by this amount of protrusion will be negligible. According to Allen’s study, moreover, any error from this type of misalignment can be eliminated by proper design of the gap size which will be discussed soon.

The schematic of the tube base which was constructed of brass is in Figure 3.5. This tube base is not only to fasten the brass tube at one end but also to provide the following three functions:

(1) Fixing the active heating system to the other end

(2) Controlling alignment of the floating element to the center position inside the gage housing by a series of set screws
(3) Providing two ports for the wires from strain gages, which will be discussed soon, to the amplifiers.

Figure 3.6 shows the active heating system. A stainless steel tube of 1.588 mm diameter is for water IN, and a copper tube of 3.175 mm diameter is for water OUT. There are two guides to maintain the stainless steel tube at the center of the cantilever tube and the tube base. These guides are shown in Figures 3.7 and 3.8. The gap between the floating element and the stainless steel tube is 0.762 mm so that the incoming water can make a stagnation point at the center of the backside of the floating element. Then the water flows along the outside of the stainless steel tube and inside of the brass tube, and flows through the lower guide, and it finally goes out through the copper tube.

This skin friction balance fits into a gage housing constructed of brass. Figure 3.9 shows the schematic of the gage housing. The gap size between the floating element and the gage housing is 0.310 mm which yields the ratio of gap to diameter as 0.012. As shown in Allen's study, when the ratio of gap to diameter is greater than 0.01, the effect of lip force is virtually negligible. In the present study, however, great care was taken to assure a perfect alignment as much as possible. There is a series of small set screws ( # 0–80 ) around the circumference of the lower end of the gage housing into which the cantilever tube base fits. The floating element is aligned within the housing by adjusting these set screws. The flange on the outside of the housing has four screw holes. The entire gage arrangement can be mounted on the wall of the supersonic wind tunnel using screws through this flange.
In order to measure the shear force on the floating element, four strain gages were installed in orthogonal positions around the circumference of the brass tube near the tube base. These orthogonal positions were made by grinding flats on the surface of the tube, and each position had an area of 3 mm x 20 mm. The shear force acting on the floating element produces a tensile force on one position and a compression force on the opposing position. Thus, a streamwise component and a cross-streamwise component of the shear force can be measured using strain gages on the four orthogonal positions. Figure 3.10 shows the schematic of the brass tube with the strain gages. In this study, standard foil strain gages (Measurements Group, Inc., Type WK–13–062AP–350) were chosen because of their self-temperature-compensation capability with a matched thermal expansion coefficient for the brass tube. The strain gages are usually cured in a conventional oven to set the curing temperature as required. For this new gage under design, however, the strain gages could not be cured in an oven because it would be dangerous to expose the heat flux and the resistance temperature sensors, which were already made on the floating element, to a high temperature for such a long period as recommended in the strain gage curing instruction bulletin. An alternative method was therefore used as shown in Figure 3.12. The cantilever tube soldered to its base was pulled out from the gage housing so that the strain gage positions on the tube could be completely cleared. A strain gage was bonded on the designated position by coating with M–Bond 610 adhesive, and it was wrapped tightly using a thin Teflon tape. Then, it was cured for two hours by blowing hot air of 150 °C from a heat gun (Master Appliance
Corp., Model HG 751B) while an other heat gun was providing the heat flux sensor and the resistance temperature sensor with cold air. One strain gage was cured at a time. After all four strain gages were cured, M-Line 134-AWQ conductor wires were soldered to each strain gage. Then, the conductor wires were pulled out through two ports of the brass tube base. All the strain gages and the conductor wire connections were coated with M-Coat A. The gap between the strain gages and the gage housing was filled with silicone sealant (Dow Corning) to prevent the temperature effect of the cold air on the strain gages during the operation of the supersonic wind tunnel.

3.3 Heat Flux Microsensor

A heat flux microsensor is a layered heat flux gage. Several thin film layers form a differential thermopile across a thermal resistance layer. The first time the heat flux microsensor was introduced by Hager et al. (1989), it was microfabricated using photolithographic techniques with a wet chemical processing on the substrate. Since this gage was sputter coated onto the surface of a substrate, the total thickness was less than 2 μm and the temperature difference across the thermal resistance layer was very small. According to Hager et al. (1990), the signal of this gage was proportional to the heat flux, and a continuous measurement was possible with response time of 20 μsec. These attractive characteristics allowed the heat flux sensor to be used in both steady and transient flows. However, subsequent tests revealed that this sensor could not withstand high temperatures.
A new high temperature microsensor which was used in this study was made by fabricating stainless steel masks instead of the wet chemical processing. A pulsed argon laser was used to cut the CAD designs directly from the 0.05 mm thick stainless steel sheets. These masks fixed to the surface of the substrate allowed the sputtered films to grow only in the designated areas. A total of six layers of the sensor and forty thermocouple pairs were aligned on to the substrate under a microscope. These layers consisted of silicon monoxide (thermal insulator), nichrome, platinum, silicon monoxide, nichrome, platinum, and the top was coated using aluminum nitride. In the present study, the heat flux sensor and the resistance temperature sensor were fabricated directly on the substrate which is the floating element of the skin friction balance.

Aluminum nitride was chosen as the substrate, since it has high thermal conductivity of 140–170 W/m·°K, high thermal diffusivity of $70.8 \times 10^{-6}$ m$^2$/sec. When the hot water is injected to the center of the floating element, the heat must be spread as soon as possible and as widely as possible through the floating element. Therefore, aluminum nitride with these high thermal characteristics is a good material for the substrate. This ceramic substrate removed the difficulties in isolation of the electric circuits from the substrate. Since the aluminum nitride is very strong against compression forces, diamond tools were used to grind the edge and to make holes for the feed-through pins.

There are four feed-through pins, two for each sensor. The pins were initially fixed by using the push nuts at the backside of the substrate. However, the pins could be easily loosened by any incorrect handling, because the push nuts were very small and the contact
area of the conical heads of the pins were also quite small. Therefore, silver epoxy was used to fix the pins at the backside. Then, the platinum wires connected the pins to the heat flux amplifier.
4. EXPERIMENTAL FACILITY AND INSTRUMENTATION

4.1 Virginia Tech Supersonic Wind Tunnel

The present experiments were performed in the supersonic wind tunnel at Virginia Tech. This tunnel is a blow-down type which exhausts high pressure air from storage tanks to the atmosphere. It has a 23 cm x 23 cm test section and can be run at Mach numbers of 2.4, 3.0, and 4.0 using interchangeable two-dimensional nozzle blocks.

A schematic of this wind tunnel facility is shown in Figure 4.1. The air to be used in the wind tunnel is compressed by a four-stage, water-cooled, reciprocating compressor (Ingersoll–Rand Type 40) driven by a 480 volts, 500 horsepower motor (Marathon Electric Company). The compressed air goes through a dryer to remove moisture and through a prefilter to remove oil. An afterfilter remove particles of dust and dirt. The air is then stored in two high pressure tanks with a combined volume of 23 m³. These storage tanks can be charged up to 54.7 atm. When the tunnel is set to run, the air from these tanks is released to a settling chamber by a 20.32 cm diameter fast-actuating butterfly valve and a 30.48 cm diameter hydraulically actuated servo-valve. This hydraulic valve is controlled to regulate the settling chamber pressure by a control signal from a computer through a servo-amplifier circuit with feedback from the settling chamber pressure transducer. A total pressure probe with a transducer and a temperature probe with an Omega Type-K thermocouple provide the settling chamber information. The settling
chamber also has a perforated transition cone and a set of five screens to reduce flow angularity and turbulence intensity. Then, the air flows into a two-dimensional, converging-diverging nozzle which is designed to compensate for the growing of boundary layer and into the test section. The test section has a constant area and a large door on each sidewall. These doors have glass windows to allow schlieren and shadowgraph photography of the flow field, and also have interchangeable metal windows on which a model or a gage can be mounted. The air then enters a step diffuser, and finally exhausts into the atmosphere through a shrouded exit duct.

