5.0 Results

This chapter discusses the results of utilizing the numerical end-to-end model to accomplish the goals outlined in Section 1.6:

1. To complete an end-to-end model of the Clouds and the Earth's Radiant Energy System scanning thermistor bolometer radiometers. This would be the culmination of a multi-year effort by various members of the Thermal Radiation Group at Virginia Tech.
2. To validate the model against actual data from the testing and characterization of the CERES flight instruments.
3. To analyze anomalous flight instrument behavior and provide a rigorous explanation of its cause.
4. To conduct a series of studies to enhance the understanding and data retrieval capability of the as-built CERES instruments.

Upon completion of the first goal, the end-to-end model was validated by comparing its predicted performance to that of the CERES PFM total channel sensor. The model's performance was brought into agreement with the PFM total channel through a procedure where the physical specifications of the model were varied within an acceptable tolerance range until the predicted performance matched that of the actual flight sensor. This procedure is discussed in detail in Sections 5.1 through 5.3.

Once the model's performance was brought into agreement with the actual flight sensor, a study was conducted to quantify and classify the presence of an anomalous slow response mode in the flight sensor data. In addition, the end-to-end model was used to verify the origin of the slow mode and to assess the effectiveness of numerical filtering algorithms designed to restore the performance of the flight instrument. These discussions appear in Sections 5.2 and 5.4.

Finally, the model was used to enhance the understanding of several aspects of the flight instrument's behavior in the hope that the enhanced understanding will improve the data retrieval capability of the as-built flight sensors. In conducting this study the model was used to predict the experimentally measured optical and instrument point spread functions (Sections 5.5 and 5.6), as well as to quantify the effects which degradation of the detectors may have on data retrieval capability (Section 5.8).

5.1 Detector Module Assembly Responsivity and Time Constant Validation - Flight Hardware and Model

Nominal dimensional specifications and thermophysical property values for the detector module assemblies in the CERES Proto-Flight Model (PFM) are shown in Table 5. Current manufacturing processes do not allow for the consistent fabrication of bolometer detectors with identical physical dimensions. Although the uncontrolled variations in physical dimensions are quite small during the manufacturing process, they are sufficiently large to introduce appreciable differences in the thermal and electrical characteristics of the completed bolometers. Use of the nominal dimensions is adequate for general studies of the CERES instrument's characteristics. In order to simulate and predict the behavior of actual as-built assemblies, it is necessary to develop a procedure which allows the numerical model to more closely resemble the characteristics of the actual bolometers.

The first step in implementing this procedure was to conduct a numerical sensitivity study which revealed the effects of varying the thickness of each layer in the bolometers individually while holding all other layers at their nominal dimensions. The electrical time constant as well as responsivity were calculated for each geometry in order to quantify the sensitivity of these parameters to thickness variations. Responsivity is a measure of a detector's ability to sense a unit amount of radiant energy and is defined as the ratio of the detector response to the amount of energy arriving at the detector surface. In using this definition one should note that the spectral flatness of the blackening agent of the detector is included in the value of the measured responsivity; thus the responsivity needs to be associated with a wavelength interval. For this study, time constant and responsivity were based on V_{out} and V_2-V_1 in the pre-amplifier electronics, respectively, as labeled in Figure 3.9. Results may be seen in Table 6, which presents the predicted performance sensitivities of the detector module assembly to changes in detector layer dimensions. It is important to note that the sensitivities are essentially linear over realistic ranges of layer thickness, and independent of wavelength interval.

The reader should take note that the concept of time constant is based upon the premise that the detector module assembly responds in a first-order fashion to a step input applied at time t=0. Algebraically this may be stated as

$$R_{transient} = \left[1 - \frac{1}{e^{t/\tau}}\right] R_{steady\ state} \quad , \tag{5.1}$$

where e is the exponential operator and t is time. The time constant, τ, of a first-order system is defined as the period it takes for the response to reach 63.2 percent of its final value.

It is assumed that the CERES detectors respond in a first-order fashion for the purposes of presenting values for the time constant. The correctness of this assumption is discussed in Section 5.2. The numerically determined time constant and responsivity for a thermistor bolometer detector module assembly having the specified PFM detector module assembly dimensions seen in Table 5 are 8.64 ms and 65.2 VW^{-1}, respectively. These values were numerically determined by allowing a step increase in monochromatic energy to uniformly illuminate the absorber layer (ε = 0.9), and then allowing the instrument to reach a steady-state condition. This methodology allows for the influence of self-heating in both detectors to be taken into account.

The time constant and responsivity for the Proto-Flight Model total channel detector module assembly have been measured by TRW and determined to be 9.2 ms and 62.2 VW^{-1}, respectively. The value of 62.2 VW^{-1} is a broadband averaged value based on the measured responsivities at 0.514 and 10.6 μm, which differed by 1.4 VW^{-1}. In order to realize these values in the numerical model, the dimensions of the various layers of the active and reference bolometers were adjusted accordingly using knowledge of the actual fabrication practices. For instance, the layers of epoxy between the aluminum substrate and Kapton® are modeled as slightly thinner than the layers between the Kapton® and thermistors. This is because in the manufacturing process the Kapton® is pressed into the lower epoxy layer to help achieve a good bond and to remove any visible air-bubbles entrapped between the epoxy and Kapton®. During fabrication, the thermistor layer, which is a ceramic material, may not be pressed as forcefully into the layer of epoxy below it because the ceramic could easily fracture. It should be noted that the final values are all within an accepted range of fabrication tolerances. Final values for the layer thicknesses are given in Table 5.

Precise values of time constant and responsivity were not available for the individual active and compensating detector detectors, but only as a built-up detector module assembly. Because of this lack of information, the modeled dimensions of the active and compensating detectors are identical. Section 5.9 reports the results of a study which involved modifying the dimensions of the detectors such that they are mismatched in order to assess influences of mismatched detectors on response characteristics.

5.2 Detector Module Assembly Time Response Function Validation - Flight Hardware and Model

In Section 5.1 it was assumed that the CERES detectors responded in a first-order fashion. Upon examination of data from the characterization of the CERES flight sensors, it was evident that the shape of the response did not conform to true first-order behavior. The actual data show that the detectors contain a slow mode of response which affects approximately the last two percent of their transient response mode. This may be seen in Figure 5.1 which displays the theoretical normalized response curve for a first-order system with a time constant of 9.2 ms along with the actual CERES instrument data normalized to its final steady state value. Also displayed is the response of the baseline numerical model which uses the final detector dimensions as determined by the process described in Section 5.1. From this figure it is apparent that the actual flight sensors make the transition to a slow mode at about 98 percent of the final response. This transition results in a lengthening of the time it takes for the sensors to reach steady state by about one second. It is also apparent from Figure 5.1 that the baseline numerical model does not correctly account for this slow mode. In fact, the baseline numerical model curve falls somewhere between the theoretical prediction and the actual flight sensors. From this point forward this baseline model will be referred to as the ideal model since it does not contain the undesirable slow mode in its response.

It was hypothesized that this slow mode could be attributed to the presence of a thermal resistance associated with the interface between the active and compensating detector disks. In their original design the CERES sensors were to have a single common heatsink, or substrate, to which both the active and compensating detectors would be mounted. During the fabrication process it was found to be very difficult to consistently manufacture detectors which met the defined performance specifications (i.e. time constant and responsivity). The procedure for creating the active and compensating detectors requires that they be “built up” and permanently attached to their individual substrate, or heatsink. In the original design this meant that both the active and compensating detectors were fabricated on opposite sides of the same aluminum disk. This process prohibited the creation of a series of detectors from which matched pairs could be selected and bonded to a common heatsink. As a result, the manufacturing yield of detector module assemblies which met performance requirements was not acceptable.

In order to boost yield, it was decided to redesign the detector module assembly such that a series of detectors would be fabricated on aluminum substrates which were exactly one-half the thickness of the original common heatsink. Upon completion of the fabrication process, matched pairs could be selected and the two detector disks could then be bolted together. This design creates an inherent problem, however; the two detectors are now separated by a larger thermal impedance. The interface between the two aluminum substrates could now realistically result in differential heating of the two disks. In order to alleviate this potential problem, 50 μm of pure indium was sputtered to the back surfaces of both the active and compensating disks. The idea behind this procedure is that when the disks are bolted together the indium will compress and cold-weld such that a more intimate thermal contact is achieved.

The principle behind having the active and compensating detectors is that they will remove any common-mode thermal noise from the detector module assembly output. By eliminating extraneous thermal noise the output from the detector module assembly may then be assumed due only to scene energy illuminating the active detector element. It has already been established that the thermal design of the sensors dictates that the common heatsink be maintained at a uniform nearly constant temperature elevated from its surroundings such that any absorbed energy from the scene will be rapidly diffused to the surrounding structure. If a thermal impedance exists between the two aluminum substrates, differential heating between the two disks may occur. If the active disk were to rise in temperature only a few micro-Kelvin above the compensating disk, the electronics would interpret this as more scene energy arriving at the active detector, or an increase in the measured response. The result would be a false signal superimposed on the output which is correlated to the incoming energy, but whose time response and magnitude are an order of magnitude lower and slower than the signal produced from the scene energy.

The ideal numerical model already described assumes that the indium creates intimate thermal contact between the two aluminum substrates and that no interface resistance exists at this location. In order to attempt to bring the model into better agreement with the flight sensors, a study was conducted to assess the influence of increasing the thermal impedance of this interface. Typically, an interface resistance creates a discontinuity in the temperature profile at the location of the junction between two adjacent materials, as illustrated in Figure 5.2(a). This discontinuity is often explained in terms of voids at the interface boundary. In performing the study, the interface resistance was modeled by assigning an effective thermal conductivity to the indium layer. This procedure preserves the assumption of intimate thermal contact at the interface boundaries while accurately modeling the temperature differential across the interface. An example of the resulting temperature profile is shown conceptually in Figure 5.2 (b). Although this technique does not allow for the correct temperature prediction in the indium layer, globally it preserves the flux diffusion rate through the interface which is required of a successful model.

