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Description of Heat Flux Measurement Methods Used in Hydrocarbon and Propellant Fuel Fires at Sandia

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Description of Heat Flux Measurement Methods Used in Hydrocarbon and Propellant Fuel Fires at Sandia

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Abstract

The purpose of this report is to describe the methods commonly used to measure heat flux in fire applications at Sandia National Laboratories in both hydrocarbon (JP-8 jet fuel, diesel fuel, etc.) and propellant fires. Because these environments are very severe, many commercially available heat flux gauges do not survive the test, so alternative methods had to be developed. Specially built sensors include "calorimeters" that use a temperature measurement to infer heat flux by use of a model (heat balance on the sensing surface) or by using an inverse heat conduction method. These specialty-built sensors are made rugged so they will survive the environment, so are not optimally designed for ease of use or accuracy. Other methods include radiometers, co-axial thermocouples, directional flame thermometers (DFTs), Sandia "heat flux gauges", transpiration radiometers, and transverse Seebeck coefficient heat flux gauges. Typical applications are described and pros and cons of each method are listed.

Heat Flux Measurement Methods in Fires

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Heat Flux Measurement Methods in Fires

Introduction

Heat flux measurements are important because, in heat transfer applications, providing only a temperature measurement is sometimes not sufficient to fully specify the environment. Using the electrical analogy (Ohm's Law), one needs two of the three variables (current, potential difference, or resistance) to fully specify an electrical circuit. Similarly, in a heat transfer application, one needs two of the three variables (heat flux, temperature difference, and thermal resistance) to fully specify the problem of interest. The temperature difference is similar to a potential difference, heat flux is similar to current, and thermal resistance is similar to electrical resistance.

Most often the metric of interest is the response of an item in a fire. This metric is more related to the heat flux than just to the temperature because the thermal resistances to radiation and convection are not captured when only the temperature is specified.

The measurement of heat flux in hydrocarbon fuel fires (e.g., diesel, JP-8, etc.) is difficult due to the high temperatures (e.g., 1000°C or higher), the sooty and chemically reactive environment, and due to large temperature gradients which produce thermal stresses. Many commercially available sensors do not work well in fires because they are water-cooled. Soot builds up on the sensing surface due to thermophoresis and this layer of soot changes the gage sensitivity. Post-test soot deposit thicknesses of 1/4" have been measured after fires at Sandia National Laboratories (SNL). Eliminating use of water-cooled gauges, partially cooling the gauges, or using gas purging would reduce or eliminate the soot deposition. However, many non-cooled gauges do not survive in long duration fire environments, or the gage sensitivity is not constant with the sensor temperature.

Radiometers, which cover and protect the sensing surface with a transparent window (e.g., sapphire) are used in fires and in some cases work well. But like any water-cooled gage, the surface can become coated with soot, and sometimes the window cracks, or becomes discolored.

Therefore, many commercially available gauges do not work well when trying to measure heat flux inside a hydrocarbon fuel fire.

Similarly, for propellant fires, the temperatures (e.g., 2,500°C or higher) are so extreme as to make most commercially available gauges useless – they will fail seconds after being exposed to the fire. The only reliable methods to measure heat flux in propellant fire environments are 1) via heavy, thick walled "calorimeters" that will survive for some period of time, and 2) non-contact devices. Non-contact devices such as optical pyrometers or spectrometers are being developed for use in propellant fires. (The devices have already been developed, but interpreting the results one obtains is not an easy task.)

An example of a device that is not actively cooled is a relatively simple device called a "calorimeter." Calorimeters are made of metals that do survive fires (e.g., Inconel 600 or 304 stainless steel [SS]) with a thermocouple (TC) on the unexposed, cold side backed with insulation. Net flux into the exposed, hot surface can be estimated using temperature measurements of the cold surface, material properties, and assumptions about the cold side boundary condition. Inverse heat conduction codes (e.g., IHCP1D (ref. [1]) or SODDIT (ref. [2])) can estimate the exposed

Heat Flux Measurement Methods in Fires

surface net flux. One-dimensional heat conduction is almost always assumed to simplify the calculations. If the heat transfer is not 1-dimensional, an added element of uncertainty is present.

One can also mount the TC on the exposed side, and this is sometimes the only avenue available because the unexposed side surface cannot be disturbed. However, we have found that there are relatively large bias errors for TCs mounted on the exposed side, and the temporal fluctuations (i.e., noisy data) make data reduction difficult. Measurements made on the unexposed side, if made through a relatively thin surface (e.g., 1/16 to 1/8"), are enough to reduce the noise and bias errors.

In commercially available heat flux gauges (e.g., Gardon or Schmidt-Boelter) a voltage is generated based on the net amount of energy absorbed into the sensing surface. Based on the calibration method, this net energy can be related to either an incident radiative flux or a net flux. In fire applications, an estimate of the heat flux (both radiative and convective) incident on the gage surface is usually the measurement desired. Net flux to one surface cannot be used for other surfaces because every surface will absorb energy differently. For thermally massive test objects, the fire is affected by the presence of the massive object. In addition, convection can be sensitive to geometry and surface temperature. Therefore, one should be careful about applying convective fluxes from one type of gage to another.

For this discussion, the fire is assumed to not be affected by the gage because normally the heat flux gauges are small relative to the fire.

Due to the severe environment and difficulty of measuring the required parameters, the measurement of net heat flux in hydrocarbon fuel fires usually has large uncertainties (e.g., ± 15 -19% when using an inverse heat conduction code (ref. [3])). Two-dimensional effects can add additional uncertainty, e.g., 2-20% depending on the degree of 2-dimensionality (ref. [4]). Uncertainties of similar magnitude can occur when one estimates incident flux given net flux from an S-B or Gardon type gage because of soot deposition on the sensor face, and from convective effects. Uncertainties of steady state heat flux measurements using various methods were analyzed in ref. [5] and large uncertainties (e.g., ± 25 -40%) are possible.