4.2 Infrared Radiometer

Even though there is a resistance temperature sensor on the floating element of the skin friction balance, it can measure only the temperature of the point where it is located on the floating element. Therefore, an infrared radiometer was used to simultaneously measure the temperature distribution on the whole surface area of the floating element and the surrounding wall.

Infrared thermal imaging radiometry, also called scanning infrared thermography, uses a small, fast response detector and a set of oscillating mirrors to create a thermal image of a specified area of interest. This area can be made very small using auxiliary optics. The main issue in the application of infrared imaging radiometry to a particular process is the temporal, spatial, and thermal resolution of a specific infrared system.

In the present study, an Inframetrics Model 600 IR Imaging Radiometer was used to
measure the temperature distributions on the surface of the floating element and its surrounding wall. This thermal imaging system produces a National Television System Committee TV-compatible video signal which can be displayed on a monitor or can be recorded on a tape using a high resolution video cassette recorder.

The horizontal scan rate is 8 kHz. In combination with vertical scanning, this horizontal scanning produces a 30 Hz frame rate. The frames are 2:1 interlaced to produce a 60 Hz field rate. A field is composed of 400 lines and each line has 256 pixels. Transient events which can be observed with an ordinary video camera can also be observed by using this thermal imaging system. For the analysis of faster transient events, the system can be used in line scan mode. In this mode, the vertical scanning is disabled and the system continuously scans the same horizontal line. Each traverse for horizontal and vertical directions is performed in 125 μsec so that transient events can be observed up to several kilohertz. The spatial resolution of the system is determined by the properties of the detector or by the combined properties with those of the auxiliary optics if used. A Mercury-Cadmium-Telluride detector has a fundamental instantaneous field of view of 2 milli-radians. With the power microscope lens, the system can resolve an element as small as 30 μm. The thermal resolution of the system is related to the spatial resolution. To assure precise temperature measurements with a scanning system, the element of interest must be larger than the smallest element which can be resolved visually. The minimum detectable difference in temperature is 0.1 °C in the imaging mode. The absolute accuracy of the system is approximately ± 2 °C after emissivity correction.
Since the infrared radiometer uses liquid nitrogen as a coolant, the infrared radiometer must be setup horizontally. So, there had to be a hole through the aluminum window. Through this hole, the infrared radiometer was supposed to measure the temperature distribution on the whole surface area of the gage and the surrounding wall on opposite side of the supersonic wind tunnel. This is why the new gage was installed in the sidewall of the supersonic wind tunnel. However, large pressure difference between inside and outside of the supersonic wind tunnel was a problem in the use of the infrared radiometer, because either a glass or a quartz window can not be used due to their transmittances and the wave lengths for which the detector of the infrared radiometer is sensitive. Sapphire is known as the best material for the infrared radiometer, but it is much too expensive. Thomann and Frisk (1968) solved this kind of problem for a Mach 7 hypersonic wind tunnel by making a window extension. This window extension was covered with a thin plastic foil (Cryovac XL, 0.025 mm thick) which was supported by a perforated force-carrying metal sheet. Their infrared radiometer measured surface temperature of the model installed inside the wind tunnel through this plastic foil. Unfortunately, this plastic foil does not exist anymore.

In the present study, a low density polyethylene film with silica and antioxidants (Cryovac, Duncan, SC., 0.015 mm thick) was used instead of that plastic foil. Also, instead of the window extension, a simple window cover was made to hold the polyethylene film. This window cover was made of two acrylic sheets, and could be attached on the aluminum window using screws. The polyethylene film and a metal screen
were placed between those acrylic sheets. The metal screen had 4.5 mm x 4.5 mm holes. Even though this metal screen was located just in front of the lens of the infrared radiometer, the infrared radiometer could not feel the existence of the metal screen, because the lens was focused on the wall of the other side of the supersonic wind tunnel. Figure 4.2 illustrates the structure of the window cover and Figure 4.3 shows the window cover installed on the aluminum window of the supersonic wind tunnel.

4.3 Probes and Rake

The pressure and temperature distributions in the boundary layer were measured using three probes. The Pitot pressure probe was made of a 1.588 mm diameter stainless steel tube as shown in Figure 4.4. The cone-static probe illustrated in Figure 4.5 has a 10 degree half angle brass cone which was epoxy bonded into a 1.588 mm diameter stainless steel tube. There are four 0.381 mm diameter pressure holes located 2.54 mm from the vertex of the cone. These pressure holes are spaced 90 degrees apart around the cone circumference and emptied into a common chamber to reduce the error due to flow angularity. The total temperature probe consists of a conical Lexan cap and a 1.588 mm diameter stainless steel tube as shown in Figure 4.6. There is a 0.787 mm diameter inlet orifice at the tip of the cap so that the air flow can enter and stagnate inside the cap. Two outlet orifices of 0.254 mm diameter are located 3.81 mm from the tip and are spaced 180 degrees apart around the cap circumference. Just at the inside of the outlet orifices, the junction of an Omega Type K thermocouple is aligned. This thermocouple and the Lexan
cap were bonded with epoxy onto the stainless steel tube.

All these three probes were installed in a rake as shown in Figure 4.7. This rake is a double wedge type with a 11.3 degree half angle. Two pieces of brass single wedge hold the probes by folding together and using small set screws. A 0.508 mm thick stainless steel sleeve was soldered on the rake to prevent excessive deflection during the operation of the supersonic wind tunnel. This rake also has a strut which is a 1.27 cm diameter stainless steel pipe. An aluminum coupling connects the probe rake shaft to the traverse shaft. There is a 6.35 mm x 6.35 mm square hole on the side of the coupling to pull out the Teflon tubes and thermocouple wire coming through the shaft of the probe rake.

4.4 Traverse System

To measure the continuous flow field from the wall, the rake with probes can be automatically moved across the boundary layer using a computer controlled traverse system shown in Figure 4.8.

Its shaft is geared to a stepper motor (Computer Device Corp. Model 4D–9200A), and it can be moved 0.013 cm per step. The motor is run by a controller (American Precision Industries, Model DMA–64), and this controller is conducted by a stepper command processor board (UAI, Model 3071) which communicates with an IBM PC through a standard RS–232C serial port. Therefore, the speed and the distance of the traverse were controlled by the main computer.

The horizontal displacements of the probes were measured with a Linear Voltage
Displacement Transducer (Trans-Tek, Model 0246-0000G-6). The housing of this LVDT is attached to a part of the traverse and the core is fixed to the traverse shaft so that it can move with the probes. This LVDT can measure the probe displacements up to 15 cm at an accuracy of 0.5 percent. A custom-made power supply provides an excitation voltage of 12 VDC to the LVDT.

4.5 Pressure Transducer

Statham Model PA 822–100 Pressure Transducer (0–100 psia) was used for the Pitot pressure measurements, and a PA 285TC–50–350 Pressure Transducer (0–50 psi) was used for the cone static pressure measurements. These transducers are thin film strain gage pressure transducers. The sensing element of each pressure transducer utilizes a vacuum-deposited fully active strain gage bridge, and a ceramic film provides an electrical insulation for the bridge element.

4.6 Data Acquisition

A custom-made four-pole Bessel filter was used to filter high frequency instrumentation noise. All the signals of the boundary layer measurements, except thermocouple signals, were filtered before being recorded. As mentioned by Horowitz and Hill (1980), this Bessel filter has a characteristic of a uniform time delay which is independent of frequency.