Figure 5.3 displays the normalized time response functions predicted by the model with different values of effective thermal conductivity for the indium layer. The accepted value of thermal conductivity, k, for indium is 80.84 $Wm^{-1}K^{-1}$. Note that as the effective value of thermal conductivity drops, the “knee”, or fold-over point, in the data moves downward and the time required to reach steady state increases. Since this knee does not manifest itself until about the 98 percent level, it has little effect on the time constant, defined as the time when 63.2 percent of the steady-state value has been reached. For the behavior displayed in Figure 5.3 the spread in time constant is only about 0.15 ms with the value varying inversely with the magnitude of the effective thermal conductivity of the indium layer. Figure 5.4 displays a comparison between the Proto-Flight Model total channel sensor normalized time response function and the “best-fit” curve from Figure 5.3. The “best-fit” label was assigned based upon visual examination of the data. The two normalized response curves agree at any given point to better than 0.3 percent. The value of effective thermal conductivity which produces this fit is 0.03 $Wm^{-1}K^{-1}$.

The flight sensor data shown in Figure 5.4 were obtained from averaging a series of scans where the sensors were rotated from a cold space reference onto the internal calibration sources at 254 deg/s. The reason for averaging a large number of scans is to beat down the random noise, which is on the order of less that one percent. During the scan from the cold space reference to the internal calibration source, the sensor briefly sees the inside of the scan head, which contains structure at approximately 38 C. Although this scan profile effectively serves as a data source for the purposes of analyzing the slow mode, it is not appropriate for analyzing the full-scale response of the instrument to a step input. The best source of data for a full-scale comparison is during the shortwave radiometric calibration of the total channel. In this procedure, a rotating chopping wheel is located between the sensor and the radiometric source. This experimental design provides an ideal source for step input data collection. Unfortunately, this procedure was not designed to specifically collect step input data, and only a handful of data sets were collected.

Since a statistically insignificant number of data were collected to beat down the noise, only a qualitative comparison of these data to the model can be made. The results of this comparison are given in Figure 5.5, which demonstrates that the model does an adequate job of predicting the full-scale response of the PFM total sensor to a step input.

5.3 Radiometric Calibration

The purpose of the radiometric calibration is to develop a correlation between the output from the radiometric channels and the known filtered radiances which illuminate them. The equation used to accomplish this correlation, first presented in Section 3.6 is

$$\begin{aligned}\tilde{L}(t-\tau) = A_V[m(t) - \overline{m}(t_k) - o(t)] + \frac{t-t_k}{\Delta t}\{A_S[\overline{m}(t_{k+1}) - m(t_k)] \\ + A_H[T_H(t_{k+1}) - T_H(t_k)] + A_D[V_D(t_{k+1}) - V_D(t_k)] \\ + A_B[V_{bias}(t_{k+1}) - V_{bias}(t_k)]\} \quad ,\end{aligned} \tag{5.2}$$

where

$$t_{k+1} = t_k + \Delta t \quad .$$

The various symbols in Eq. 5.2 are described under Eq. 3.3.

The ground calibration procedures are designed to define the gain and offset terms which appear in Eq. 5.2. The basic calibration methodology for a given instrument consists of (1) defining the filtered radiance reaching the instrument active bolometer detector, (2) correcting for variations in the sensor zero-radiance offset with elevation angle (please see discussion below), (3) measuring alternately the radiances from a reference calibration source and a cold space reference, and (4) performing least-squares analyses of the filtered calibration radiances and the independent variables in Eq. 5.2.

The procedure for numerically duplicating the ground calibration procedures is identical to that listed above with the exception of Step 2. The instrument sensitivity to variations in zero-radiance offset as a function of elevation angle is not currently part of the numerical modeling effort. As a result, the numerical model is insensitive to scan elevation angle. The decision to not model the zero-radiance offsets was made because the offsets are primarily due to two phenomena, electronic noise and physical strain, that are not readily modeled. Electronic noise is picked up by the sensor electronics assembly as it moves through various electro-magnetic fields during an elevation scan. The strength and location of these fields vary greatly from instrument to instrument since they are sensitive to the exact routing of electrical lines in the elevation head during fabrication. This phenomenon is not completely understood physically and any attempt to model it would be guess-work and not appropriate for a first-principle model. The second phenomenon is physical strain. In addition to being excellent thermal detectors, thermistor bolometers are ideal micro-strain gauges as well. As the elevation assembly rotation rate changes, the torque applied to the assembly changes as well. The time varying torque may produce small deflections in the sensors and thus cause the detectors to produce a false signal. The modeling effort required to predict these strains is outside the scope of the current effort.

5.3.1 Spectral characterization implementation

The end-to-end spectral characterization of the CERES flight sensors was calculated by using a Fourier Transform Spectrometer (FTS) as a monochromatic source which varied from 0.2 to greater than 200 μm. Simultaneously, the CERES sensors and a spectrally flat, wedge shaped, lithium tantalate reference detector were illuminated by the FTS source. Forming the ratio of the steady-state response of the CERES sensors to the spectrally flat reference detector provided the relative shape of the spectral response of the flight sensors. This curve was then anchored to the absolutely determined spectral response in the 0.3- to 2.0-μm region. The results of this effort were presented earlier in Figure 3.6.

In order to determine appropriate values for the independent variables in Eq. 5.2, the filtered radiances must be known. In the end-to-end numerical model, the spectral, spatial and temporal distribution of incident radiance is nominally determined by the Monte-Carlo-based optical ray trace. In order to incorporate radiance fields, which are neither spatially uniform in magnitude nor spectral content, distribution factors were calculated for a series of discreet solid angles which completely cover the instrument field-of-view. In essence, a full-field source has been approximated by discretizing the field-of-view into a statistically significant number of equal solid angles based on the field-of-view scan and cross-scan angles. A complete discussion of this procedure may be found in Section 5.6.

A limit to the applicability of current-generation ray-trace modeling efforts is the lack of detailed knowledge of wavelength-dependent thermal and radiative properties of media present in the physical domain of the ray-trace. In the current effort, the wavelength-dependent absorptivity, emissivity and specularity of the mirror, detector, and instrument surfaces are not known across the entire spectrum of interest. Absolute reflectance measurement data are available for the silvered primary and secondary mirrors over the spectral band of 0.3 to 2 μm. These data were used to anchor the spectral response curve as discussed previously. As a result of only having limited absolute data, it was deemed ineffective to calculate a three-dimensional array of monochromatic distribution factors for the radiative exchange between each discrete solid angle of the aperture and gridded surface of the active detector. Computing this large array would not only be prohibitive due to computational effort, but it would require the use of hypothetical wavelength-dependent surface properties and would thus be inappropriate. Instead, a baseline array of monochromatic distribution factors was calculated for each combination of discretized solid angle and gridded active detector surface. These calculations were carried out using spectrally flat ideal surface property data for all optical and instrument surfaces. This essentially assigns a value of unity to the normalized spectral response. This process provides the baseline spatial distribution and peak magnitude of energy arriving at the active detector for a given radiance at the instrument aperture independent of wavelength. Spectrally dependent distribution factors, D_{ijk}, are computed by multiplying the baseline distribution factor array, D_{ij}, by the normalized spectral response, $S(\Delta\lambda_k)$, seen in Fig. 3.6, at each measured wavelength. This effort results in a three-dimensional array of monochromatic distribution factors, D_{ijk}, where i represents the discrete field-of-view solid angle, j represents the gridded active detector surface, and k the wavelength interval of interest. In the current model, the field-of-view is discretized into 1155 equal solid angles, the detector into 256 equal surfaces, and k into 470 wavelength intervals.

The spectral response of the PFM total channel is relatively flat over broad spectral regions. In particular, it is flat between 5 and 50 μm, where the vast majority of energy emitted by blackbody surfaces, whose temperatures are representative of Earth scenes, resides. This flatness allows accurate spectrally weighted response function to be calculated over broad wavelength intervals. Figure 5.6 displays the relationship between the unfiltered longwave radiances emitted by absolute blackbody sources during the ground calibration of the PFM total channel and the filtered radiances absorbed the active detector. The filtered radiances are determined by convolving the normalized spectral response, Fig. 3.6, with the unfiltered radiances emitted by blackbody sources. This convolution may be represented symbolically as

$$\tilde{L} = \int_0^\infty S(\lambda) L_\lambda(\lambda) d\lambda \quad , \tag{5.3}$$

where $\tilde{L}$ is the broadband filtered radiance, $S(\lambda)$ is the normalized spectral response, and $L_\lambda(\lambda)$ is the spectrally dependent unfiltered radiance of the blackbody source. It is clear from Figure 5.6 that a strong linear relationship exists between the unfiltered and filtered radiances for the longwave portion of the PFM total channel. This relationship may be stated as

$$\tilde{L} = 0.8738L - 0.4923 \quad , \tag{5.4}$$

where $\tilde{L}$ is the broadband filtered radiance ($Wm^{-2}sr^{-1}$) and L is the broadband unfiltered radiance ($Wm^{-2}sr^{-1}$).

By taking advantage of the flatness over specific wavelength regions, the number of wavelength intervals, k, for which distribution factor matrices must be calculated may be drastically reduced. In the case of the PFM total channel, only a single array of distribution factors are needed to adequately describe the radiative transfer between the aperture and active detector for typical longwave Earth scenes; that is, k=1.