Another type of measurement method will be called the "energy balance method." In this method the temperature of a surface is measured and an energy balance is developed on that surface. Heat flux from a hemisphere surrounding the surface contributes to the energy balance. From the energy balance one can estimate the incident heat flux. This method has been used successfully to estimate steady incident heat flux in fires (ref. [4]).

This report describes commonly used heat flux measurement methods at Sandia, some uncommon methods, and some potentially new methods being studied. These methods have been developed over the years at SNL and elsewhere to improve our techniques for heat flux measurement in fires.

Heat Flux Measurement Methods in Fires

Background

Literature Review

A number of different types of commercially available heat flux gauges are available. Gardon (circular foil) gauges, Schmidt-Boelter (S-B) gauges, radiometers, thermopile type gauges, and “thin film” gauges are a few. Figures 1 and 2 show photographs of a typical Schmidt-Boelter type gage (Figure 1) and a thin film gage (Figure 2). Thin film gauges are not suitable for fire measurements because they do not survive.

Childs, et al. (ref. [6]) provides a comprehensive listing of heat flux measurement techniques. Optical devices are also used to infer heat flux if the emissivity of the radiating surface is known. Optical sensors are normally grouped into two types, thermal and photon detectors (ref. [7]). All types of gauges respond to the net energy into the sensing surface, except photon detectors which respond to the incident radiative flux. This report focuses only on non-optical detectors.

Figure 1: Typical Schmidt-Boelter Heat Flux Gage

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Figure 2: Thin Film Type Heat Flux Gage

The sensitivity of a typical non-optical type gage (e.g., Schmidt-Boelter, Gardon, thin film, etc.) is usually determined by a radiative calibration in a blackbody type furnace. These calibration devices are typically designed so that convection is negligible. If this is so, then the conversion from net to incident radiative heat flux is straightforward (assuming the absorptivity is constant with wavelength or the application has the same radiative distribution as the calibration source, and if the gage temperature is low enough). One divides the net flux by the surface absorptivity to obtain the incident flux. Alternatively, one can compare the output of the gage to a known calibration source to directly obtain the incident radiative flux.

In combined environments (radiative and convective), one cannot make this conversion unless the gage sensitivity to convective heat flux is the same as for radiative heat flux. This is typically assumed but there is a growing body of evidence that suggests that gage sensitivity coefficients are not the same for convection and radiation. Recent measurements on a Schmidt-Boelter gage showed that the convective sensitivity (i.e., $\text{kW/m}^2/\text{mV}$) coefficient was about 12% lower for convection (ref. [8]). Past, similar studies on Gardon gauges have shown the same behavior (ref. [9]). Therefore, to obtain an accurate estimate of the incident flux to a surface with both radiative and convective heat transfer present, one needs to account for the different sensitivities. This is not normally done because it is often not realized that the sensitivities are different.

One way to estimate the effect of a mixed (radiative and convective) environment is to partition the heat flux into convective and radiative parts, and assume the total is made of appropriate fractions of each. This is mathematically shown in equation {1}:

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$$q_{net} = mV * (K_{rad} * F_{rad} + K_{conv} * F_{conv}), \quad \{1\}$$

In equation {1} 'mV' is the voltage output of the gage, K_{rad} is the gage sensitivity to radiative heat flux, K_{conv} is the gage sensitivity to convective heat flux, and F_{rad} and F_{conv} are the fractions of energy from each source. The convective and radiative fractions are not accurately known in fire environments, and can vary with time. Manufacturers normally only calibrate their gauges in radiative environments, so convective calibration factors are not known. If one assumes the sensitivities are the same, then equation {1} reduces to equation {2}, the same as what is commonly used.

$$q_{net} = mV * K_{rad}, \quad \{2\}$$

As shown in ref. [5]), the approach used in equation {1} can result in very high measurement uncertainties (e.g., $\pm 25\text{-}40\%$) because F_{rad} , F_{conv} , and K_{conv} are not well known.

An alternate method of estimating the effect of convection on the gage calibration was developed by Kuo and Kulkarni (ref. [9]) who studied the sensitivity of Gardon type gauges to a mixed environment consisting of both radiative and convective parts. They showed that for Gardon gauges (calibrated in radiative-only environments), significant errors could occur under certain conditions of large gage sensing surface and large convective heat transfer coefficients. For a free convection vertical wall fire experiment, an error of about 15% would occur by using the sensitivity provided by the manufacturer. In other words, the gage sensitivity to convective heating was different than for radiative heating. Kuo and Kulkarni developed correction factors for different sized gauges and convective heat transfer coefficients based on the gage geometry and factors including the heat transfer coefficient, h . This method would lead to lower estimates of the uncertainties than those described in ref. [5].

Borell and Diller (ref. [10]) evaluated a convective calibration method for Gardon type gauges. They found that the convective calibration was non-linear, whereas a radiation calibration is linear to within a few percent (e.g., $\pm 3\%$). Borell and Diller noted that radiative calibration methods deliver a constant heat flux to the gage surface, whereas a convective calibration method delivers a constant convective heat transfer coefficient to the gage surface. This causes different results for the two types of calibrations, due to different temperature profiles on the gage surface. Strictly speaking, these results only apply to the Gardon type gage which under radiative flux conditions has a parabolic-type temperature profile on the sensing surface. Schmidt-Boelter (S-B) type gauges are believed to have a much flatter temperature profile on the gage surface. However, as indicated above, recent evidence shows that S-B type gauges also have different gage sensitivities to radiative and convective heat transfer (ref. [8]).

Robertson and Ohlemiller (ref. [11]) discuss heat flux measurements in low radiation environments. The desired measurement was the incident radiative flux to a surface. In these low radiation environments one has to be sure to account for the gage surface temperature, which is a function of the cooling water temperature. An apparent output occurs if the gage surface temperature is either above or below the effective radiation temperature of the surroundings. In addition, convective input to the gage when mounted in a vertical structure was apparent when testing the

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gage with and without the mounting structure in place. These two effects show the importance of understanding gage calibration and operation in the particular application of interest.