Ectron amplifiers were used for the signals from the pressure transducers in the
boundary layer measurements, and the signal conditioning amplifier of Measurements Group was used for the strain gage signals of the skin friction balance.

The Ectron amplifier ( Model 562 FJ ) is a true differential DC instrumentation amplifier. It provides linearity, gain stability, and input impedance. Direct coupling and very high loop gains assure overall accuracy. The internal circuitry uses discrete components and integrated circuits to provide high performance. All components are mounted on a single circuit card, and the output signal is ±10 volts at 10 mA with a frequency response of 80 kHz. The P516–5SG strain gage excitation supply delivers the maximum current of 1.2 A, and the range of continuous excitation voltage adjustment is from 2.5 V to 15 V in two parts; 2.5V ~ 5 V and 5V ~ 15 V. The regulation is ±0.01 % for a 10 % change in load or line voltage, and the output noise is less than 1 mV rms. Sensing terminals enable a constant voltage at a remote destination under varying load conditions.

The Measurements Group Model 2310 Strain Gage Signal Conditioning Amplifier is designed for both conditioning and amplifying low-level signals from strain gages or strain gage based transducers. This amplifier incorporates precision high-stability bridge completion resisters, dummy gages, and four shunt-calibration resistors. Some principal features include:

- Fully adjustable calibrated gain from 1 to 11000
- 12 steps bridge excitation from 0.5 to 15 VDC
- Input impedance above 100 MΩ
- Wide-band operation exceeding 25 kHz, -0.5 dB at all gains and output levels

- Four cut-off frequency active filter from 10 to 10000 Hz

- Automatic balance reset

A personal computer with an 80286 micro-processor was used to acquire all the data. This computer is equipped with a Metrabyte Model DAS–20 High Performance Analog and Digital Interface Board. This board has an on-board Channel/Gain queuing RAM which allows a list of Channel/Gain to be created, and the board also automatically follows the desired sequence with 12-bit resolution. DAS–20 can be operated in 16 channels single ended or 8 channels differential input modes. It also has 2 analog output channels and 16 digital input/output lines. A Metrabyte Model EXP–20 Expansion Multiplexer was used for temperature measurements. It has 16 fully differential inputs which can be multiplexed into a single input channel of DAS–20 board. Since the EXP–20 includes an On-board electronic cold junction temperature sensor and instrumentation amplifier, it is ideally suited for measuring the thermocouple inputs.

These DAS–20 and EXP–20 data acquisition boards were controlled with a commercially available software package, LABTECH NOTEBOOK (Laboratory Technologies Corp., Version 4.0). With this software, the calibration equation for each channel can be directly entered to create the output file containing data in engineering units. For temperature measurements with various types of thermocouples, this NOTEBOOK performs a polynomial linearization on the compensated thermocouple voltage and records the temperature in the specified unit. Data sampling rate and time
period can be specified according to measurements. This data acquisition system can be either manually triggered or automatically triggered through an interface with the main computer for the wind tunnel operation.
5. INSTRUMENTS CALIBRATIONS

5.1 Pressure Transducers Calibrations

Each calibration of the pressure transducers was conducted by exposing the transducers to several known pressures, and then by computer reading each output voltage.

For a calibration of the Pitot pressure transducer, pressures were provided by a pneumatic pressure tester (AMETEK, Model MK 100) which was connected to a commercial air bottle. This dead weight tester can provide pressures from 1 atm to 7 atm with minimum increment of 0.073 atm. The air bottle gave high pressure to the dead weight tester and this tester regulated the output pressure according to the given dead weight, and then this known pressure was sent to the pressure transducer.

The cone-static pressure transducer was calibrated with pressures less than 1 atm using a HEISE pressure gage and a vacuum pump (Welch Scientific Co. Model 1400). The schematic of this calibration is shown in Figure 5.1. A tube line from a commercial air bottle was divided into two lines. One line was connected to the vacuum pump through an ON/OFF valve and the other line was divided again into two lines. One line was connected to the HEISE pressure gage and the other line was used to supply pressures to the pressure transducer. The calibration procedure was simple. First, the vacuum pump drained all air inside the tube lines, and then the ON/OFF valve was closed. Second, let the
other ON/OFF valve leak air from the air bottle and this pressure was read on the HEISE pressure gage. Third, close this ON/OFF valve when the pressure reached the expected level, and let the data acquisition computer read the output voltage from the pressure transducer. Fourth, repeat controlling the ON/OFF valves and recording the output voltages.

During each calibration, the output voltages were amplified by Ectron amplifier and filtered by the custom-made Bessel filter, and then were read by the computer equipped with DAS–20 A/D system. The sampling rate was 100 Hz, and the stage period was 5 seconds. A Least Squares Curve Fit program was used to compute the coefficients of a first-order linear equation which related the output voltages to the corresponding pressures.

5.2 LVDT Calibration

Calibration of the Linear Voltage Displacement Transducer was performed using a PTI Cat. No. 2210 Cathetometer. Since the skin friction and heat flux gage would be installed on the sidewall of the supersonic wind tunnel, LVDT was used to measure the horizontal displacements of the pressure and the temperature probes from the surface of the sidewall. The cathetometer was designed to measure vertical displacements. Thus, a simple mounting was made of aluminum plates to hold the cathetometer horizontally as shown in Figure 5.2. Measuring the probe displacements in horizontal direction may cause error due to angular deviation between the probes and the cathetometer, because the
probes move inside the test section and the cathetometer is placed outside the test section of the wind tunnel. Therefore, the cathetometer read the horizontal displacements of the traverse shaft instead of those of the probes. Since the probe rake is connected to this traverse shaft, the probe displacements are same as the shaft displacements. A 2 cm x 3 cm yellow tape was attached to rear end of the traverse shaft and a black line was marked vertically on the tape to indicate the shaft displacements. The three probes, rake, and traverse were completely installed in such a way that the probes touched the wall surface. A cathetometer reading at this point was used as the surface reading. The readings were then made for three horizontal positions of the black line. Corresponding voltages were measured by the DAS-20 system, and a Least Squares Curve Fit program were used to compute the coefficients of a linear equation which represented the relation between the output voltages and displacements.

5.3 Skin Friction Balance Calibration

The most popular method for the calibration of the skin friction balance is directly applying a force to the desired measurement axis of the skin friction balance. This force can be applied using the weight standards which are attached to the center of the floating element via a thin thread. Of course, the balance should be fixed very carefully in a horizontal direction which yields the desired calibration axis to be vertical.

It is very convenient to use a Scotch tape to attach one end of the thread to the center of the floating element surface. For this study, however, the thread could not be
attached to the center of the floating element, because there were the heat flux sensor and the resistance temperature sensor. Thus, two points on the vertical center line of the floating element were chosen for thread attachment. General Scotch tape also could not be used due to the active heating. The water temperature for the active heating was supposed to be 40 °C, and the Scotch tape lost its adhesive force on the floating element when this hot water was supplied to the other side of the floating element. Therefore, another material was needed to hold the thread stably. M-Coat A (Measurements Group, Inc.) and a Cement (True Bond) were tried instead of Scotch tape, and M-Coat A was selected, because it did not show any defect in its characteristics regardless of that hot water.