5.3.2 Calculation of instrument gains

Equation 5.2 is the algorithm which will be used to relate flight data to filtered radiances. In the actual ground calibration conducted by TRW, data were collected to accurately determine A_V and A_S using a regression technique. The heatsink temperature, T_H, and bias voltage, V_{bias}, which are multiplied by the A_H and A_B terms, respectively, in Eq. 5.2, are expected to remain constant over the life of the mission. Based upon the experience of the Earth Radiation Budget Experiment (ERBE), which used a similar instrument and electronics, this is an attainable goal. It was not deemed cost effective to extend the CERES ground calibrations for the purposes of measuring the influence that heatsink temperature and bias voltage variations have on the instrument performance, and thus they were not varied. Instead, the A_H and A_B terms, which account for this influence, will be determined on orbit using onboard calibration sources if the need arises.

In the numerical modeling effort reported here, A_V, A_S, A_H, and A_B are calculated. The A_D term was not included simply because the range over which the voltage from the digital-to-analog converter will be varied over the life of the mission is not yet known. The digital-to-analog converter voltage level is included in the numerical model; however, over the course of the numerical simulation the value of V_D was purposely not varied, and thus a value for A_D was not obtained.

5.3.2.1 determination of A_V

During the CERES PFM ground calibration the heatsink temperature, T_H, bias voltage, V_{bias}, and digital-to-analog voltage, V_D, were not varied. The result of not varying these parameters is a reduction of Eq. 5.2 to a final form of

$$\tilde{L}(t-\tau) = A_V[m(t) - \overline{m}(t_k) - o(t)]. \tag{5.5}$$

The Narrow Field Blackbody (NFBB) in the TRW Radiometric Calibration Facility (RCF) was varied over the temperature range of 206 to 312 K during the longwave calibration of the total channel. This resulted in a range of unfiltered radiances of approximately 39 to 171 $Wm^{-2}sr^{-1}$ and a filtered radiance range of 28.3 to 149.2 $Wm^{-2}sr^{-1}$, as indicated in Figure 5.6. The specific temperature levels, and the filtered and unfiltered radiances are reported in Table 7. It should be noted that by filtered output it is meant the bracketed terms in Eq. 5.5. Figure 5.7 presents the results of performing a least-squares linear regression to the filtered instrument output and filtered radiances listed in Table 7. The regression produces a slope of 6.623 counts/$Wm^{-2}sr^{-1}$, with a correlation coefficient R2 of 1.0. The experimental setup dictates that this value must be increased by 0.738 percent due to experimentally determined out-of-field contamination. By adding this contribution, a final gain value of 6.672 counts/$Wm^{-2}sr^{-1}$ may be arrived at for the PFM Total channel. From inspection of Eq. 5.2, A_V is obtained by taking the inverse of this gain, resulting in a value of 0.1499 $Wm^{-2}sr^{-1}$/count [64].

In order to predict a value of A_V using the numerical model, it is first necessary to determine the relationship between incident unfiltered radiance and the amount of power which arrives at the active detector. This relationship is determined by integrating the baseline distribution factor matrix over the field-of-view. Distribution factors are typically used to relate the amount of energy emitted and absorbed by surfaces. In the current effort the distribution factors represent radiative transfer within a discrete solid angle. By specifying the amount of energy in a known solid angle, and knowing the area of the instrument aperture, a value of monochromatic unfiltered radiance ($Wm^{-2}sr^{-1}$) at the aperture may be determined. This may be represented algebraically as

$$L_{FOV} = P_{Detector} \frac{\frac{1}{M}\sum_{i=1}^{M} P_i}{\Omega A_{AP} \sum_{i=1}^{M}\sum_{j=1}^{N} D_{ij} P_i} \quad , \tag{5.6}$$

where L_{FOV} is the monochromatic unfiltered radiance at the aperture, $P_{Detector}$ is the power incident to the active detector in microwatts, P_i is the power in a discretized field-of-view solid angle, M is the integer number of discrete solid angles within the field-of-view, N is the number of discretized surfaces on the active detector, Ω is the size of a discretized field-of-view solid angle in steradians, and A_{AP} is the area of the aperture in meters squared. For a uniform calibration source, Eq. 5.6 reduces to

$$L_{FOV} = P_{Detector} \frac{1}{\Omega A_{AP} \sum_{i=1}^{M}\sum_{j=1}^{N} D_{ij}} \quad , \tag{5.7}$$

Substituting known values into Eq. 5.7 yields the relationship

$$L_{FOV} = 9.479\ P_{Detector} \quad , \tag{5.8}$$

where $P_{Detector}$ is the power in of microwatts incident to the active detector and L_{FOV} is the unfiltered monochromatic radiance at the instrument aperture ($Wm^{-2}sr^{-1}$). The instrument gain A_V may be approximated by

$$A_V^{-1} = R\,C\,G_{elect}\,(L\,\overline{S} - k) \quad , \tag{5.9}$$

where R is the responsivity of the numerical model as discussed in Section 5.1 (VW^{-1}), C is the analog-to-digital conversion factor of 409.5 counts per volt, G_{elect} is the electronic gain from the signal conditioning electronics of 2200.36 (-), L is the unfiltered radiance ($Wm^{-2}sr^{-1}$) defined by Eq. 5.7, $\overline{S}$ and k are the slope and offset terms in Eq. 5.4 and are assigned the values of 0.873 (-) and 0.4923 ($Wm^{-2}sr^{-1}$), respectively. Carrying out the mathematics in Eqs. 5.8 and 5.9 leads to a value of 6.832 counts/$Wm^{-2}sr^{-1}$ for the gain of the numerical model. Taking the inverse, a value of 0.1464 $Wm^{-2}sr^{-1}$/count is obtained for the A_V term. This value agrees with the A_V term obtained experimentally for the PFM total channel to within 2.33 percent. This close agreement validates the optical ray-trace and particularly its interface with the electro-thermal diffusion portion of the end-to-end instrument model.

5.3.2.2 determination of A_S

The A_S term is used to account for drifts in the instrument output between adjacent space looks. Inspection of Eq. 5.2 reveals that A_S is intended to correct for differences in the output counts during a 6.6-s elevation scan. Count differences are due to a time-varying thermal change in the detectors which is not due to the scene energy. This energy may be due to time-varying emission from the instrument structure which can enter the optical train and contaminate the signal coming from the scene. The physical phenomenon giving rise to this change in detector output while chopping against a constant source is identical to that of the A_V term. As a result the A_S term must be identical in magnitude to the A_V term.

5.3.2.3 determination of A_H

The purpose of A_H is to correct for changes in instrument gain due to variations in the temperature of the aluminum substrates over the period of 6.6 s between adjacent spacelooks.

The substrate temperature is measured directly in two locations on the detector module assembly: at the heater location and in the rear endcap. Variations in the substrate temperature effectively change the nominal thermal condition of the detectors, both active and compensating. If the nominal temperature of the aluminum substrate changes as a whole, the responsivity of the detector module assembly will change in a well understood fashion. This understanding is what allows common mode thermal noise to be removed

The impact of changing the temperature of the heatsink may be assessed in two ways. Equation 5.2 adequately accounts for changes which occur between two adjacent spacelooks. It does not account for changes that occur when the setpoint temperature of the heatsink is changed and then maintained at that new temperature for an extended period of time. In the former case A_H accounts for the drift between space looks; in the latter there is no drift, but a new value of A_V must be determined.

In order to numerically predict the value of A_H, Eq. 5.2 is first rearranged to isolate A_H; that is

$$A_H = \frac{\tilde{L}(t-\tau) - A_V\left[m(t) - \overline{m}(t_k)\right]}{T_H(t_{k+1}) - T_H(t_k)} \quad , \tag{5.10}$$

where $\tilde{L}$ is the known filtered radiance, A_V is the previously determined gain due to scene energy, m(t) is the instrument output in counts, $\overline{m}(t_k)$ is the instrument output averaged over the last spacelook in counts, $T_H(t_{k+1})$ is the heatsink temperature at the new spacelook, and $T_H(t_k)$ is the heatsink temperature at the previous spacelook. The numerical model is then operated in a mode where the temperature setpoint of the detector module assembly was changed from the nominal temperature of 38°C. The resulting values of heatsink temperature and output counts are substituted into Eq. 5.10 and a value of 1.6174 $Wm^{-2}sr^{-1}K^{-1}$ is determined for A_H. This value is constant for realistic variations in heatsink temperature.

In the second scenario the temperature setpoint is varied and a sensitivity of A_V to variations in setpoint temperature is calculated. By operating the end-to-end model at various detector module setpoint temperatures, new values of responsivity are calculated for a broad range of setpoint temperatures. The ratio of responsivity change to setpoint temperature change is inversely proportional to the change which would be seen in the value of A_V. This sensitivity was determined to be 0.352 percent per degree Kelvin. That is, for every degree Kelvin the setpoint temperature moves in one direction, the value of A_V should move in the opposite direction by 0.352 percent.

5.3.2.4 determination of A_B

The influence of varying the bias voltage, V_{bias}, on instrument output was determined in the same manner as was the A_H term. That is, A_B was isolated from Eq. 5.2 and the model was then run with different values of V_{bias}, and a value of A_B was calculated. The end-to-end model produces no calculable change in instrument output due to realistic bias voltage variations. This is consistent with the findings of the ERBE mission which also used thermistor bolometers in a bridge setting. Therefore a value of zero is assigned to both the A_B term and the sensitivity of A_V to variations in bias voltage.

5.3.2.5 determination of A_D

The A_D term is intended to account for any drift in the instrument response due to discrete variations in the digital-to-analog bridge balance voltage, V_D. The final design of the CERES electronics dictates that V_D be applied to the pre-amplifier electronics at node 11 in Figure 3.9. The voltage V_D is varied only among known discrete levels at known times in order to keep the instrument output on scale. The result is that a constant bias is introduced to the instrument output. The bias is then removed by chopping against cold space during the spacelooks on every 6.6-s scan. Since only a constant bias is introduced, the value of A_D becomes identically zero.

Values of the numerically predicted gain terms as well as their experimentally determined counterparts are given in Table 8.