Bryant, et al. (ref. [12]) studied the uncertainty of incident radiative heat flux measurements estimated from total heat flux measurements for gauges placed outside but near a typical room corner fire specified by ISO 9705 (ref. [13]). In this case, a Schmidt-Boelter (S-B) gage was located some distance away from the fire. The gage was water-cooled without (presumably) having the gage surface fouled by soot deposition. The uncertainty methodology used in Bryant, et al. consisted of establishing a control volume around the sensing surface then developing an energy balance. In this manner the incident radiative flux was related to the net flux, the convective flux and a re-radiation term. An uncertainty analysis was performed on each of these parameters by using sensitivity coefficients. Uncertainties in each of the parameters were determined from measurements, general knowledge, or manufacturer's specifications. A root-sum-square method was used to combine the individual uncertainties. Results showed that for the case considered one could estimate radiative flux from total flux measurements to within about 20%, but only with a confidence level of about 67% or 1σ .

As a result of the uncertainties present in mixed radiative and convective environments, and because fire environments are so severe that many commercially available gauges do not survive these environments, a suite of relatively low cost, simple designs have been developed at SNL and elsewhere to attempt to rectify the problems.

Heat Flux Measurement Methods

The SNL built sensors are, for the most part, simple and robust in design and don't require a calibration. They depend on one or more temperature measurements, well known boundary conditions, well known temperature dependent material properties, and a mathematical model to infer heat flux.

1.1 Data Reduction Methodologies

This section discusses the terminology and methodology used for data reduction for the Sandia built heat flux gauges.

Figure 3 shows a schematic of the surface being analyzed. This is the sensing surface of the gage, subjected to a mixed environment consisting of both radiative and convective fluxes, an environment typical of a hydrocarbon fuel fire. It is assumed that condensation does not take place. A control volume is drawn over the sensing surface. The heat flux net into the surface (q_{net}) is a function of the incident radiative flux ($q_{inc,r}$), the surface properties (emissivity ε and absorptivity α), the emitted flux ($q_{emit} = \varepsilon\sigma T_s^4$), and the convective heat gain or loss, $q_{conv} = h(T_\infty - T_s)$. It is further assumed that there is neither condensation nor any chemical reactions that could affect the heat flux to the surface (e.g., material deposition from propellant fires). ' T_s ' is the surface temperature, ' h ' is the convective heat transfer coefficient, and ' T_∞ ' is the free stream gas temperature.

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$q_{inc,r}$ = incident radiative flux
 q_{emit} = emitted flux
 q_{conv} = convective flux
 q_{net} = absorbed flux

Figure 3 Energy Balance at Sensing Surface

Equation {3} expresses the overall energy balance on the control volume surrounding the gage surface:

$$q_{net} = \alpha q_{inc,r} - q_{emit} + q_{conv} \quad \{3\}$$

Substituting for q_{emit} and q_{conv} :

$$q_{net} = \alpha q_{inc,r} - \epsilon \sigma T_s^4 + h(T_{\infty} - T_s) \quad \{4\}$$

Normally, one invokes the assumption that $\alpha = \epsilon$ for a gray body and that will be done here. Rearranging equation {4} one can solve for $q_{inc,r}$, the incident radiative flux:

$$q_{inc,r} = (q_{net} / \epsilon) + \sigma T_s^4 - (h / \epsilon)(T_{\infty} - T_s) \quad \{5\}$$

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One is normally interested in $q_{inc,r}$ and q_{conv} . These provide the total heat flux incident on the surface. The total flux incident on the surface is the sum of the incident radiative part and the convective part:

$$q_{inc,t} = q_{inc,r} + h(T_{\infty} - T_s). \quad \{6\}$$

Two methods are commonly used to estimate the total incident flux to the surface from equation {5}:

- 1) the “energy balance” method
- 2) inverse heat conduction method (which measures net flux) plus the emitted flux

In both methods one attempts to measure or infer the parameters in the right side of equation {5}. The difference is how one attempts to quantify the net flux (q_{net}) to the surface. In the energy balance method (ref. [14]) one estimates the energy storage in the gage (metal and insulation) to obtain q_{net} ,¹ while in the inverse heat conduction method one uses an inverse heat conduction code to estimate q_{net} . Both methods provide relatively good estimates of heat flux, but both have pitfalls that can result in large uncertainties.

Uncertainty analyses of these methods have been performed and documented in ref. [5], so will not be repeated here. However, thorough descriptions of the methods including pros and cons have not been documented yet and so will be done so in this report.

1.2 Energy Balance Methods

1.2.1 Sandia “Heat Flux Gage” (HFG)

An early attempt to develop a Sandia-built gage was called the Sandia “Heat Flux Gage” (HFG). This gage consisted of a thin (e.g., 10 mil thick) stainless steel sensing surface mounted onto a cylindrical housing (4” diameter). The unexposed side of the thin plate (inside the housing) was backed with ceramic fiber insulation. A thermocouple was mounted on the unexposed side of the thin plate, between the plate and the insulation, and that temperature with thermal properties and assumptions was used to infer the incident radiative flux to the surface. The response of this gage was analyzed in ref. [14]. See Figure 4 and Figure 5 for photographs of a typical HFG. The sensing surface is painted black before use.²

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¹ The data analysis program (an Excel macro) has been very useful in comparing results to those from using the inverse heat conduction method.

² We normally use Pyromark™ black paint.

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Figure 4: Overall View of Sandia "HFG"

Figure 5: Sandia HFG Sensing Element

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Although the basic idea was sound, there were two design faults that made the HFG less desirable than other methods. The first issue was related to the thermocouple (TC) attached to the unexposed side of the thin plate, and the second was the mounting hardware.