Figure 5.3 illustrates the schematic of the skin friction balance calibration. The output signals from the strain gages were filtered and amplified using a strain gage conditioning amplifier (Measurements Group, Inc. Model 2310), and then were read by the Zenith data system. Sampling rate was 100 Hz for 3 seconds. These data were averaged for each test weight, and the Least Squares Curve Fit provided a linear calibration curve. Calibrations were performed without and with the active heating. As shown in Figures 5.4 and 5.5, the output signal of the skin friction balance measured with the active heating was about 1.5% higher than that measured without the active heating.
5.4 Heat Flux Microsensor Calibration

Calibration of the heat flux microsensor was performed by comparison with a Gardon gage which had been already calibrated. An infrared spot heater (Research Inc., Model 4141) provided the heat flux microsensor and the Gardon gage with same amount of heat, and then the output voltages from each of them were compared. The scale factor of the heat flux microsensor was 92.7 W/cm²/volt after amplification, while that of the Gardon gage was 37.5 W/cm²/volt.

5.5 Infrared Radiometer Calibration

Since the polyethylene film has its own transmittance, the infrared radiometer needed a calibration of the actual arrangement. The infrared radiometer shows the temperature of an object as a color, and there is a color band indicating the temperature of the corresponding color on the monitor screen. Therefore, a thermocouple was embedded in the aluminum window just above the location of the new gage, and this aluminum window with the new gage was exposed to a steady state temperature for a few hours. Then, the surface temperature of the gage became to be the same as that of the surrounding wall. So, the shape of the gage could not be distinguished from the surrounding wall on the monitor screen, because the gage and the surrounding wall had the same color. Then, the calibration of the infrared radiometer was performed by comparing the thermocouple temperatures and the colors on the monitor screen.
6. INSTRUMENTATION SETUP AND DATA PROCESSING

6.1 Boundary Layer Measurements

As already mentioned, the new combined skin friction and heat flux gage was installed in the sidewall of the supersonic wind tunnel to use the infrared radiometer which measured the transient surface temperatures on the floating element of the gage and the surrounding wall. Thus, the flow conditions also had to be measured on the sidewall. Allen (1980) said, moreover, that the wind tunnel sidewall provided a long run of turbulent flow and a thick boundary layer.

Tip locations of the probes were illustrated in Figure 6.1. One probe on the top position had its tip outside the gage area, the probe on the middle position was aligned to the center of the floating element surface, and one probe on the bottom position was located such that its tip was near the edge of the floating element surface. The shaft of the probe rake was connected to that of the traverse through the sidewall. The traverse system including LVDT was installed just outside the sidewall as shown in Figure 6.2. Teflon tubes of the pressure probes were pulled out through the shaft of the probe rake and its coupling and then connected to corresponding pressure transducers. These pressure transducers transferred the output signals to the Ectron amplifiers and then the Bessel filters. After being amplified and filtered, the signals were read and recorded by the Zenith data system with the DAS–20 A/D converter. Omega type K thermocouple wire of the
total temperature probe was also pulled out through the same route as the Teflon tubes, and then directly connected to the Metrabyte EXP-20 expansion multiplexer of the DAS-20 A/D system. The traverse moved 6.35 mm per second, and all data was recorded for 5 seconds with the sampling rate of 100 Hz.

The initial conditions of the boundary layer flow were measured about 26 times of the boundary layer thickness upstream from the location of the gage. The initial displacement from the sidewall to the center line of the probes was 0.794 mm for the measurements of the initial conditions. It was, however, 1.449 mm at the location of the measurements of the skin friction and the heat flux, because there were the heat flux microsensor and the resistance temperature sensor on the surface of the gage.

According to Bowersox (1992), the Mach number can be expressed as a function of the ratio of the cone-static pressure (P_c) to the Pitot pressure (P_t). He obtained the equation using the Least Squares Curve Fit as

$$\frac{1}{M} = -0.052976 + 4.684 \times - 18.6786 \times^2 + 50.7006 \times^3 - 54.1577 \times^4$$

where $\times = P_c/P_t$ and $M \in [1.5, 4.4]$. With the Mach number from this equation, another flow quantities were computed using the usual perfect gas relations for the compressible flow like the Rayleigh Pitot formula and the Sutherland's equation from NACA 1135 and Bertin-Smith (1989).
Considering the temperature signal from the thermocouple, every 5 points of both the probe displacement and the flow quantity data were averaged into a single data pair for smoothing the data without distorting it.

6.2 Skin Friction, Heat Flux, and Surface Temperature Measurements

Figures 6.3 and 6.4 show the actual setup of the new gage and the infrared radiometer. The output signal of the skin friction balance was filtered and amplified by the signal conditioning amplifier, and the output signal of the heat flux microsensor was amplified by the Vatell heat flux gage amplifier. Those signals were then read by the Zenith data system with the DAS-20 A/D converter. The control electronics unit of the infrared thermal imaging system received the signal from the scanner, then sent it to the RGB monitor to display the measured thermal image. This thermal image was then sent to VCR and recorded on the tape.

The skin friction balance of the new gage measured shear force over the surface area of the floating element. This shear stress was then normalized by the dynamic pressure to obtain the coefficient of the skin friction. The dynamic pressure of the freestream is defined as

\[ q = \frac{1}{2} \rho_\infty U_\infty^2 \] (6.1)
Assuming a perfect gas,

\[ q = \frac{1}{2} \gamma P_\infty M_\infty^2 \]  \hspace{1cm} (6.2)

Here, the Mach number \((M_\infty)\) and the ratio of the specific heats \((\gamma)\) are known. The static pressure can be calculated from the measured total pressure using the isentropic relation:

\[ P = \frac{P_t}{\left[ 1 + \frac{\gamma - 1}{2} M^2 \right]^{\frac{\gamma}{\gamma - 1}}} \]  \hspace{1cm} (6.3)

Then, the coefficient of the skin friction can be obtained as

\[ C_f = \frac{\tau_w}{q} \]  \hspace{1cm} (6.4)

Since the heat flux microsensor shows the amount of heat flux as the output voltages, the amount of heat flux is simply

\[ q = S \cdot E \]  \hspace{1cm} (6.5)
Then, the heat transfer coefficient (Stanton number) can be obtained as

\[ St = \frac{\dot{q}}{\rho_\infty U_\infty (H_w - H_\infty)} \]  \hspace{1cm} (6.6)

where

\[ H_w = C_p T_{tw} = C_p T_w \]  \hspace{1cm} (6.7)

\[ H_\infty = C_p T_{te} \]  \hspace{1cm} (6.8)

Usually, for high speed flows, the heat transfer coefficient is expressed using the reference temperature concept as

\[ St^* = \frac{\dot{q}}{\rho^* U_e C_p (T_w - T_{aw})} \]  \hspace{1cm} (6.9)

where the * superscript denotes that the properties are to be evaluated at the reference temperature. This reference temperature is given as

\[ T^* = T_e + 0.5 (T_w - T_e) + 0.22 (T_{aw} - T_e) \]  \hspace{1cm} (6.10)
where the adiabatic wall temperature can be obtained as

\[ T_{aw} = T_e + r \frac{U_e^2}{2 C_p} \]  \hspace{1cm} (6.11)

with the recovery factor \( r = Pr^{1/2} \) for a turbulent flow. However, as already mentioned, the results of the experimental measurements will be compared with the computational results using the CFD code GASP, and GASP uses the equation (6.6) to calculate the heat transfer coefficient. Therefore, in the present study, the equation (6.6) was used to obtain the heat transfer coefficient for the purpose of the comparisons.
7. EXPERIMENTAL RESULTS AND DISCUSSION

7.1 Boundary Layer Measurements

Boundary layer profiles were measured first at the location of the gage, and then measured at the upstream location. The distance between the upstream location and the gage location was 26.24 cm. It was about 26 times the boundary layer thickness of the gage location.