5.4 Removal of Non-First-Order Response Characteristics Through Numerical Filtering

The analog signal produced by the CERES sensors is sampled every 10 ms. If frequency content of higher than 50 Hz, the Nyquist frequency, were present in the analog signal, aliasing errors would occur and the high-frequency analog signal would appear as a signal of lower frequency in the sampled output. The optical, thermal, and electrical design of the sensors ensure that this does not occur. Characterizing the instrument point spread function of the CERES sensors during ground calibration allows response modes on the order of 30 ms to be accurately characterized and accounted for. A complete discussion of the instrument point spread function is presented in Section 5.6. Operationally the sensors view cold space at approximately 3-s intervals. A space view provides a constant zero radiance source to monitor long-term drift in the instrument performance. If any drift is encountered between adjacent space looks it is accounted for and removed by linearly interpolating between the two zero-point space looks using Eq. 5.2.

The slow response mode of the sensors discussed in Section 5.2 has a time constant and magnitude of approximately 300 ms and 2 percent, respectively. This response time places it between the instrument point spread function and space look time intervals, and its relatively large magnitude provides a means by which the sensor output may be misinterpreted. This misinterpretation would be the failure to recognize that 2 percent of the signal at time t is really caused by the scene seen 300 ms previous to time t. Any negative effects introduced to the data product by this slow mode phenomenon may be attenuated by numerical filtering. The goal of such a filter would be to restore the time response of the instrument such that it would more closely follow the numerically derived response curve labeled ideal in Figure 5.8. This figure shows a comparison of response shapes for the ideal model and the “best-fit” model discussed in Section 5.2. The ideal version of the numerical model does not contain the slow response mode since the increased thermal impedance between the active and compensating disks due to the sputtered indium interface layer is not included. This was accomplished by modeling the interface as having intimate thermal contact and applying the nominal value of thermal conductivity, k, of 80.84 $Wm^{-1}K^{-1}$ for the indium layer.

Smith [65] has proposed that the slow mode be modeled as a superposition of first-order response modes. This idea can be stated mathematically as

$$w(t) = u(t) + v(t) \quad , \tag{5.11}$$

where w(t) is the measured response at time t, u(t) is the ideal first-order response, and v(t) is the superimposed slow mode response. It is desired to recover the ideal response, u(t), that the instrument was originally designed to provide based on alias and blur errors. Smith assumes that the slow mode, v(t), is driven by the ideal output u(t), and proposes a two-state numerical filter to recover u(t). This filter may be stated as [66]

$$v(t) = p_0 v(t-1) - p_1 w(t) \tag{5.12}$$

and

$$u(t) = w(t) - v(t) \quad , \tag{5.13}$$

where

$$p_0 = \exp[-\lambda \Delta t(1+\kappa)] \tag{5.14}$$

and

$$p_1 = \kappa[1 - p_0] \quad . \tag{5.15}$$

In Eqs. 5.14 through 5.15, λ is a characteristic time of the slow mode (s^{-1}), κ is the magnitude of the slow mode in percent, and Δt is the sampling period of 10 ms.

5.4.1 Numerical filter validation

In order to validate the effectiveness of the filtering algorithm proposed by Smith, values for λ and κ must be determined. The numerical model provides a simple means of obtaining these values. As stated in Section 5.2 the mechanism which creates this slow mode is hypothesized to be differential heating of the active and compensating aluminum substrates. The result of this differential heating is an artificial increase in the measured responsivity of the flight sensors. This rise in responsivity may be seen in Figure 5.9, which displays the response curves of the modeled detector module assembly for different values of the effective thermal conductivity of the indium interface. In previous figures the response curves were always normalized by their individual steady-state response values. In Figure 5.9 the curves are normalized to the steady-state response of the ideal model. This latter normalization methodology clearly displays the increase in responsivity associated with increasing the thermal impedance seen at the indium interface. In the current effort the ideal model corresponds to the curve with an effective thermal conductivity, k_{eff}, of 80.84 $Wm^{-1}K^{-1}$, the best fit model corresponds to the curve with an effective thermal conductivity, k_{eff}, of 0.03 $W/m^{-1}K^{-1}$. From Figure 5.9, the increase in response of the best-fit model (k_{eff}=0.03 $Wm^{-1}K^{-1}$) over the ideal model (k_{eff}=80.84 $Wm^{-1}K^{-1}$), and thus the value of κ in Eq. 5.14, is seen to be 0.726 percent.

In order to determine the magnitude of λ in Eq. 5.14, a curve was fit to the response shapes of the ideal and best-fit response functions seen in Figure 5.8. Smith assumed that the ideal response shape would be of the form

$$u(t) = 1 - e^{t/\tau} . \tag{5.16}$$

From inspection of Figure 5.1 it is apparent that the theoretical curve defined by Eq. 5.16 is of insufficient order to match the response function of the ideal model, which is labeled "baseline model" in this figure. In order to match the response of the ideal model, Eq. 5.16 had to be modified by adding a second term. The resulting equation is

$$u(t) = 1 - [C_1 e^{-t/\tau_1} + C_2 e^{-t/\tau_2}] \tag{5.17}$$

where C_1 and C_2 sum to unity, and τ_1 and τ_2 are characteristic time constants of the two modes. Similarly, a curve was fit to the best-fit normalized response, w(t), such that

$$w(t) = u(t) + v(t) \quad , \tag{5.18}$$

where u(t) represents the ideal model and is defined by Eq. 5.17, and v(t) represents the slow mode. This slow mode is modeled as

$$v(t) = C_3[1 - e^{-t/\tau_3}] \tag{5.19}$$

where C_3 and τ_3 are equivalent to κ and λ^{-1} in Eq. 5.13. Results of these curve fits may be seen in Figure 5.10, and corresponding values for the quantities C and τ may be found in Table 9.

In order to test the effectiveness of the filtering algorithm, values of p_o and p_1 were determined using values determined for κ and λ from C_3 and τ_3, in Table 9. Results of implementing the filtering algorithm, Eqs. 5.12 through 5.13, may be seen in Figure 5.11 for the case of a step input at t=0. This figure demonstrates that the ideal response shape, labeled u(t), may be recovered from the response produced by the best-fit model, w(t), for a step input at t=0. In Figure 5.11, w(t) is determined using the numerical model, v(t) using Eq. 5.12, and u(t) using Eq. 5.13. To rigorously test effectiveness of the algorithm, the model was used to scan a typical Earth scene which was created by Villeneuve's Atmospheric Radiation Model (ARM) [33]. A complete discussion of the interface between the end-to-end model and Villeneuve's ARM is given in Section 5.9. Results of this validation may be seen in Figures 5.12 and 5.13. Figure 5.12 displays the known radiative input from Villeneuve's model along with the radiance recovered by applying the filtering algorithm to the predicted radiance of the "best fit" version of the end-to-end model, and the predicted radiance obtained from the ideal version of the numerical model. The purpose of this figure is to demonstrate the algorithm's effectiveness by showing that there is little difference between the time varying radiances predicted by the ideal model, u(t), and the time varying radiances recovered by filtering the output of the "best-fit" model, w(t). Figure 5.13 displays the ability of the filtering algorithm to force the best-fit model of the as-built PFM total channel flight sensor response, w(t), to perform as well as the ideal detector for the time varying incident radiance shown in Figure 5.12. This is accomplished by plotting the percent deviation of A from B where A is either the predicted radiance of the numerically filtered, u(t), or unfiltered, w(t), versions of the end-to-end model and B is the radiance predicted by the ideal version of the end-to-end model.

A subtle but important point arose out of this validation study. The filtering algorithm reduces the magnitude of the steady-state and transient responses of the flight sensor to a given scene input by a percentage of κ. In other words, the responsivity, R (VW^{-1}) of the sensor has been reduced by the magnitude of κ due to the filtering algorithm. In Section 5.3.2.1 the gain term A_V was determined prior to the implementation of the filtering algorithm. As a result, the value of A_V determined prior to the implementation of the filtering algorithm must now be modified by the magnitude of κ as well. This is due to the fact that responsivity, R, is inversely related to A_V, as seen in Eq. 5.8.

5.5 Determination of the Optical Point Spread Function

The optical point spread function (OPSF) maps spatially heterogeneous scene-emitted radiances from the instrument aperture onto the active detecting element. The optical throughput of any instrument features attenuation at the edges of the field-of-view due to the properties of real optical systems. In CERES the use of spherical mirrors enhances this effect by creating a blur circle in the plane of the precision aperture. This blur circle is caused by the inability of spherical reflecting optics to focus their collected energy onto a point in the focal plane [34].

In order to predict the OPSF of the CERES instrument using the ray-trace module created by Bongiovi [30], it was first necessary to discretize the field-of-view into an array of discrete solid angles. Collimated distribution factors were then calculated for each discretized solid angle, as discussed in Section 5.3. The discretization process essentially replaces a contiguous full-field source with a discretized array of equivalent point sources.

The most efficient way of defining the discretized angular bins is with the sensor scan angle, η, and cross-scan angle, ξ. Definitions of these angles may be seen in Figure 3.7, which presents the physical dimensions of the CERES field stop, and in Figure 5.14. Scan angles η are measured in the plane defined by the sensor optical axis and the short axis of the field stop. Cross-scan angles ξ are measured in the plane defined by the sensor optical axis and the long axis of the field stop. In Figure 3.7 the sensor optical axis is perpendicular to the plane defined by the field stop. The ray-trace module requires that the location of the far-field point source be given in terms of zenith angle, θ, and azimuth angle, ϕ, as defined in Figure 5.14. The zenith angle θ is measured with respect to the sensor optical axis. The azimuth angle ϕ is measured in the plane defined by the field stop. The relationship between zenith angle, θ, azimuth angle, ϕ, scan angle, η, and cross-scan angle, ξ, is

$$\phi = \arctan\left(\frac{\tan(\eta)}{\tan(\xi)}\right) \tag{5.20}$$

and

$$\theta = \arctan\left(\frac{\tan(\eta)}{\sin(\phi)}\right) \quad . \tag{5.21}$$

The angular increments in the scan direction, $\Delta\eta$, are based upon the nominal elevation scan rate of 63.5 deg/s, and the size of the time steps in the numerical model, 0.001 s. This results in an angular increment, $\Delta\eta$, of 0.0635 deg in the scan direction. Angular increments in the cross-scan direction, $\Delta\xi$, are defined to be 0.1 deg. A value of 0.1 deg produces angular bins with an aspect ratio of about unity, while producing a nearly square matrix of bins. Since small angles have been used in the discretization process, the solid angle, Ω, defined by each bin may be approximated very accurately as

$$\Omega = \Delta\eta\Delta\xi \, . \tag{5.22}$$

Haeffelin [62] has demonstrated that this discretization is statistically sufficient for modeling contiguous full-field sources.