One should always attempt to attach the smallest size TC to the surface of interest, while still maintaining reliability. A rule of thumb is to have the diameter of the TC no larger than the plate thickness and smaller if possible.³ There are 2 reasons for this: 1) better transient response with smaller TCs, and 2) depression of the plate temperature because of the mass of the TC. The smaller the TC the faster the transient response, so one should use the smallest TC possible. When mounted on a surface, the TC draws heat away from the plate, so the TC should be as small as possible to disturb the plate temperature as little as possible. For very thin plates (e.g., 10 mil thick used for the HFG), using the rule of thumb, one should use a 10 mil diameter TC. Although mineral-insulated, metal sheathed (MIMS) TCs this small can be purchased, they are very fragile and not robust so fail often. In early designs, 20 mil diameter TCs, as well as “intrinsic TCs” (where individual wires were stripped out of the metal sheath and attached to the plate) were used. However, both types of TCs had poor reliability. As a result, over time, the TC size migrated to 63 mil diameter designs to gain the reliability required. These 63 mil diameter TCs were ~6 times larger diameter than the 10 mil sensing plate so the TC affected the measurement by drawing heat away. As a result the transient response of these gauges was very poor as documented in ref. [14].

The second issue resulted from the housing. As can be seen in the figures, the sensing plate was surrounded by a heavy mounting structure to protect it from the fire. This caused heat loss through the sides, so 2-dimensional effects were present. Comparisons with other heat flux estimates showed the HFG design to be less than optimal (ref. [15]).

These two issues (2-d effects and too large a TC) resulted in poor performance so we stopped using this design. However, this design can be modified to correct these issues.

1.2.2 Coaxial Thermocouples

Coaxial thermocouples are fast response sensors that measure the surface temperature of an object. The measurement is made by drilling a hole in the object perpendicular to the plane of the surface and inserting the coaxial TC into the hole. This method obviously only works if one can drill a hole in the test object, and this is not always the case. The benefits of this method are faster response and more accurate exposed surface temperatures.

A typical coaxial TCs consists of a 10mil diameter constantan rod surrounded by a 63 mil outside diameter chromel sleeve. A very thin (e.g., 0.5 mil thick) layer of magnesium oxide insulation electrically isolates the two thermoelectric elements. A “sliver” junction is made by abrading the exposed tip.⁴ The sliver junction is fragile and so intermittent signals sometimes result. Vacuum deposited plating is another more robust method to make the junction between the thermoelements. Figure 6 shows a section view schematic of a coaxial TC. This figure only shows the basic components. There is no active cooling in a coaxial TC.

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³ Personal conversation with Dr. Ned Keltner, retired Sandian, President of F.I.R.E.S., Inc., June 2008.

⁴ Description from B. Blackwell, Appendix 1.

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The coaxial TC provides a measure of the exposed surface temperature. With a relatively simple energy balance equation on the exposed surface, one can estimate the incident flux. Equation {7} expresses this balance on the exposed surface node (see Appendix 1):

$$[kA(T_1^{n+1} - T_2^{n+1}) / \Delta x] - [qA] + [\rho C \Delta x A (T_1^{n+1} - T_1^n) / 2 \Delta t] = 0 \quad \{7\}$$

where 'k' is the thermal conductivity of the surface material (i.e., the material surrounding the coaxial TC), 'A' is the surface area, T_1 is the temperature of the surface node, T_2 is the temperature of the next node in the material, ρ is the density, C is the specific heat, n is the present time, n+1 is the next time, Δx is the node thickness perpendicular to the surface plane, 'q' is the applied flux, and Δt is the time step.

Figure 6: Section View of Coaxial TC

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Blackwell (ref. [16]) has analyzed and compared results (temperature and heat flux) from 4 coaxial TCs, 4, 20 mil diameter MIMS TCs, and 4 intrinsic TCs exposed to the same environment. Results are provided in Appendix 1 and show that calculated net heat flux from inverse methods (20 mil and intrinsic TCs on the unexposed side) and the coaxial TCs (exposed side) were very close.

This method is best used in those applications where the material of the coaxial TC can be made the same as or close to the parent material. In this case the co-axial TC would be expected to respond very similar to the test unit. If this is not the case one might be required to perform additional calculations to see if the material differences add significantly to the uncertainty of the measurement. Also, coaxial TCs are best used for applications where fast response is critical, and one can penetrate the test item to insert the coaxial TC.

1.3 Inverse Heat Conduction Methods

Inverse heat conduction methods use unexposed face temperature measurements and thermal properties to estimate the net flux into a surface. One-dimensional heat flow is almost always assumed but multi-dimensional models do exist. Because the unexposed surface temperature is affected by any mode of heat transfer, the net flux comes from radiation and convection. This method is especially useful for very intense fires (e.g., propellant fires) where exposed surface sensors would not survive. It is also useful for measurements where front face temperatures are too inaccurate and/or noisy for use in estimating heat fluxes. When a sensor is mounted on the exposed surface it is subjected to severe environments and rapidly changing temperatures. As a result, noisy data and bias errors are often present.

One issue with inverse methods is that the solution is not unique. One obtains different results depending on (for example) the number of “future times” used (ref. [22]). Therefore, there is a certain amount of engineering judgment required to use this method, and this judgment may not always be correct. Nonetheless, this is a powerful and useful method and is extensively used.

1.3.1 “Calorimeter”

“Calorimeters” are a broad class of devices used to mimic a test item and provide data for heat flux estimation. For example, if the test item is of a cylindrical shape, and one would like to estimate the heat flux to that size and shape, one can fabricate a calorimeter of the same outer dimensions, shape and thermal mass. The calorimeter is outfitted with TCs, and insulated on the inside to provide a known boundary condition. In this manner the calorimeter responds in a similar manner as the test item, and one can obtain heat flux measurement estimates without having to use an expensive test item (e.g., by drilling holes for gauges). If the fire is large enough, one can install a calorimeter next to the test item. If it is not large enough, multiple tests may be required.

A typical configuration of a calorimeter encompasses a TC on the unexposed side of a metal surface backed with insulation. See Figure 7. In this case an adiabatic boundary condition may be used. Properties (thermal conductivity and specific heat) of the metal and insulation are known as functions of temperature. Assuming the thicknesses are accurately known, and the boundary conditions are well characterized, one can use an inverse heat conduction code to estimate the net flux to the exposed surface. For rugged construction one normally uses a 63 mil diameter MIMS TC (usually Type K, chromel-alumel) mounted on the unexposed side of a 63 mil thick or thicker

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inconel or 304 SS plate. Figueroa, et al., (ref. [3]) evaluated the uncertainty of using this method and found it to be $\pm 15\text{-}19\%$ with 95% confidence.