Figure 7.1 shows the flow conditions at the upstream location. The boundary layer thickness was about 0.686 cm. Figure 7.2 represents the flow conditions at the gage location. Since these measurements were conducted on the new gage, the height of the first measuring point was about 0.24 cm above the surface of the floating element to preserve the heat flux sensor. Freestream Mach number was 2.4, Reynolds number per unit meter was \(4.87 \times 10^7\) with total pressure of 5.2 atm and total temperature of 300 °K. The boundary layer thickness was about 1.005 cm, so the boundary layer grew 46.5 % between the upstream location and the gage location. The freestream showed small fluctuations in the flow direction, but all of the profiles were very smooth within the boundary layer. Therefore, this flow was a compressible, developing viscous turbulent flow. Velocity, pressure, and density profiles of the upstream measurements were used as the initial flow conditions to the calculations to be described later.
7.2 Skin Friction and Heat Flux Measurements

A total of nine measurements were conducted for each case of the active heating ON and OFF conditions. Output signals from the skin friction balance and the heat flux microsensor are shown in Figures 7.3 and 7.4, respectively. The passage of the starting normal shock can be seen on the left of the figures, and the shutting down of the supersonic wind tunnel can be seen on the right of the figures. Also, after shutting down of the supersonic wind tunnel, the signals went back to their initial levels which were shown before starting of the supersonic wind tunnel. This fact indicates that the new gage is quite reliable and can be used repeatably. The time period of interest was chosen from 6 to 12 seconds of each measurement for data reduction, because this time period was applicable commonly to all of the measurements, considering the time of the starting normal shock and the stopping time of the supersonic wind tunnel.

Tables 7.1 and 7.2 summarize the results of the measurements without and with the active heating, respectively. When the active heating was OFF, the mean skin friction coefficient was 0.00156 for all nine measurements with maximum deviation from the mean of 2.3 %, and the mean heat transfer coefficient (Stanton number) was 0.00122 with maximum deviation from the mean of 15.2 %. An estimate of the skin friction coefficient was calculated using the empirical Schultz-Grunow relation for the skin friction coefficient in wall-bounded incompressible turbulent boundary layer:

\[ C_f = 0.0456 \text{ Re}^{0.25} \]
This relation is valid up to approximately $\text{Re}_x = 10^7$. The Reynolds number based on the boundary layer thickness could be obtained from the results of the boundary layer measurement at the gage location. Then, using the compressible/incompressible skin friction coefficient relation given by Van Driest II (Schetz, 1984), this incompressible skin friction coefficient was converted to a compressible skin friction coefficient. The estimated skin friction coefficient was 0.0013 which was about 17% lower than the measured skin friction coefficient. When the active heating was ON, the mean skin friction coefficient was 0.00199 with maximum deviation from the mean of 4.0%, and the mean heat transfer coefficient was 0.00684 with maximum deviation from the mean of 9.3%. So, the skin friction coefficient was increased about 28% and the heat transfer coefficient was increased about 460% by the active heating. From these results, it was showed that accurate measurements can be obtained using a non-nulling cantilever type skin friction balance with active heating of the floating element.

7.3 Surface Temperature Measurements

After each run of the supersonic wind tunnel, the aluminum window through which the gage was installed was taken off from the wind tunnel, and was placed on a table which was covered with a thick cloth. Then the surface temperature was very carefully monitored using a thermocouple which was embedded in the aluminum window. When the surface temperature was recovered to that before running the supersonic wind tunnel, the aluminum window was installed on the supersonic wind tunnel again. By doing this, the
initial surface temperature of the aluminum window could be kept constant for every run of the supersonic wind tunnel.

Figure 7.5 shows the history of temperature variations on the gage and the surrounding wall during the simultaneous measurements of the skin friction and the heat flux. In this figure, top and bottom lines show the temperature variations when the active heating was ON, and the two lines in the middle represent the temperature variations when the active heating was OFF. Also, the solid symbols indicate the surface temperature of the gage, and the empty symbols show the surface temperature of the surrounding wall. Therefore, the amount of temperature mismatch i.e. the difference between the surface temperature of the gage and the surface temperature of the surrounding wall can be read from this figure. When the active heating was OFF, it was from 2.5 °K to 4.5 °K. The active heating produced the temperature mismatch of 18.7 °K. Therefore, the ratio of the temperature mismatch to the freestream total temperature is 0.062 which corresponds to the levels typically found in high heat flux cases.

7.4 Effects of Temperature Mismatch

To investigate the effects of the temperature mismatch, it was necessary to normalize the skin friction coefficient by the corresponding total pressure, since the absolute value of the skin friction coefficient was dependent on the magnitude of the total pressure as shown in Figure 7.6. Figure 7.7 shows a typical shape of the normalized skin friction coefficient. As shown in the figure, this normalization completely eliminated the dependence of the
skin friction coefficient on the total pressure. Also, the time period of interest was chosen from 6 to 12 seconds, because this time period was applicable commonly to all of the measurements with considering the time of the starting normal shock and the closing of the supersonic wind tunnel.

Figure 7.8 represents the maximum, mean, and minimum of the normalized skin friction coefficients at each time step of nine measurements for each case of the active heating ON and OFF. The uncertainty range of the skin friction measurements was $\pm 4.5\%$ with the active heating ON, and $\pm 7.2\%$ with the active heating OFF. The percent increase in the normalized skin friction coefficient by the active heating is shown in Figure 7.9. The top line in Figure 7.9 indicates the percent increase from the lower dashed line in the case of heating OFF to the upper dashed line in the case of heating ON in Figure 7.8. The bottom line in Figure 7.9 represents the percent increase from the upper dashed line in the case of heating OFF to the lower dashed line in the case of heating ON in Figure 7.8. The middle line in Figure 7.9 shows the percent increase from the solid line in the case of heating OFF to the solid line in the case of heating ON in Figure 7.8. In Figure 7.9, the average of the maximum increase line is 37\%, that of the minimum increase line is 8\%, and that of the mean increase line is 22\%. Now, the amount of temperature mismatch at each time step can be read in Figure 7.5, and the normalized skin friction coefficient at the corresponding time step can be read in Figure 7.8. Then, the effect of temperature mismatch on the skin friction measurements is revealed. This effect is shown in Figure 7.10. In this figure, the circular symbols represent the measured data, and the solid line
came from the Least Squares Curve Fit. When the temperature mismatch was zero i.e. the surface temperature on the floating element is equal to that on the surrounding wall, the normalized skin friction coefficient was $2.13 \times 10^{-5}$. In the range of the temperature mismatch produced without the active heating, it was from $2.16 \times 10^{-5}$ to $2.19 \times 10^{-5}$. So, the normalized skin friction coefficient was increased from 1.4 % to 2.8 % without the active heating. When the active heating was ON, the normalized skin friction coefficient was measured as $2.64 \times 10^{-5}$. This presents 24 % increase in the normalized skin friction coefficient. Westkaemper (1963) said the effect of temperature mismatch was negligible in his particular measurements, but Voisinet (1978) concluded that an effect existed and this effect could be significant. As Voisinet expected, the effect was sizable in this study.