5.5.1 Significance of the blur circle

As stated earlier, the spherical mirrors in the CERES optical train create a blur circle in the plane of the field stop. The effect of having a circle, as opposed to a point, is that attenuation at the edges of the field is enhanced. A finite circle allows energy arriving from angles beyond the physical dimensions of the field stop to reach the detecting element because the angle of incident energy is measured to the center of the blur circle. For instance, if an instrument has a field stop with a physical cutoff of 1.3 deg with respect to the optical axis, and a blur circle of 0.2 deg radius, then the detector will be illuminated as the angle of the incident energy with respect to the optical axis becomes less than 1.5 deg, or the sum of the physical limitation and the radius of the blur circle. A feature of the blur circle is that it is neither radially nor azimuthally uniform in intensity. The circle is actually composed of concentric rings, with the central spot containing the majority of the energy. In addition, Bongiovi [30] demonstrated that the secondary mirror and its mount are imaged in the CERES blur circle. Figures 5.15 and 5.16 display the attenuation effects imposed by the blur circle on the CERES geometry.

Figure 5.15 displays a comparison of the attenuation measured for the PFM total channel flight sensor and the predicted attenuation from the ray-trace module as a function of angular distance beyond the edge of the field stop. The edge of the field stop corresponds to 0 deg in these figures so that attenuation in both the scan and cross-scan directions can be shown on the same graph. The experimental data were measured in the scan direction only; however, model data are presented for both directions. It is clear in this figure that attenuation occurs more rapidly along the cross-scan axis. This is because the blur circle is being obscured on two fronts due to the diamond shape of the field stop in this direction.

Figure 5.16 displays a comparison of the modeled attenuation with the attenuation predicted by linear optics associated with a uniform blur circle. This is done in an attempt to define an equivalent blur circle diameter. Results are presented only for the scan axis in this figure. From this figure it may be seen that the model predicts an equivalent blur circle of about 0.25-deg radius.

5.5.2 Optical Point Spread Function

The OPSF is created by assembling the matrix of collimated distribution factors calculated in Section 5.5 and normalizing it to the matrix element having the largest magnitude. The result of this procedure may be seen in Figure 5.17, which presents the predicted full-field normalized optical throughput for the PFM total channel. It is readily apparent in Figures 5.15 through 5.17 that significant attenuation occurs before the physical edge of the field stop is encountered and that significant energy is seen by the active detector beyond the physical edge of the field stop.

CERES data processing algorithms replace the actual spatial distribution of Earth-source radiance within an instantaneous footprint with an equivalent blackbody source. Not having sharp cut-off at the edge of the field-of-view leads to uncertainty in the definition of footprint size. Typically the 50-percent contour seen in Figure 5.17 is taken as the equivalent field-of-view and thus the footprint size. Section 5.9 presents the results of a study to determine the optimal TOA footprint for CERES such that errors due to the convolution of heterogeneous scenes and optical attenuation are minimized.

5.6 Determination of the Instrument Point Spread Function (PSF)

Accurate knowledge of the dynamic instrument point spread function (PSF) is essential in determining the geo-location of the instrument data time series as well as the relative weighting of the instantaneous scene over the optical field-of-view, or convolution. The dynamic instrument point spread function is defined as the dynamic response of a radiometric channel as it scans a far-field point source. When this source comes within the optical field-of-view the detector temperature begins to rise such that the output signal of the instrument rises above that of a nominal space look. When the source moves beyond the optical field-of-view the detector begins to cool off and thus the instrument output returns to its nominal value. By repeating this for all cross-scan angles, a surface may be built from combining the individual trace lines. The dynamic point spread function takes into account the shape of the precision field-stop, the time response of the detector, and the filtering and time response of the associated signal conditioning electronics [67].

The PSF of the CERES instruments is measured in the laboratory by TRW using their state-of-the-art Radiometric Calibration Facility [68]. In this procedure the flight sensors are scanned across a full-field collimated source of radiant energy. The collimating optics of the source combined with the optics of the CERES flight sensors produce a nonuniform circle of energy at the precision aperture of approximately 0.25-deg radius, as discussed in Section 5.5. The source is directed to the sensors via a movable mirror. This mirror allows the collimated source to be rotated for each data set collected such that the full field-of-view in the cross-scan direction may be adequately sampled. Figure 5.18 displays the trace lines across the precision aperture which are created by this technique. The dynamic response output data from the instrument as it scans across these trace lines is then combined such that a topographical plot is created. Figure 5.19 provides an example of a topographical representation of a point spread function for a generic scanning instrument. Sampling the full field-of-view in the scan direction is accomplished by the scanning motion of the flight sensors. Intrinsic difficulties in taking these experimental measurements suggests that a better understanding of the data could be obtained by the use of an independent instrument model [69].

5.6.1 Mapping of the PSF on cloud imager data

The CERES data processing algorithms replace the actual spatial distribution of emitted radiance within an instantaneous footprint with an equivalent blackbody source. This is done by converting the radiance measured by the instrument to a hemispherical radiance through the use of Bidirectional Reflectance Distribution Functions (BRDFs), as described in Section 2.8. The BRDFs are used to convert the radiance in a given angular bin coming from the scene into hemispherical radiances which may then be converted to a flux leaving the surface of the source scene. These BRDFs are a strong function of scene type, which in remote sensing means percent cloud cover, cloud type, cloud optical depth, cloud-top pressure, surface type (land or ocean), ground cover (desert, vegetation, ice, or snow), etc. The instrument point spread function covers a TOA patch sufficiently large to contain a good mixture of these parameters. This results in the question of how to best describe the footprint to use in the operational algorithms. Stated differently, how should the point spread function be weighted such that the nonuniform radiance fields of typical Earth scenes may best be approximated as a uniform blackbody.

Currently the CERES operational algorithms convert the three-dimensional instrument point spread function into a two-dimensional discretized array of tabular data. The columns of the array represent the discretized angular intervals in the cross-scan direction, $\Delta\xi$, while the rows represent the discretized angular intervals in the scan direction, $\Delta\eta$. The individual elements represent the integrated value of the normalized point spread function over the solid angle defined by $\Delta\xi\Delta\eta$ such that all of the elements sum to unity. Currently this array has been defined as a 16-by-16 array, as seen in Figure 5.20. This figure displays the rectangular grid superimposed upon a topographic projection of the 50-percent and 5-percent profiles of a CERES instrument point spread function.

The discretized point spread function in Figure 5.20 will be used to weight high-resolution cloud imager and other *a priori* knowledge of scene parameters in order to determine the most appropriate scene type. For the CERES PFM instrument cloud imager data will be available on approximately a 1-km resolution scale. This implies that several cloud imager measurements will be available for each of the 16-by-16 bins. By instituting this weighting method, a spatially heterogeneous scene will be weighted, based upon the response characteristics of the flight instruments, in order to obtain the best BRDF to utilize in the inversion process from radiance to TOA flux.

5.6.2 Centroid calculation

In order to assure accurate geo-location of instrument data time series, the centroid of the instrument point spread function must be known. Knowledge of the centroid location relative to the optical axis permits a time lag, τ, to be determined. The time lag, τ, is then implemented operationally in Eq. 5.2. The centroid, Ψ, is defined as the PSF-weighted average of the scan angles,

$$\Psi = \frac{\int_{\mathrm{fov}} \eta \; \mathrm{PSF}(\eta, \xi) \, d\eta \, d\xi}{\int_{\mathrm{fov}} \mathrm{PSF}(\eta, \xi) \, d\eta \, d\xi} \quad , \tag{5.23}$$

where η is the scan angle, ξ is the cross-scan angle, and PSF(η,ξ) is the normalized instrument point spread function. This provides Ψ in the units of angular degrees. It must be converted to the time domain by dividing by the value of the elevation scan rate; that is, $\tau \equiv \Psi / \omega$, where ω is the elevation scan rate in degrees per second.

5.6.3 Importance of scan rate

As discussed in Section 5.6, the PSF is a function of instrument scan rate. By scan rate it is meant both elevation and azimuthal rotation rate. The CERES instruments will operate at three discrete elevation scan rates during normal science-observing modes. These rates are 0, 63.5, and 254 deg/s. These rates will appear in the nadir, normal, and rapid retrace portion of the short scan profiles, respectively. The instruments will operate at two discrete azimuthal scan rates during normal operations. These rates are 0 and 6 deg/s. These rotation rates will be experienced in the normal cross-track, and bi-axial scanning modes, respectively.

5.6.3.1 nadir scan

In the nadir scan mode, the scan head is held motionless at the elevation angle of 90 deg, or the nadir point. The only temporal variation of scene radiances within the field-of-view is due to the satellite orbital motion. This motion, 6 km/s on the ground, is slow relative to the size of the field-of-view and instrument response rate. As a result, the dynamic instrument point spread function collapses to the instantaneous optical field of view, or to the Optical Point Spread Function shown in Figure 5.17. For this operational mode τ is assigned a value of 0.0 s since the PSF is symmetrical about the optical axis in the scan direction.