If improved accuracy is desired, a TC can be installed on the back side of the insulation layer. This provides a different boundary condition that can be used to further estimate the flux. In ref. [3], Appendix C, it was concluded that the difference between an assumed adiabatic BC (1 TC) and a prescribed temperature BC (i.e., a TC on the back side of the insulation plus the TC on the unexposed metal surface) was at most 4-6%. Therefore, the prescribed temperature BC from the back side of the insulation improved the accuracy by 4-6%. But this only applied to a well insulated inner surface. In other cases where only a limited amount of insulation is available, it is of benefit to mount the additional TC.

Benefits of the calorimeter method include low cost, rugged design, ease of fabrication, and no calibration required. But to reduce the data one needs an inverse heat conduction program (e.g., SODDIT or IHCP1D). Note that IHCP1D can only perform inverse heat conduction calculations to estimate the net heat flux. SODDIT (Sandia One-Dimensional Direct and Inverse Thermal) can perform both inverse and direct calculations. This feature is valuable because the results of the two methods can be estimated and the results compared.

Figure 7: Cross-Section Schematic of Calorimeter

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1.3.2 Directional Flame Thermometer (DFT)

A variant of the calorimeter is called the “directional flame thermometer” or DFT. Originally developed in the U.K., (refs. [17] and [18]) they have been adopted for use in the U.S. By using a flat plate and allowing the plate to equilibrate with the fire, one can obtain an estimation of the “effective flame temperature.” The same temperature measurement can be used with an inverse heat conduction program to estimate the net heat flux to the surface. Keltner (ref. [19]) has recommended use of DFTs in some ASTM standards.

A variant of the DFT is the plate thermometer (PT)(Ulf Wickstrom, SP in Sweden) which attempts to measure the effective flame temperature. However, heat flux is not normally calculated when using the PT. Mr. Wickstrom believes the effective flame temperature is a sufficient measure of the fire.

Construction of the DFT is basically the same as the HFG and calorimeter, but doesn't have the HFG pitfalls. See Figure 8. The plate is nominally 4-3/4" square, 1/16" or 1/8" thick Inconel 600 sensing plates. TCs are 1/16" (63 mil) diameter Inconel 600 sheathed, Type K strapped in place with nickel or nichrome straps (0.003-0.005" thick x 0.25-0.38" wide). The TC is mounted on the unexposed side and backed by ~1" thick ceramic fiber insulation. Another plate and TC is attached on the other side of the insulation. This arrangement allows one to estimate the heat transfer through the insulation. The insulation is good but not a perfect material, so the adiabatic assumption adds additional uncertainty when performing data reduction.

Figure 8: Cross-section View of DFT

The thicker plate is used in applications where the dynamics of the fire is slow, and the opposite is true when the 1/16" thick plate is used.

It is important to obtain thermal properties for both the Inconel plate and insulation as functions of temperature. Heat flux uncertainties are much larger when constant properties are used (ref. [3]).

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DFTs are best used for experiments with plenty of space because of their size. They are relatively inexpensive to fabricate and field, and data reduction is relatively easy using one of the available inverse conduction codes (ref. [1] and [2]).

1.4 Other Methods

1.4.1 Transpiration Radiometers

Transpiration radiometers use a gas flow to “blow off” the boundary layer on the (porous) gage surface (ref. [20]). As a result, the only flux measured is the radiative component. Calibrations are performed under controlled flow rates, but these are hard to duplicate in large windy fires. The transpiration rate may not be sufficient to overcome the wind, so the gage calibration may be suspect. In a dynamic fire environment (i.e., where the wind speed varies) it may be required to vary the transpiration rate, which would be very difficult because one does not know how the wind would vary with time. In addition, since the gage is water-cooled, the surface is susceptible to thermophoresis where soot from the fire is deposited onto the gage. We successfully fielded this type of gage in hydrocarbon fuel fires, but they can be delicate and are not commercially available so are expensive to fabricate. The porous surface can become fouled if the soot loading is high. However, they are an option if radiation only heat flux measurements are desired, and radiometers are not suitable.

1.4.2 Windowed Radiometers

Windowed radiometers sense only radiative heat transfer because the window reduces the convective heat flux from the sensing surface. Windowed radiometers are fabricated such that the sensing surface and window form an enclosure which makes convection negligible. A typical window material is sapphire. Problems we have had with these gauges include soot fouling of the window, cracking and discoloration of the window, and less than a 180° field of view (e.g., 120°). If one uses the gage in an application different from how the gage was calibrated the sensitivity may not be the same. However, if the window can be kept clean this type of gage is good for radiative heat flux measurements. These gauges are commercially available from several manufacturers.

1.4.3 Transverse Seebeck Coefficient Gauges

Diller (ref. [21]) has begun development (with Sandia National Laboratories) of a new type of heat flux gage called the “Transverse Seebeck Coefficient Heat Flux Gage” (TSCHFG). Although the temperature difference is developed parallel with the heat flow, the voltage output is developed transversely (perpendicular to the heat flow). See Figure 9. Therefore, if one needs more output, one just adds more elements to the gage making it wider, not thicker. (This is different from the S-B type gage in which the temperature difference and voltage output are both developed parallel to the heat flux through a thin wafer.) The individual elements can be made from many metals, including chromel and alumel. As a result operating temperatures are typical of those for chromel-alumel thermocouples (e.g., 1200°C).

Initial evaluation of the first version of the gage is provided in Appendix 2. Advantages of this gage are 1) no requirements for water cooling, 2) can withstand temperatures over 1000°C, can be mounted on the surface of the test item so the surface is not perturbed appreciably, and the gage design is simple. Cons include a difficult calibration which depends on the adhesive used, the

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gage has to be electrically isolated from the surface, and the gage can disturb convective flow over the surface to which it is mounted. Development of this novel gage design continues.