Following the same procedures, the heat transfer coefficient, the percent increase in the heat transfer coefficient, and the effect of temperature mismatch on the heat flux measurements are shown in Figures 7.11, 7.12, and 7.13, respectively. In Figure 7.11, the uncertainty range of the heat flux measurements was ± 9 % with the active heating ON, and ± 13 % with the active heating OFF. The average of the maximum increase line in Figure 7.12 is 590 %, that of the minimum increase line is 350 %, and that of the mean increase line is 450 %. The solid line in Figure 7.13 indicates the heat transfer coefficient as $1.01 \times 10^{-3}$ when the temperature mismatch was zero. The measured range of the heat transfer coefficient without the active heating was from $1.25 \times 10^{-3}$ to $1.30 \times 10^{-3}$. Thus, the heat transfer coefficient was increased from 24 % to 29 % without the active heating.
The heat transfer coefficient was measured as $6.90 \times 10^3$ with the active heating. This shows 580 % increase in the heat transfer coefficient. Hornbaker and Rall (1964) measured 25 % increase, and Bachmann et al. (1965) measured up to 50 % increase in the heat transfer coefficient with their particular experimental apparatuses.
8. COMPUTATIONAL RESULTS

8.1 Software Used

A computational fluid dynamics code GASP (General Aerodynamic Simulation Program) version 2.0 was used to simulate the experimental measurements. According to McGrory et al. (1992), GASP is a fully conservative shock capturing CFD code. Its library consists of 206 files divided into 6 directories. Its source code and utilities incorporate 37 FORTRAN and C source code files which contain 584 subroutines with over 88,000 lines of code. GASP was made to solve the integral form of the time-dependent, three dimensional Reynolds-Averaged Navier-Stokes equations subject to initial and boundary conditions. It can also solve subsets of the Reynolds-Averaged Navier-Stokes equations including the Thin-Layer Navier-Stokes equations, the Parabolized Navier-Stokes equations, and the Euler equations.

The variable with which the user works predominantly is the primitive variable vector, even though GASP is a fully conservative code. This primitive variable vector contains the species densities, the velocity components, the non-equilibrium vibrational energies, the pressure, the turbulent kinetic energy, and the dissipation rate. It is not necessary that all of these variables be given. The number of variables is dependent on the model, and the user can select only some of them appropriately. Therefore, the user can specify the inflow conditions and the boundary conditions on the wall which may be
discontinuous. This is the main reason that GASP was chosen here to simulate the present experimental measurements with discontinuous surface temperature.

8.2 Grid System

The coordinate system was set as the conventional Cartesian coordinate system according to the rule of right-handed rotation. The axes were denoted by X in the flow direction, Y in the outward direction from the wall, and Z in the span direction. Unit vectors are i, j, and k, respectively.

To compare with the experimental measurements without the active heating and to verify the grid choice, two-dimensional calculations were conducted before the three-dimensional calculations. Figure 8.1 shows the two-dimensional grid. Its dimension is 101 × 41, and the numerical domain is 0.525 m × 0.367 m. The grid is uniform in the X direction, but evenly clustered at the location of the gage. The hyperbolic tangent method (Thompson et al., 1985) was used to stretch the grid from the wall to the freestream so that the grid is clustered near the wall in order to resolve the boundary layer. As mentioned in McGrory et al. (1992), a minimum of approximately 10–15 points are required in the boundary layer to predict accurate results, but placing too many points in the boundary layer is not recommended because other areas of the flow also may require better resolution. Concerning the height of the first grid point above the wall, a general rule for turbulent problems is that the order of the non-dimensional distance from the wall, $y^+$, of the first grid point should be unity. In the present study, because of the complexity
of the three-dimensional boundary layer, 24 points were placed in the boundary layer, and the height of the first grid point was $3.18 \times 10^{-4}$ m from the wall so that $y^+$ was about 1. This two-dimensional grid was converted into three-dimensional grid as required by GASP (101x41x2). The numerical domain converted in the span direction was the diameter of the floating element of the new gage as shown in Figure 8.2 (a). In this figure, the circle represents the floating element. For three-dimensional calculations, the two-dimensional grid was expanded to the span direction so that its dimension was $101 \times 41 \times 12$ as shown in Figure 8.2 (b) and its numerical domain was 0.525 m x 0.367 m x 0.033 m. The grid area representing the floating element in calculations was 96 % of the real area of the floating element. A grid cell has a square shape, and its length is equal to $1/7$ of the diameter of the floating element. By doing this, the measured surface temperature distribution on the floating element could be input correctly. There are two strips of grid cells containing the freestream conditions at each side of the grid area which represents the floating element on the grid.

Since these calculations are to simulate the case that a supersonic flow is to face to the condition of discontinuous temperature distribution on the wall, space marching in the flow direction was adopted.

### 8.3 Initial and Boundary Conditions

From the boundary layer measurements at the upstream location, the velocity, the pressure, and the density profiles were obtained to input into GASP as the initial
conditions at the first x-plane. These inflow profiles are shown in Figure 8.3 with the measured data. Least Squares Curve Fit was used to get the approximate profiles of the velocity and the density between the wall and the first measured point. As mentioned in Section 8.1, GASP could read these inflow profiles pointwise from the two input files, bci.z1 file specifying the types of the pointwise boundary conditions and bcq.z1 file containing the flow profiles. In the present study, however, all of the inflow profiles were given in the input file bcq.z1 using the same boundary condition type so that the other input file bci.z1 was not used and GASP was set to read the inflow profiles directly from bcq.z1 file (GASP inputs: i0bc = 2, idimbc = 3).

To input the discontinuous surface temperature distribution as measured, the air was assumed as a perfect gas, and the static pressure was also assumed as constant on the wall. This latter assumption is permissible in the current flow configuration. Velocity was set to zero (u = 0, v = 0, w = 0) on the wall. Thus, GASP realized the discontinuity in the surface temperature distribution by reading the velocity, the pressure, and the density at each point in the space marching direction from the input file bcq.z1 (j0bc = 2, jdimbc = −3).

For the boundary conditions at the first and last Z-planes, GASP was set to extrapolate all flow quantities from the interior to first order accuracy (k0bc = −3, kdimbc = −3).
8.4 Inviscid and Viscous Fluxes

For the inviscid flux calculations, second order upwind biased full flux input flags were chosen in the marching direction (invflxi = 4, rkapi = -1.0), third order upwind biased Roe split flux was selected in the vertical direction (invflxj = 3, rkapj = 0.3333), but there was no inviscid flux in the span direction (invflxk = 0, rkapk = 3.0). A catastrophic limiter based upon the density and the pressure was used for the space marching direction, the Min-mod limiter was used for the vertical direction, and there was no inviscid flux limiter in the span direction (limi = 1, limj = 2, limk = 0).

The Baldwin-Lomax algebraic model is known as the most suitable turbulence model for wall bounded flows. In the present study, this model was used with thin layer approximations to calculate the viscous flux in the vertical direction (visflxi = 0, visflxj = -2, visflxk = 0). Laminar viscosity and thermal conductivity were computed with the Sutherland formula (modlmu = 2, modlk = 2), and the spatial accuracy of the wall gradient calculations was set to second order (ivac = 2). The turbulent Prandtl number and the turbulent Schmidt number were set to their typical values (prt = 0.9, sct = 0.5). Since the Baldwin-Lomax turbulence model is an algebraic model, all of the remaining control parameters for the two equation turbulence models were turned off (ikeps = 0, ikejac = 0, kemin = 0, kefill = 0).

8.5 Results and Discussion

The predictions of the flow at the gage location by two-dimensional calculations are
presented with the measured data without the active heating in Figure 8.4. Freestream conditions were close to each other, but the shapes of the computed profiles were plumper than those of the measured profiles in the boundary layer even though the boundary layer thicknesses were almost same. Therefore, the computed flow had higher Mach number and velocity, but it had a lower temperature in the boundary layer.

Figures 8.5 and 8.6 show the skin friction coefficient normalized by freestream total pressure and its percent increase according to increase in the amount of temperature mismatch, respectively. Each symbol represents the measured or the calculated data, and the solid lines were derived using the Least Squares Curve Fit with the corresponding data. When the temperature mismatch is zero i.e. the surface temperature on the gage and that on the surrounding wall are same, the normalized skin friction coefficient was $2.13 \times 10^{-5}$ by measurements and $2.05 \times 10^{-5}$ by calculations. The difference between measurements and calculations was only 4%. However, the difference was increased as the amount of temperature mismatch increased. As shown in Figure 8.5, the solid lines indicate that the difference was less than 10% of the measured data when the temperature mismatch was less than 9 oK.