5.6.3.2 cross-track mode

In the cross-track operational mode the sensor assembly rotates in a plane perpendicular to the satellite ground track direction at a nominal rate of 63.5 deg/s. Temporal variation of the scene in this mode is due primarily to the motion of the sensors as they scan across the face of the Earth. Variation due to satellite motion is negligible as discussed above. Figure 5.21 displays the predicted dynamic point spread function for the nominal elevation scan rate. In this figure, scan and cross-scan angles of 0 deg correspond to the instrument optical axis. The centroid of the point spread function lags the optical axis considerably due to thermal diffusion and electronic delay. The calculated centroid for this operational mode is 1.55 deg. This value corresponds to a time lag, τ, of 24.4 ms.

The experimental data which TRW recorded were of a much lower resolution than that provided in the numerical approximation. Additionally the data were recorded on a nonuniform array of scan and cross-scan angles. By nonuniform it is meant that the scan and cross-scan angles were not sampled at regular intervals. Because of this it is not possible to obtain a quantitative comparison between the measured and predicted fully assembled point spread functions. A qualitative comparison is easily accomplished, however, by comparing individual trace lines. Figure 5.22 displays a comparison between the measured data and the predicted response curve for a trace taken along the 0-deg cross-scan angle. The fact that these traces agree well encourages us to assume that the completely assembled point spread functions would also agree.

5.6.3.3 bi-axial mode

In the bi-axial operational mode the sensor assembly scans across the Earth at one of two discrete rates. During normal operations the sensors rotate in the normal elevation scan mode, as shown in Figure 3.2. This produces a nominal Earth scan rate of 63.5 deg/s. During time periods when viewing the sun directly is a possibility the instrument will be commanded to rotate the sensors in the short scan profile, also shown in Figure 3.2. In this profile the sensors scan across the Earth in one direction at 63.5 deg/s then return at a rate of four times the nominal, or 254 deg/s. Superimposed on the elevation scanning is a rotation about the azimuthal axis at a constant rate of 6 deg/s. As a result the trace lines which the scene radiances follow as the sensors scan over them are no longer perpendicular to the long axis of the field stop, as shown in Figure 5.18. The trace lines now follow the vector addition of 63.5 or 254 deg/s in the scan direction and 6 deg/s in the cross-scan direction. That is, they pass through the field-of-view at either an angle of 0.0942 or 0.0236 deg with respect to the scan direction depending upon elevation scan rate. For the elevated scan rate this means that while in the field-of-view the source shifts approximately 0.03 deg relative to the scan direction. This shift is negligible. For the normal scan rate it means that while in the field-of-view the source shifts approximately 0.12 deg relative to the scan direction. This shift is too small to be accurately measured in the laboratory and is, in fact, on the order of the field-of-view discretization used in the end-to-end model. This shift will not introduce any appreciable error in the data reduction strategies.

The numerically predicted point spread function for the rapid retrace portion of the short scan mode is depicted in Figure 5.23. The calculated centroid for this operational mode is 6.02 deg. This value corresponds to a time lag, τ, of 23.7 ms, which is very similar to the time lag predicted for the nominal scan rate above.

The centroid for the rapid retrace is approximately four times that corresponding to the normal scan rate, 6.02 deg versus 1.55 deg, which is what one might anticipate when the scan rate is increased by a factor of four. This is in fact what has been assumed in the CERES operational algorithms. The centroid is a mathematical value obtained by integrating a variable of interest over a physical domain, as seen in Eq. 5.23. A consequence of the integration is that an infinite number of spatial distributions of the variable of interest over the physical domain may result in the same calculated value for the centroid. The implication is that the shape of the rapid retrace point spread function does not need to be a direct scaling of the nominal point spread function. Figure 5.24 illustrates this point by comparing trace lines from the predicted PSFs for scan rates of 63.5 and 254 deg/s. The two curves are the predicted trace line corresponding to a scan rate of 254 deg/s and the trace line corresponding to a scan rate of 63.5 deg/s. The trace line for the 63.5 deg/s scan rate has been linearly stretched in the angular direction such that its centroid lines up with that of the 254 deg/s. That is, both curves in Figure 5.24 have centroid values of 6.02 deg, where 0.0 deg corresponds to the sensor optical axis. Inspection of Figure 5.24 reveals that neither the shape nor the location of the peak value for the two curves agree, yet their calculated centroids are identical. The point of this result is that care must be taken when calculating values used in the angular bins for weighting cloud imager data. A study of the sensitivity of using the wrong values in these bins is given in Section 5.10.

Figure 5.25 displays a direct comparison of the numerically predicted point spread functions for the (a) normal scan and (b) rapid retrace portion of the short scan. This figure clearly shows the lengthening, in angular dimension, of the point spread function as the elevation scan rate increases.

5.7 Determination of the CERES Instrument Transfer Function

The four-pole Bessel filter integrated in the CERES Sensor Electronics Assembly is a low-pass filter designed to attenuate any signal with a frequency above 22 Hz. A Bessel-type filter was chosen because it provides a constant time delay in the output signal across the spectrum below the corner frequency. This characteristic allows the output from the filter to preserve the shape of the input function across the frequency band of interest. In reality, however, the CERES detector electronics feature two levels of filtering. The first level is provided by the thermal diffusion through the thermistor bolometers themselves in conjunction with the pre-amplifier electronics. The electronic signal is then further conditioned by the four-pole Bessel filter. Transfer functions for these two filtering stages were predicted by allowing the end-to-end model to be illuminated by a uniform, periodic time-varying source of known frequency and allowing the instrument output to reach its steady-state behavior. This procedure was repeated for several frequencies, and the outputs normalized to the value of steady-state output that would be reached if the source were of the same strength but not varying with time. The results of this analysis may be seen in Figure 5.26 which displays the predicted Bode and phase angle diagrams for the PFM total channel sensor. From Figure 5.26 it may be clearly seen that although the Bessel filter has a corner frequency of 22 Hz, the entire instrument (i.e. both stages) produces a corner frequency of approximately 14 Hz.

In order to assess the effectiveness of the low-pass filtering, a simple numerical experiment was conducted. The end-to-end model was allowed to view a diffuse source whose brightness varied with time as seen in Figure 5.27(a). Specifically, the input signal was defined by the superposition of two sine waves of equal peak-to-peak amplitudes of 30 μW, but different frequencies, 10 and 30 Hz on a constant signal of 30 μW. The resulting time series of power arriving at the active detector has mean, minimum and maximum values of 30, 0, and 60 μW, respectively. The effect of the filtering should be to remove the extraneous "noise" component at 30 Hz, while preserving the "signal" at 10 Hz. The predicted output may be seen in Figure 5.27(b).

In Figure 5.27, the curve labeled "Instrument" refers to the fully conditioned signal as it leaves the output of the Bessel filter. The curve labeled "Pre-amp" represents the signal immediately before entering the Bessel filter. It may clearly be seen in Figure 5.27(b) that the conditioning which occurs through the pre-amplifier is not effective at removing the 30-Hz component of the signal. However, the Bessel filter clearly removes the 30-Hz component. The fully conditioned signal is similar to what the instrument would produce if it were observing the superposition of a 10 Hz signal having a sinusoidal oscillation of 30 μw peak-to-peak and a constant signal of value 30 μW. In this scenario the minimum and maximum power arriving at the active detector would be 15 and 45 μW, respectively. Figure 5.28 gives a comparison of the fully conditioned signal and the results from an experiment in which a single sine wave of 10-Hz frequency having a 30 μW mean value and 30 μW peak-to-peak variation was viewed by the end-to-end model. The attenuating effect of the filtering is roughly described by Eqs. 5.24 and 5.25 where f_{n1} and f_{n2} represent 10 and 30 Hz, respectively. The quantity

$$Y_{in}(t) = 30 + 15\sin(2\pi f_{n1} t) + 15\sin(2\pi f_{n2} t) \tag{5.24}$$

represents the actual power input to the active detectors, while the quantity

$$Y_{out}(t-\tau) = 30 + 15\sin(2\pi f_{n1} t) \tag{5.25}$$

represents the recovered power which the instrument believes it is seeing as a function of time

5.8 Impact on Instrument Performance of Mismatched or Degrading Detectors

It is the nature of thermistor bolometers to drift. This drift is due to a long-term curing process produced by vacuum exposure. Before entering orbit, the longest single period of time they remain under vacuum is about ten days, or 240 hrs, during radiometric ground calibration. Drifts are explained by variations in thermal impedance between the thermistor layer of the detectors and the aluminum substrate. During the manufacturing process it is possible to trap air in the epoxy which bonds the Kapton® layer to the substrate, or in the epoxy layer which bonds the thermistor to the Kapton®. The physical layout of these layers may be seen in Figure 3.8. Upon vacuum exposure these air pockets begin to outgas slowly, and as they do so they leave behind evacuated voids. The evacuated voids conduct heat more slowly than air-filled voids and thus the thermal impedance has increased. In the worst case, the voids may be so large and their venting path so restricted that upon exposure to vacuum the detector can delaminate due to the relatively high-pressure air pockets between the layers.

It is not possible to predict how the performance of the detectors will vary over the life of a mission. With the testing that is available to characterize the detectors while still on the ground, the detectors which are believed to be most stable are chosen to be active detectors. The idea is that the diffusion and thus the response characteristics of the compensating detectors are less important overall since they do not directly view rapidly changing scenes. However, the compensating detectors do see thermal variations on the same time scale but of a lower magnitude due to self-heating. The interaction between self-heating and overall system performance is still not well understood and is generally treated as negligible. If self-heating were truly negligible varying the thermal impedance of the compensating detector only should have no influence on the time constant or responsivity of the fully assembled detector module assembly.

In order to simulate the curing process the end-to-end model was run with varying levels of thermal impedance between the compensating thermistor layer and aluminum substrate. This increase in thermal impedance was accomplished by varying the thickness of the epoxy layer which bonds the thermistor to the Kapton® in the compensating detector. Results of this study are given in Table 10.