Side View of Transverse Seebeck
Coefficient Heat Flux Gage

Figure 9: Side View Schematic of Transverse Seebeck Coefficient Heat Flux
Gage

1.5 Heat Flux Measurement Methods in Propellant Fires

Measuring heat flux in propellant fires using non-optical devices is very difficult. Temperatures at or exceeding 2500°C are common, so many metals will melt. Gauges based on thermocouples or typical sensors do not survive very long. Optical methods are the preferred methods and (for example) include “2-color” optical pyrometers and spectrometers.

However, we have used calorimeters of very large mass and thickness that allow measurements for relatively short tests. For example, Figure 10 shows a “pipe calorimeter” which consisted of a 7/8” thick x 4 ft long 304 SS tube fitted with TCs on the inside surface and was packed with insulation. The propellant was placed below the horizontally mounted cylinder. An inverse heat conduction analysis was used with the thermocouple data and material properties to estimate the net heat flux absorbed by the pipe calorimeter. The flux from propellant fires has a very large component from both convective flux and material deposition, so trying to estimate the flux from radiation alone is difficult. Nonetheless, if the test is relatively short, and the pipe calorimeter temperature doesn’t exceed the melt temperature of 304 SS (~1400-1450°C), then the inverse heat conduction method with a thermally massive calorimeter will give heat flux estimates.

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Figure 10: Pipe Calorimeter for Heat Flux Measurements in Propellant Fires

Summary & Conclusions

Summary

Table 1 provides a summary of the measurement methods, when the methods are best used, and pros and cons of their application. This table may be used to help guide selection of the heat flux measurement method for the application of interest.

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Table 1: Summary of Heat Flux Measurement Methods and Applications

Method name	Description	Data analysis and reduction	Pros	Cons	Comments
Total heat flux gage. Commercially available	Schmidt-Boelter gage (thermopile type)	Manufacturer provides sensitivity coefficient based on radiative calibration	Relatively easy to use; data reduction is simple, good for radiation dominated heat flux measurements, relatively small (~1" diameter)	Prone to soot deposition when using water-cooled gauges, sometimes fail in high intensity fires.	Needs water cooling for our applications. Manufacturer's supplied sensitivity coefficient has a higher uncertainty for convective heat flux applications.
Total heat flux gage. Commercially available	Gardon gage (circular foil type)	Manufacturer provides sensitivity coefficient based on radiative calibration	Relatively easy to use; data reduction is simple, good for radiation dominated heat flux measurements, relatively small (~1" diameter)	Prone to soot deposition when using water-cooled gauges, sometimes fail in high intensity fires.	Should not be used for heat flux measurements with non-negligible convective component, especially shear flows. Needs water cooling for our applications.
Radiometer. Commercially available, measures only radiative heat flux	Schmidt-Boelter type sensor with a window	Manufacturer provides sensitivity coefficient based on radiative calibration	Relatively easy to use; data reduction is simple, for radiation heat flux measurements only, relatively small (~1" diameter)	Sometimes window becomes discolored after exposure and cracks.	Good for obtaining the radiative component only of the heat flux. Needs water cooling.

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Method name	Description	Data analysis and reduction	Pros	Cons	Comments
Coaxial thermocouples; commercially available	Small diameter coaxial thermocouple to measure surface temperature	Energy balance on front surface with thermal model of coaxial TC allows one to infer heat flux from front surface temperature	Small, does not require water cooling, easy to use, measures both radiative and convective heat flux with no assumptions about the sensitivity coefficient, does not require a calibration	Sliver junctions are fragile, best used with materials the same as the parent surface, increased uncertainty with non-metallic surfaces, requires you are able to drill a hole in the surface	Can purchase plated junction to overcome sliver junction. Data reduction method requires model of coaxial TC.
Sandia HFGs, specially built	TC on unexposed side of plate, use energy balance method to infer heat flux	Energy balance on front surface with thermal model of HFG includes energy storage in the plate and insulation	Relatively cheap to build (cheaper than typical S-B or Gardon gauges), Excel macro data reduction program allows for uncertainty bars, batch processing of multiple gauges, and compares well with inverse heat conduction results.	Sensing plate is too thin for use with reliable TCs. Mounting hardware generates too much 2-d effects.	Can probably re-design for better response and reduced 2-d effects.
DFTs, specially built	TC on unexposed side of 2 plates; insulation between plates	Use thermal properties of insulation and plates with inverse heat conduction program to infer net flux into surface.	Relatively cheap to build (cheaper than typical S-B or Gardon gauges), compares well with energy balance method, provides net flux from both radiative and convective sources	Relatively large, data reduction one-at-a-time, not a unique solution.	Small 2-d effects for 180° field of view,
Calorimeters, specially built	TC on unexposed side of plate backed	Use thermal properties of	Relatively cheap to build (cheaper than typical S-B or	Data reduction one-at-a-time, not a	Can make same size/shape as test unit.

Heat Flux Measurement Methods in Fires

Method name	Description	Data analysis and reduction	Pros	Cons	Comments
	by insulation	insulation and plates with inverse heat conduction program to infer net flux into surface.	Gardon gauges), compares well with energy balance method, provides net flux from both radiative and convective sources	unique solution.	
Transpiration radiometers, specially built	Gas flow through porous surface "blows off" boundary layer so only have radiative flux	Gage calibrated using set gas flow in radiative environment	Can measure radiative heat transfer effectively	Need special calibration, porous surface can foul with soot, requires water cooling, requires hole in surface, specially built so expensive	Relatively large gage (18 cm long x 8 cm diameter)
Transverse Seebeck coefficient heat flux gage, specially built	Thin strips of thermoelements stacked on end; connected on alternating plates on top then bottom, insulation between plates	Energy balance method; calibration required to establish gage sensitivity, sensitivity depends on mounting scheme, need surface temperature	Relatively easy to build, robust at high temperatures, no cooling required	Data reduction is difficult and depends on mounting scheme; may be difficult to calibrate, hard to mount the same each time.	Gage output goes to zero if heat transfer to surface is poor. Needs further development.