GASP predicted the dimensionless heat transfer coefficient (Stanton number) better. Figures 8.7 and 8.8 represent the heat transfer coefficient and its percent increase according to increase in the amount of temperature mismatch, respectively. When the temperature mismatch was zero, the heat transfer coefficient was $1.01 \times 10^{-3}$ by measurements and $1.04 \times 10^{-3}$ by calculations. Thus, GASP predicted it only about 3%
greater than measurements. The difference in the heat transfer coefficient was also increased as the amount of the temperature mismatch increased. The solid lines shown in Figure 8.7 present that the difference was less than 10 % of the measured data when the temperature mismatch was less than 8.5 °K.

Therefore, the computational results and the experimental results were quite close to each other when the active heating was not used. This means that the results of the two-dimensional calculations were satisfactory to be compared with the results of the experimental measurements conducted without the active heating, and that the grid used here was appropriate to simulate the present experimental measurements. However, the real flow was three-dimensional in the experimental measurements.

For three-dimensional calculations, the input temperature distributions on the gage and the surrounding wall were obtained from the results of the experimental measurements with the active heating. The amount of surface temperature mismatch between the floating element and the surrounding wall was 18.7 °K at the heating area around the center of the floating element, 17.5 °K between the heating area and the edge of the floating element, and 12.5 °K at the edge of the floating element. The results of the three-dimensional calculations were presented as a square symbol in Figures 8.5 through 8.8. The normalized skin friction coefficient was $2.64 \times 10^{-5}$ by measurements and $2.04 \times 10^{-5}$ by calculations. The heat transfer coefficient was $6.89 \times 10^{3}$ by measurement and $1.08 \times 10^{3}$ by calculations. Therefore, GASP predicted the normalized skin friction coefficient and the heat transfer coefficient as 23 % and 84 %, respectively, less than measurement. From this

**Computational Results**
comparison, it is considered that the Baldwin-Lomax algebraic turbulence model could not properly treat the response of the turbulent structure of the flow to the sudden temperature gradient on the wall. The active heating would produce a new thermal boundary layer on the floating element which was different from the existing boundary layer on the surrounding wall. When a supersonic turbulent flow interacts with this new boundary layer, some variations are expected in the viscous and turbulent characteristics of the flow like amplification of turbulence fluctuations. However, if a turbulence model did not take those variations into account, then it could not predict mechanical and thermal phenomena correctly. In particular, the length scale predicted by a Baldwin-Lomax type model cannot account for both the existing and the new boundary layer. In such models, the length scale is computed from the velocity profiles, and here the thermal boundary layer will have two length scales – one for the existing boundary layer and one for the new boundary layer. Therefore, it is presumed that the disagreement between the experimental results and computational results was caused mainly by deficiencies in the turbulence model for this complex, developing, viscous flow. The grid chosen was much finer than suggested for the two-dimensional cases in such flows. However, one must always allow for the possibility that the grid choice contributed to the disagreement.
9. CONCLUSIONS AND FUTURE WORK

9.1 Conclusions

The major conclusions reached from this research are keyed to the goals of the study described in Chapter 1.

(1) Simultaneous direct measurements of skin friction and heat flux

Simultaneous direct measurements of skin friction and heat flux were successfully conducted in the Virginia Tech supersonic wind tunnel. The freestream Mach number was 2.4 and Reynolds number per meter was $4.87 \times 10^7$ with total pressure of 5.2 atm and total temperature of 300 °K. For these direct measurements, a new gage which can measure the skin friction and heat flux simultaneously was designed, constructed, and tested. The main features of the new gage are

- Non-nulling floating element type skin friction balance using the standard foil strain gages which have the self-temperature-compensation capability

- Heat flux microsensor fabricated on the floating element of the skin friction balance

- Active heating/cooling system inside of the cantilever beam of the skin friction balance that permits control of the temperature of the floating element

Calibrations of both skin friction balance and heat flux microsensor showed linear and
repeatable response. A total of nine measurements were conducted for each case of the active heating ON and OFF. Inspecting the output signals from the skin friction balance and the heat flux microsensor showed that the new gage is quite reliable and can be used repeatably in a supersonic wind tunnel. Results of the tests presented show that accurate measurements can be obtained using a non-nulling cantilever type skin friction balance with active heating of the floating element. Once assembled, the gage is physically robust.

(2) Study effects of temperature mismatch on the skin friction and heat flux measurements

The effects of temperature mismatch between the gage surface and the surrounding wall on the measurements of skin friction and heat flux were documented. For this purpose, the surface temperature measurement technique using an infrared radiometer was also presented, introducing a low density polyethylene film to solve the window problem for the supersonic wind tunnel measurements. Using the infrared radiometer, it was possible to simultaneously measure the surface temperature distributions on the whole area of the gage and the surrounding wall. The amount of temperature mismatch generated by the gage itself without the active heating was from 2.5 °K to 4.5 °K. The active heating produced the temperature mismatch of 18.7 °K under the freestream total temperature of 300 °K. So, the largest temperature mismatch corresponds to the levels typically found in high heat flux cases when it is expressed in dimensionless terms as $\Delta T_w / T_w = 0.062$. This temperature mismatch produced on purpose by the active heating made sizable effects – a 24 % increase in the skin friction measurement ($C_f$) and
a 580 % increase in the heat flux measurement (St). The uncertainty range of the skin friction measurements was ± 4.5 % with the active heating, ± 7.2 % without the active heating. In the heat flux measurements, it was ± 8.6 % and ±13.0 % with and without the active heating, respectively.

(3) CFD Simulations

To simulate the present experimental measurements, the computational fluid dynamics code GASP was used. The input flow conditions were obtained from the boundary layer measurements at the upstream location. The temperature mismatch was input by specifying the pressure and the density at each grid point on the wall. The Baldwin-Lomax algebraic turbulence model was used with the thin layer approximations. When the temperature mismatch was zero, i.e. the surface temperature on the gage and that on the surrounding wall were the same, the difference between measurements and calculations was only 4 % in the skin friction measurements and 3 % in the heat flux measurements. In the range of the temperature mismatch generated by the gage itself without the active heating, the computational results and the experimental results were close to each other such that the difference was less than 10 % of the experimental results. Therefore, GASP gave good predictions for the two-dimensional cases. However, the results of the three-dimensional calculations were much different from those of the experimental measurements. For three-dimensional calculations, the surface temperature distributions on the gage and the surrounding wall measured with the active heating were used. GASP predicted the normalized skin friction coefficient and the heat transfer
coefficient as 23 % and 84 %, respectively, less than measurements. The active heating produces a new thermal boundary layer on the gage, and there are some variations in viscous and turbulent characteristics of the flow when the existing supersonic boundary layer flow interacts with the new thermal boundary layer. If a turbulent model does not have a capability to take those variations into account, it can not predict the mechanical and thermal phenomena correctly. In particular, the length scale predicted by a Baldwin - Lomax type model cannot account for both the existing and the new boundary layers. In such models, the length scale is computed from the velocity profiles, and here the thermal boundary layer will have two length scales – one for the existing boundary and one for the new boundary layer. Therefore, it is presumed that the disagreement between the experimental results and computational results was caused mainly by deficiencies in the turbulence model for this complex, developing, viscous turbulent flow.

9.2 Future Work

In the present study, the performance and the characteristics of the new gage were illustrated. Here, some future works are suggested for further improvements and more advanced techniques.