Inspection of Table 10 reveals that as the thermal impedance increases, i.e. as epoxy thickness increases, the overall responsivity and time constant of the assembled detector module assembly decrease. In explaining this phenomenon, it is important to realize that the signal which leaves the bridge in the pre-amplifier electronics is directly proportional to the temperature difference in the thermistor layers of the active and compensating detectors. As the thermal impedance of the compensating detector increases, the temperature of the compensating thermistor will be higher for the same amount of self-heating. As a result the active and compensating detectors are now nearer in temperature. This means that their temperature difference, and thus the signal produced for the same radiative input to the active detector, is now smaller. In fact, a doubling of the epoxy layer thickness results in a decrease in responsivity of 3.8 percent and a decrease in time constant by 4.8 percent. Note that any actual changes which occur on the actual flight sensors will be sampled and accounted for through the use of on-board calibration sources.

5.9 Integration of the Atmospheric Radiation Model with the CERES Instrument Model

A Monte-Carlo-based ray-trace model of the shortwave radiative transfer in the atmosphere was developed by Villeneuve [33]. The primary goal of Villeneuve's work was to demonstrate the sensitivity of the anisotropy of the Earth's shortwave radiative field to variations in cloud parameters. Villeneuve computed reflected shortwave radiative fields at the top of the atmosphere for several typical Earth scenes. The Earth scenes and particularly the cloud patterns were made very realistic by using processed LandSat images to define both cloud morphology and cloud properties.

This work is invaluable in producing realistic inputs for the end-to-end model of the CERES instruments. A procedure was developed to scan a series of Villeneuve's individual Earth scenes in order to produce an input time series for the end-to-end model.

The Atmospheric Radiative Transfer (ART) model produces angular distributions of radiance at the top of the atmosphere. A scanning program was developed by Villeneuve [33] to simulate the scan pattern of a scanning radiometer. This program takes the individual 50-by-50 km scenes shown in Figure 5.29 and constructs a strip of atmosphere by assembling any number of these scenes in a specified order. An example of 500 km of atmosphere made up of ten scenes is shown in Figure 5.30. In the scanning program the simulated location of the satellite with respect to the end of the strip is defined by its altitude above the TOA and normalized x-y coordinates. Both x and y vary between zero and unity; however, in the case of Figure 5.30, unity represents 50 km in the x-direction on the TOA and 500 km in the y-direction. Once the satellite is positioned, the scan path can be determined by giving the coordinates of the starting and ending points of the scan. Figure 5.31 shows three satellite positions and their corresponding scans. The satellite at position number 3 would start its scan with a nadir view and end with a limb observation, whereas the satellite at position number 1 would start with a limb view and end at nadir. Another input to the scanning program is the scan step size. The scan step size is set to match both the true scan rate of the radiometer and the time step increment of the numerical model.

Villeneuve created a reverse ray-trace whereby the radiance arriving at the instrument aperture in each of the angular bins defined in Section 5.5 is computed for each scan step. This radiance is determined by randomly tracing a statistically significant number of rays from the instrument aperture towards the TOA within each angular bin in order to establish a relation between the radiative field leaving the TOA and that arriving at the instrument. The TOA has been discretized into 1-km cells, and for each TOA cell, the angular distribution of solar radiation leaving the TOA is known. Hence, when the ray hits a TOA cell, its angle of incidence is determined and a connection is established between the radiance leaving the TOA cell in the direction of the incoming ray and the radiance incident upon the instrument aperture in the angular bin within which the ray was emitted. Once a sufficient number of rays has been traced for one angular bin, the radiance arriving at the aperture associated with a given bin is simply the average of the radiances associated with each of the traced rays. The radiance at the aperture is then converted into power.

For each scan step, the scanning program provides an angular distribution of power at the aperture which serves as the radiative input to the instrument end-to-end model. The spatial distribution of absorbed energy on the detector is determined from the angular distribution of power at the aperture. The response of the sensor is then computed using the end-to-end model. Since the scan step corresponds to the time step of the electrothermal model, the radiative input to the model can be updated at every time step. This process simulates very accurately a continuous scan across an Earth scene since the time step is very small, 1 ms.

Once the time series of instrument output has been obtained, the time series of measured radiances is recovered utilizing Eq. 5.2 and the coefficients determined in Sections 5.3.2.1 through 5.3.2.5.

5.9.1 Field-of-view optimization

The optical field-of-view covers a TOA patch that is largely nonhomogeneous due to both cloud composition and Earth surface type. The CERES operational data processing algorithms replace the actual spatial distribution of emitted radiance within an instantaneous footprint with that of an equivalent blackbody source. This results in the question of how best to define the footprint to use in the operational algorithms.

In order to attempt to answer this question, a study was conducted using Villeneuve's Atmospheric Radiative Transfer model in conjunction with the completed end-to-end model.

Villeneuve's model was used to generate a 500-km TOA patch which was subsequently scanned by the end-to-end model to determine the amount of energy which arrives at the active detector as a function of time. In Section 5.3.2.1 a direct relationship was established between the power arriving at the active detector and the aperture radiance for a full-field, uniform calibration source. Of course, operationally such scenes do not exist. The filtered radiance recovered by Eq. 5.2 is the integrated full-field radiance weighted by the optical point spread function.

It is desired to determine an effective field-of-view, and thus a footprint, such that the corresponding optical point spread function has a normalized throughput value of either unity or zero, depending on whether the source is inside or outside the effective field-of-view. In order to determine the optimal effective field-of-view a procedure was devised such that the TOA patch generated by the Atmospheric Radiative Transfer model was scanned from the three satellite locations indicated in Figure 5.31. Ten different effective fields-of-view were defined, each based on one of the contour lines of the OPSF shown in Figure 5.17. The radiance values in each discretized field-of-view solid angle, Ω, were multiplied by either unity or zero depending on whether or not Ω was inside or outside the defined effective field-of-view contour line. By repeating this for all of the full-field Ω values the average effective field-of-view radiances were determined for each of the approximately 500 footprints of the TOA patch. A ratio was then formed of this average radiance at each footprint to the actual power arriving at the detector. This ratio is plotted as a function of TOA position in Figure 5.32. The actual power arriving at the detector is due to the actual scene, not just to the discretized field-of-view solid angles within the effective field-of-view. Figure 5.32 displays these ratios as a function of TOA position for three effective fields-of-view, the 90-percent, 50-percent, and ten-percent contour lines of the optical point spread function. Ideally, this ratio should approach a constant value as the optimal field-of-view size is reached. Inspection of this figure shows large variations in the ratios of the 10- and 90-percent contour lines. To determine which of the ten effective fields-of-view most closely approximates the ideal, the standard deviation of the ratio values shown in Figure 5.32 was determined for each. These values were then plotted against the value of the contour line with the expectation that a minimum value would be observed. Figure 5.33 presents these results. From this figure it is clear that the optimal field-of-view is somewhere between the 40- and 50-percent contour lines. Further, this result is independent of the satellite orbital location in the simulations. Stated differently, if an instantaneous footprint size were to be defined such that a single uniform radiance could be assigned within it which best described the average actual radiance arriving at the aperture from the full field-of-view, the footprint size would be defined by the (approximately) 40-percent contour line of the optical point spread function.

5.10 CERES Pathfinder Experiment

The CERES operational data processing algorithms will merge higher spatial resolution imager data with lower spatial resolution broadband CERES data. The cloud imagers which will be used with the CERES data include the Visible Infra-Red Scanner (VIRS, 2-km resolution) on the TRMM platform and the Moderate Resolution Imaging Spectrometer (MODIS; 0.25-, 0.5-, and 1.0-km resolution) on the EOS-AM and EOS-PM platforms.

As part of NASA's CERES Pathfinder Experiment, CERES-like algorithms are being developed and tested using data from the NOAA-9 Advanced Very High Resolution Radiometer (AVHRR), High-Resolution Infra-Red Radiometer Sounder (HIRS/2), and Earth Radiation Budget Experiment (ERBE) instruments as surrogates for anticipated CERES broadband radiometric data. The convolution of data from the AVHRR, HIRS/2 and ERBE instruments provides a unique data set with which to study ways to improve scene identification for inversion of ERBE, and in the future CERES, broadband radiance measurements to TOA fluxes. A complete discussion of the Pathfinder experiment is given in reference 70.

The key to convolving the high-resolution imager data with lower-resolution ERBE and CERES data is knowledge of the dynamic instrument point spread function. The point spread function is used to weigh the higher-resolution imager data over the footprint of the ERBE and CERES instruments. This weighting is accomplished through the use of a grid, as described in Section 5.6.1. For ERBE, measurements of the dynamic instrument PSF were not taken during radiometric calibration. As a substitute, a theoretical point spread function based upon a true first-order system has been generated.

This section reports the results of a study to assess the sensitivity of varying the shape of the PSF on the final calculated TOA fluxes. To bound the problem the two extreme weightings of the PSF were defined and implemented in the standard algorithms used in the Pathfinder experiment. By varying only the weightings of the PSF, the sensitivity of the resulting TOA fluxes to PSF shape may be directly assessed. As stated previously the three-dimensional point spread functions are transformed into 16-by-16 equiangular arrays, where each angular bin contains the weighting of the PSF over a particular solid angle. The two extreme weightings in this methodology would be to create a large, uniform PSF, and a small, uniform PSF. This was implemented by weighting all of the 16-by-16 elements in the array with the value of 1/256 in the former case and by weighting the four central bins with a value of one-fourth in the latter case. These weightings may be seen in Figure 5.34.