Heat Flux Measurement Methods in Fires

Conclusions

- 1) Measurement of heat flux in hydrocarbon and propellant fires is difficult to accomplish with small uncertainties
- 2) Commercially available gauges work well in radiative dominated environments but have larger uncertainties (i.e., greater than $\pm 3\%$ typically provided by manufacturers) in mixed convection and radiation environments
- 3) Simple, easy to build calorimeters using either an inverse heat conduction method or an energy balance method can provide reasonable measures of heat flux in mixed heat flux environments.
- 4) There are pros and cons of most all measurement methods, so multiple methods are often employed to help reduce measurement uncertainty.
- 5) Table 1 can be used to help determine which methods may be used for applications of interest.

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Appendix 1

Comparison of Coaxial, Intrinsic and Sheathed Thermocouples for Use with Inverse Methods for Estimating Heat Flux

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1. Introduction

The measurement of heat flux continues to be an experimental challenge except for a few laboratory type experiments. This is particularly true in the fire testing arena since the measurements are typically transient and where water cooled gauges tend to have soot deposition problems. At Sandia, the fire testing community routinely uses thermocouples attached to the back face of a metal plate and an inverse heat conduction analysis [1] to infer heat flux from these measurements. Historically, sheathed thermocouples have been the preferred temperature measurement technique because the sheath shields the thermoelectric elements from moisture (both water and fuel). Even though it is well established that intrinsic junction thermocouples respond more rapidly than sheathed thermocouples, test engineers still favor sheathed over intrinsic thermocouples because of their inherent ruggedness.

Another temperature measurement technique under consideration is the coaxial thermocouple which is advertised to make a front face surface temperature measurement without the problems of either an intrinsic or sheathed thermocouple attached to the exposed front face. The coaxial thermocouple (as fabricated by MedTherm Corp., Huntsville, AL) consists of a 0.010 inch diameter constantan rod surrounded by 0.0625 inch outside diameter chromel sleeve. A 0.0005 in thick layer of MgO insulation electrically isolates the two thermoelectric elements. A "sliver" junction is made by abrading the exposed tip. When mounted in an electrically conductive wall, the coaxial thermocouple must be electrically isolated from the wall. A front face temperature measurement is made instead of a back face measurement with the sheathed or intrinsic thermocouples. When fast response is required, the coaxial thermocouple is superior to a back face temperature measurement. However, the disadvantage is that the presence of the coaxial thermocouple alters the temperature one is trying to measure. By judicious choice of materials (chromel/constantan thermocouple in a stainless steel plate), this impact can be minimized.

The purpose of this study is to compare the three temperature measurement techniques (coaxial, intrinsic and sheathed) for the same application and use their measurements to estimate the corresponding heat flux.

2. Description of Test Article

The test article was a 5 inch diameter by 3/8 inch thick 304 stainless steel plate. Figure 11 shows the twelve thermocouple locations for the test article. The nominal spacing between adjacent sensors of the same type was 1 inch and the sensors were symmetrically located about the plate center. Figure 12 shows an overall view of the back face of the instrumented test article.

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Figure13 is a photograph of the test article showing a close-up (bottom right) of a 20 mil diameter sheathed thermocouple. Nichrome foil strips (0.003 in thick x 0.25 in wide) are used to tack weld the thermocouple sheath to the plate. In the bottom left of the photograph is a coaxial thermocouple exiting from the back face of the test article. Figure 14 shows intrinsic thermocouples (left) attached to the back face of the test article. They have the characteristic "grasshopper" look for stress relief. These thermocouples have braided insulation. Again, flat metal strips are used to tack weld the lead wire to the test article. All surfaces of the test article were painted with black "barbeque grill" paint.

Figure 11: Sketch of 5 in diameter test article showing instrumentation locations

Figure 12: View of back face of fully instrumented test article showing all twelve sensors

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Figure 13: Photograph showing 20 mil diameter sheathed thermocouple (lower right) and back side of coaxial thermocouples (bottom).

Figure 14: Close up photograph showing coaxial (bottom) and (horizontal) row of intrinsic junction thermocouples

3. Radiant Heating Source

The test article was heated by a radiant heat source, named "pen light", which consists of a can (called a shroud) with the bottom partially open and the top fully closed. The outside dimensions of the shroud are 20.5 in diameter by 32 in tall. It was fabricated from 1/16 in thick Inconel. The top and sides of the can are heated from the exterior with radiant lamps. The test article is placed in the (partial) opening of the bottom end of the can. The bottom end of the can was not heated from the outside but was heated by irradiation from the interior surface of the curved side wall and interior surface of the top of the can. The idea was that the thermal radiation impinging on the test article would be close to black because of multiple interior reflections. The interior and exterior surfaces of the shroud were painted with Pyromark™ paint. For this test, the shroud was pointed down and the test article was facing up.

An active control system was used to produce the desired shroud temperature-time profile. The control system was set for three levels of 300, 600 and 900 °C. The pen light facility was instrumented with 39 thermocouples on the exterior of the shroud. Figure 15 shows the shroud temperature results for test 3. The 300, 600 and 900 °C plateaus for the control thermocouple are clearly visible. The control system has a characteristic overshoot for the 300 °C plateau. The sensors with limited response are located at the lowest level of the shroud, below the lamps. While the controller does a good job of maintaining the desired temperature-time profile for the control thermocouple, the remaining surfaces of the shroud are free to be at a different temperature. This is obvious from the data shown in Figure 5. The bottom of the shroud was covered with 1 in Duraboard insulation and the plate was held in place with a jack stand.

Figure 15: Shroud temperatures for run #3, 01/27/05

4. Test Article Temperature Response and Repeatability/Consistency

Three tests were run in order to ascertain the repeatability/consistency of the results. All twelve plate sensors for each of the three runs are shown in Figure 16. For test 1, one coaxial sensor was erratic (eventually recovering) but the remaining 11 sensors were grouped closely together. Test 2 did not demonstrate the same degree of consistency among the twelve sensors. A post test inspection of the front face of the test article showed significant peeling of the barbecue grill paint; this explains the greater sensor-to-sensor variability. The front face of the test article was repainted and test 3 was performed with a consistency similar to that of run 1. Only heat flux results from test 1 will be presented as the insulation back face temperature was not measured on test 1.