First, suggested future works are focused on the design of the new gage. A smaller size of the heat flux microsensor can be considered. A smaller heat flux microsensor leads to a smaller size of the floating element of the skin friction balance so that it will contribute to better local measurements. For the current stage of the heat flux
microsensor, for example, the length of the sensor leg can be reduced. Also, the feed-through pins require more stable settlements. Even though the silver epoxy was used to hold the feed-through pins in this study, it might be a sufficient remedy for the extremely sensitive resistance temperature sensor. A thicker aluminum nitride slice can be used as the substrate to provide the feed-through pins with firmer settlements. In this case of a thicker substrate, however, the lip size of the floating element also has to be considered, because lip forces caused by the pressure gradient around the floating element may produce significant errors. Beveling the lip at a certain degree of angle should be used.

The second group of future works are concerned with the measurements. In the present study, the amount of the temperature mismatch was less than 5 °K and greater than 18 °K. Therefore, some measurements are suggested for the intermediate range of the temperature mismatch. Also, measurements in flows of different Mach numbers are recommended.

Finally, a turbulence model which has better capabilities is desired to predict the effects of temperature mismatch more precisely when the amount of temperature mismatch is greater than 10 °K, i.e. \( \Delta T_w / T_{\infty} > 0.333 \).
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APPENDIX: ERROR ANALYSIS

Error is an inherently existing problem in any real-world measurements. There are also many kinds of sources of error, and sometimes it is very difficult to clearly distinguish the error sources. In the present study, to eliminate errors due to amplifiers, filters, and cables, calibrations were conducted using the same instrumentation and cabling that would be used for the corresponding measurements.

As the measure of the error, the $L_2$ norm (Burden and Faires, 1989) was used. The $L_2$ norm is defined as

$$e_x = \left[ \sum_{i=1}^{n} e_i^2 \right]^{1/2}$$

where $i$ denotes the various errors associated with the measurements of $x$.

For the boundary layer measurements, the standard deviations in the calibrations of the conventional probes would contribute to errors. From the results of the calibrations of the settling chamber total pressure probe, $e_{p_{10}} = 0.0041$ atm. Three sources of error could be considered to the Pitot probe and the cone-static probe. The calibration standard deviations were 0.00063 atm for the Pitot probe and 0.0034 atm for the cone-static probe. According to Rodney (1992), the errors due to the turbulence would be about 0.0068 atm for both of these probe types. He also estimated the errors associated with the vertical
location from the wall including the spatial Pitot and cone-static gradient as 0.034 atm for the Pitot probe and 0.00068 atm for the cone-static probe. Therefore, $e_{P_2} = 0.035$ atm and $e_{P_c} = 0.0076$ atm. The error of the total temperature probe was estimated as $e_{T_2} = 1 \, ^\circ$K.

Since the skin friction measurements were performed without and with the active heating, the skin friction balance calibrations were also conducted for both cases, even though the strain gages were made to have the self-temperature-compensation capability. The standard deviation was 0.067 grams without heating and 0.155 grams with heating. Converting these into $C_fP_{lo}$ using the freestream flow properties gave $1.55 \times 10^{-7}$ and $3.71 \times 10^{-7}$, respectively. Also, from the results of the skin friction measurements, the error bound was $1.56 \times 10^{-6}$ without heating and $1.18 \times 10^{-6}$ with heating. Then $e_{C_fP_{lo}} = 1.57 \times 10^{-6}$ without heating and $1.24 \times 10^{-6}$ with heating.

Heat flux measurements showed the error bound of the heat transfer coefficient as $1.62 \times 10^{-4}$ without heating and $5.92 \times 10^{-4}$ with heating. Therefore, $e_{St} = 1.62 \times 10^{-4}$ and $5.92 \times 10^{-4}$, respectively.

Table A.1 summarizes the results of this error analysis. Each percent error was obtained using the corresponding freestream total pressure, total temperature, mean value of the normalized skin friction coefficient, and mean value of the heat transfer coefficient.
Table 7.1  Measurement without the Active Heating

<table>
<thead>
<tr>
<th>RUN</th>
<th>$\tau_w$ (g/cm$^2$)</th>
<th>$C_f$</th>
<th>$\dot{q}$ (W/cm$^3$)</th>
<th>$St$</th>
</tr>
</thead>
<tbody>
<tr>
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<td>22.262</td>
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<td>1.418</td>
<td>0.00114</td>
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<td>2</td>
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<td>0.00155</td>
<td>1.584</td>
<td>0.00128</td>
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<td>22.742</td>
<td>0.00160</td>
<td>1.546</td>
<td>0.00125</td>
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<tr>
<td>4</td>
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<td>0.00157</td>
<td>1.369</td>
<td>0.00110</td>
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<td>1.747</td>
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<td>1.506</td>
<td>0.00121</td>
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</table>
Table 7.2  Measurement with the Active Heating

<table>
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<th>$\tau_w$ (g/cm²)</th>
<th>$C_f$</th>
<th>$\dot{q}$ (W/cm²)</th>
<th>St</th>
</tr>
</thead>
<tbody>
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<td>4.316</td>
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<tr>
<td>2</td>
<td>32.039</td>
<td>0.00207</td>
<td>4.816</td>
<td>0.00736</td>
</tr>
<tr>
<td>3</td>
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<td>0.00205</td>
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<tr>
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<tr>
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<td>4.666</td>
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<tr>
<td>9</td>
<td>30.273</td>
<td>0.00193</td>
<td>4.153</td>
<td>0.00634</td>
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</table>
Table A.1 Summary of Error Analysis

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<thead>
<tr>
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<th>Value</th>
<th>Error %</th>
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</thead>
<tbody>
<tr>
<td>$e_{Pc0}$</td>
<td>0.0041 atm</td>
<td>0.08 %</td>
</tr>
<tr>
<td>$e_{Pb}$</td>
<td>0.035 atm</td>
<td>0.72 %</td>
</tr>
<tr>
<td>$e_{Pc}$</td>
<td>0.0076 atm</td>
<td>0.16 %</td>
</tr>
<tr>
<td>$e_{Te0}$</td>
<td>1 °K</td>
<td>0.32 %</td>
</tr>
<tr>
<td>$e_{CFPb0} (OFF)$</td>
<td>$1.57 \times 10^4$</td>
<td>7.23 %</td>
</tr>
<tr>
<td>$e_{CFPb0} (ON)$</td>
<td>$1.24 \times 10^5$</td>
<td>4.70 %</td>
</tr>
<tr>
<td>$e_{St} (OFF)$</td>
<td>$1.62 \times 10^4$</td>
<td>13.0 %</td>
</tr>
<tr>
<td>$e_{St} (ON)$</td>
<td>$5.92 \times 10^4$</td>
<td>8.58 %</td>
</tr>
</tbody>
</table>
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- 3 mm Thick Acrylic Sheet
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(a) Active Heating OFF

TIME (sec)

Output Signal (mV)

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Heat Transfer Coefficient, ST
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Figure 8.3  Continued
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Figure 8.4  Continued
Figure 8.4  Continued
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Temperature Mismatch (deg.K)

Increase in St...
VITA

The author was born on March 11, 1958 in Korea. He received his B.S. degree in Aeronautical Engineering from Air Force Academy, Seoul, Korea, in March 1981. After graduation, he served as a military trainer and instructor until 1984. Then he continued his study and received his M.S. degree in Aeronautical Engineering from Naval Postgraduate School, Monterey, California, in December 1986. He served again as a full-time instructor at Korea Air Force Academy until he joined the Department of Aerospace and Ocean Engineering, VPI & SU, in August 1990.

[Signature]

Pach-Sung Lee