Using these two PSF weightings, TOA fluxes were calculated for a standard hour of ERBE shortwave and longwave data product. One hour of data is approximately 2/3 of an orbit and corresponds to 32044 single-sample footprints. Although this number of data points is probably not statistically significant to assess the sensitivity across all combinations of scene type, the results do provide an adequate indication of the trend. Results from this study are given in Tables 11 through 14 along with Figures 5.35 and 5.36. Tables 11 and 12 display the overall footprint counts for cloud and geotype categories, respectively, for each weighting of the PSF. By comparing the last rows and last columns of each of these tables it is possible to see the influence which weighting the point spread function differently has on scene and cloud category classification. Table 12 indicates that the listed geotype classifications are relatively unaffected by the weighting of the PSF. This is as anticipated since geophysical features are large relative to a single satellite footprint. Table 11 on the other hand, indicates that the cloud classification is highly dependent upon the weighting of the PSF. This is also as anticipated since cloud spatial distributions may vary widely on the scale of a single footprint. The implication of Tables 11 and 12 is that any differences which are present as a result of the different weighting of the PSFs are due to cloud category misidentification.

5.10.1 Shortwave results

Table 13 presents the tabulated results of the comparison between the two weightings of the PSF for the ERBE shortwave channel. Specifically it presents the mean differences between the calculated TOA fluxes, the standard deviation of these differences and the number of footprints used in these statistics. The data are classified according to cloud category. For example, looking at the statistics in the upper left corner of the table where both the column and row are labeled clear sky, there were 4,447 footprints when both weightings of the PSF agreed that the sky was clear, of these samples the mean differences between the two weightings (16-by-16 and two-by-two) was -0.05 Wm^{-2}, and the standard deviation about this mean was 3.20 Wm^{-2}. If statistically significant numbers of samples were taken for every combination of cloud category, the mean differences and their standard deviation would increase moving away from the diagonal. A systematic error is suggested by the distribution of positive and negative mean values in the table. The mean values which appear below the diagonal all have a negative value as opposed to a positive value of approximately the same magnitude above the diagonal. This is a direct reflection of the importance of properly identifying and quantifying cloud cover in the shortwave spectrum. By analyzing the data globally, that is by comparing the data independent of cloud cover, the statistics produce a mean difference value of -0.504 Wm^{-2} with a standard deviation of 15.124 Wm^{-2}. This suggests that not using the correct weighting of the PSF for the shortwave spectrum does not lead to significant errors in calculating the mean TOA flux globally; however, it does significantly increase the uncertainty. On a regional, or cloud-based, scale significant errors may be introduced, as large as 30 Wm^{-2}, by using the wrong weighting of the PSF for the shortwave channel due to the strong anisotropy of reflected shortwave energy. These errors are very large considering that on average the reflected shortwave energy is on the order of 100 Wm^{-2}, as seen in Figure 1.1.

5.10.2 Longwave results

Table 14 presents the results for the longwave ERBE channel. Inspection of this table reveals results which are drastically different from those for the shortwave channel. The mean differences for the two weightings are all less than 0.75 Wm^{-2} regardless of how different the cloud classifications are for the same geo-located area. In addition the standard deviations are an order of magnitude less than those seen for the shortwave channel. This is anticipated since the Earth-emitted longwave energy does not have the same problems with anisotropy as does the shortwave channel. Looking at these data on a global scale, the mean difference was calculated to be -0.105 Wm^{-2} with a standard error of 0.7096 Wm^{-2}.

5.11 Autoregression Analysis

The fundamental premise of an autoregressive model is that a process, represented by a time series x_i, can be modeled based on the previous values of this process. Thus, it is assumed that the time series is at least partially deterministic.

For the current effort an initial attempt has been made to predict the change in value of radiance arriving at the detector at time t, based upon the instrument output from previous time steps. The value of radiance arriving at the detector may be determined by continually summing the changes in arriving radiance which occur over a discrete time step, Δt. We can write

$$\tilde{L}(t) = \tilde{L}(t-1) + \Delta\tilde{L}(t) \ , \tag{5.26}$$

and

$$\begin{aligned}\Delta\tilde{L}(t) = A_1[m(t)-m(t-1)] + A_2[m(t-1)-m(t-2)] + \\ \ldots + A_n[m(t-(n-1)) - m(t-n)] \ ,\end{aligned} \tag{5.27}$$

where $\tilde{L}$ is the value of radiance at time t ($Wm^{-2}sr^{-1}$), $\Delta\tilde{L}$ is the change in radiance between time steps t-1 and t, the m's are instrument output in counts at times t, t-1,…t-n, and the A_i's are coefficients which convert counts into radiance ($Wm^{-2}sr^{-1}$/count). Equation 5.27 is referred to as an autoregressive model of order n.

In order to solve for the A_i's, we introduce the vectorial notation, with N greater than the order n,

$$\begin{bmatrix} \Delta\tilde{L}_t \\ . \\ . \\ . \\ \Delta\tilde{L}_{t-N\Delta t} \end{bmatrix} = \begin{bmatrix} m_t - m_{t-\Delta t} & .. & m_{t-(n-1)\Delta t} - m_{t-n\Delta t} \\ . & .. & . \\ . & .. & . \\ . & .. & . \\ m_{t-N\Delta t} - m_{t-(N-1)\Delta t} & .. & m_{t-(N-n-1)\Delta t} - m_{t-(N-n)\Delta t} \end{bmatrix} \begin{bmatrix} A_1 \\ . \\ . \\ . \\ A_n \end{bmatrix} = [D] \begin{bmatrix} A_1 \\ . \\ . \\ . \\ A_n \end{bmatrix}, \quad (5.28)$$

or

$$[L] = [D][A] \ . \quad (5.29)$$

The quantity N can be equal to n, but the accuracy of the determined coefficients will increase with increasing N, since N represents the number of points which are going to be averaged. The random noise included in the time series m_i will be reduced by use of the average with N sufficiently large. The order of the matrix D is (N+1)n; that is, the matrix D is not square. Therefore, in order to obtain the coefficients A_i the following technique is used. The first step is to multiply Eq. 5.28 by the transpose of matrix D, from which we obtain a square matrix for the system, D^TD,

$$[D]^T[L] = [D]^T[D][A] \ . \quad (5.30)$$

For the second step, the coefficients are obtained by multiplying Eq. 5.30 by the inverse of the square matrix D^TD such that

$$[A] = \left([D]^T[D]\right)^{-1}[D]^T[L]. \quad (5.31)$$

Autoregressive models have extremely high frequency resolution, independent of the sampling interval, because the calculation of the coefficients in the autoregressive model takes into account this sample interval, through the values of the time series used in Eq. 5.28. There are also significant disadvantages in using this technique. The largest of these is the problem of determining the model order n which provides the most accurate answers. This order must be decided upon before the analysis is begun. If the order is not sufficiently high the resulting spectrum estimates add nonexistent components to the true signal content. Determination of model order requires subjective judgment based on experience [71].

In the current effort n was assigned a value 4 and N set at one of two discrete levels, 5 or 12, resulting in two sets of values for the coefficients A_i. Additionally, the spectra of the time series of incident radiance, the left-hand side of Eq. 5.30, was set at one of three discrete levels, 10, 20 or 30 Hz. The use of discrete frequencies allows the influence of the conditioning electronics to be sampled. In all, four sets of A_i coefficients were determined, and their values are presented in Table 15.

Results of this preliminary study indicate that attenuation of the output signal at elevated frequencies due to the presence of the four-pole bessel filter was the overriding source of error in the attempt to recover the input signal utilizing the autoregressive technique. The autoregressive model recovered incident radiances with an accuracy of greater than 99.9 percent for cases when the spectral content of the sampled scene matched that of the scene used in the determination of the coefficient values A_i. Errors in recovered radiance were significantly greater when scenes whose spectra differed from the original scene were viewed. In fact, the errors in the recovered radiance were well correlated the bode diagram curve labeled “Instrument” presented in Figure 5.26(a).

By correlated it is meant that if a 10 Hz signal was utilized as the input for determining the coefficients A_i, then attempting to recover an input signal of 1 Hz led to an over prediction of about 20 percent. This is explained by the fact that the output of a 10 Hz signal is attenuated by approximately 1.6 db from that of a slowly varying signal, while a 1 Hz signal is attenuated by very nearly 0 db from that of a slowly varying signal of the same peak-to-peak value, as seen in Figure 5.26(a). For the current effort slowly may be defined as less than 1 Hz since there is negligible attenuation in the output signal for input scenes of less than 1 Hz. In general however, the term slowly is defined by the transfer function for a given system. In summary, the magnitude of the output signal for a 10 Hz scene is only 83-percent of that for a slowly varying signal of the same peak-to-peak magnitude, while the output for a 1 Hz scene is essentially identical to that of a slowly varying scene of the same peak-to-peak magnitude. This means that the recovered radiance should over or under predict the actual incident radiance by the ratio of the attenuated output signal of the current scene to the attenuated output signal for the scene used to define the coefficients, A_i. In the current scenario this ratio is 1.0/0.83, or an overprediction by a factor of 1.20. Similarly, if the set of coefficients derived for a 10 hz scene were utilized to recover an input signal of 20 Hz, the recovered radiance would be off by a factor of 0.53/0.83, or underpredicted by a factor of 0.63. This is due to the fact that a 20 Hz output signal is attenuated by approximately 5.6 db, or 47-percent. The results of this latter case may be seen in Figure 5.37 which displays the power reaching the active detector from a sinusoidally varying 20-Hz scene of 50 μW peak-to-peak magnitude and the power recovered using the autoregression model where the A_i coefficients were determined by viewing a sinusoidally varying 10-Hz scene of the same peak-to-peak magnitude.

Although **this** investigation was not successful in creating an autonomous autoregression model to accurately predict the performance of the CERES flight models, it does show promise. The current effort indicates that the autoregression technique has the ability to recover incident radiance to a high degree of accuracy for the domain where frequency dependent attenuation of the output signal is non-existent. In the domain where frequency dependent attenuation of the output signal does exist, the autoregression models seem to recover incident radiance at least as well as the current count conversion equations. Further research must be completed to completely understand the relationship between the attenuation caused by signal conditioning electronics and the method in which coefficients appearing in the autoregression models are calculated. The current author leaves this task for future students in the Thermal Radiation Group.