Figure 16: Plate temperature measurements for runs 1-3. Each run contained 12 plate sensors

5. Estimation of Heat Flux from Temperature Measurements

The test data reduction procedure will be different, depending on whether the front or back face temperature measurements were used. When the test article front face temperature data was used, then the data reduction procedure is not a true inverse calculation in the classical sense. Instead, it is treated as a direct problem and the heat flux is computed by a post processing of the

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energy balance equation for the front face node. When the back face test article temperature measurement is used, then the heat flux calculation is truly an inverse calculation and the sequential function specification method discussed in Beck, Blackwell, and St. Clair, Jr. [1] was used.

The results for all twelve test article temperature sensors along with the back face insulation temperature are shown in Figure 17. There is very close agreement among all the plate sensors. In the ideal world, the coaxial sensors would indicate higher temperatures during the heating phase with the temperature difference across the plate being proportional to the heat flux. At the time corresponding to maximum temperature, the variation about the mean is about $\pm 5^\circ\text{C}$. Coaxial sensors 3 and 4 do indicate the maximum temperature of all sensors but coaxial sensor 1 indicates the minimum temperature at about $t = 1570\text{ s}$. It appears as if the absolute accuracy of all the sensors is inadequate to compute a front-to-back temperature difference from the experimental data. Also, it is felt that the data does not have adequate accuracy to ascertain variations in heat flux across the face of the test article.

Figure 17: Temperature of all 12 test article sensors along with insulation back face temperature.

As indicated previously, the data from the coaxial thermocouples along with insulation back face temperature are sufficient boundary condition information for a well posed 1-D thermal

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model. The heat flux can be calculated from the computed temperature field by using the energy balance equation for the first node in the thermal model; see equation (5) in the Appendix. The results of this flux calculation are shown in Figure 18, along with the corresponding front face temperatures. The individual sensors are not identified because it was felt that the sensor differences were not statistically significant. The temperature sensors were sampled at 1 s intervals but when the thermal model was exercised with a 1 s time step, an artificial periodicity was introduced into the heat flux calculations because the temperature was recorded only to 0.1°C resolution. Since the sensor response was less than 0.1°C/s for some portions of the test, there were apparent jumps in temperature which in turn caused even larger apparent jumps in heat flux. In order to circumvent this problem, all the data presented here were reduced with a 4 s time step. During the time frame for which the shroud control temperature was 300°C , the calculated heat flux appeared to be roughly constant. This was not the case for the 600°C and 900°C shroud control temperature regimes. This can be explained on theoretical grounds and will be presented in a subsequent section.

Figure 18: Heat flux results for coaxial sensors 1-4

From the computational model, one can compute the temperature drop across the test article; this calculation is more accurate than using the experimental data directly. These temperature drop results and the corresponding heat flux are shown in Figure 19 for coaxial sensor 1. As one would have expected, there is a strong correlation between the applied heat flux and the temperature drop across the plate. Even though the physics is more complicated than a simple proportionality between heat flux and temperature drop, it is not a bad first cut.

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The heat flux results for the four coaxial sensors were processed using standard statistical techniques to obtain the mean and standard deviation. The mean temperature and $\pm 2\sigma$ limits are shown Figure 20. At the time corresponding to peak heat flux, we have

$$\frac{2\sigma_q}{q} \approx 0.04. \quad (1)$$

It is not claimed that the heat flux was estimated to $\pm 4\%$ as there are many other sources of errors not accounted for.

Figure 19: Temperature drop across the test article as obtained from the numerical model.

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Figure 20: Average heat flux for four coaxial sensors and $\pm 2\sigma$ limits

The intrinsic and 20 mil sheathed thermocouple data were reduced using the sequential function specification method for inverse heat conduction problems as described by Beck, Blackwell and St. Clair [1]. A time step of 4 s was used to be consistent with the data reduction technique for the coaxial sensors. A parameter in the inverse technique is the number of *future times*. This parameter indicates how far into the future the equations are integrated while temporarily assuming the heat flux is constant. This constant heat flux value is adjusted to minimize the least square error between the experiment and model. The heat flux resulting from this process is recorded at that time and the problem time is incremented by one time step. This process is repeated in a sequential fashion over the entire computational time domain. This parameter serves to reduce the sensitivity of the computed heat flux to errors in the measured temperature. Perturbation in front face temperature measurements are associated with perturbations in heat flux. Perturbations in back face temperature are associated with *even larger* perturbations in heat flux. The number of future times chosen for the data reduction was 3. The intrinsic and 20 mil sheathed thermocouples were each reduced as a group. The intrinsic results will be presented next.

Figure 21 presents the estimated heat flux from the four intrinsic junction thermocouples. The character of these results is very similar to those for the coaxial sensors.

The data reduction for the 20 mil sheathed thermocouples was similar to that for the intrinsic junction thermocouples. Again, a time step of 4 s and three future times were used. The results are shown in Figure 22 and they are very similar to the results for the other two sensor types. The average and standard deviation of the results from the four 20 mil sheathed

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thermocouples and four intrinsic thermocouples are shown in Figure 23. At the time of peak flux, $\sigma_q / \bar{q} \approx 0.022$, which is smaller than the corresponding value for the coaxial sensors.

The heat flux for all 12 sensors can be combined into a single plot and these results are shown in Figure 24. These results show very good consistency.

6. Modeling of the Test Article Response

If there was only experimental data, there is no way to know if all the relevant physics has been included in the data reduction equations. For example, are there significant three dimensional effects present? One could develop a 3-D model of all the heat transfer processing going on within the can (shroud) and the interaction between the shroud and the plate. However, a lot of assumptions would have to be made as to the shroud temperature variation between the measurement stations. However, a much simpler process was adopted as a first cut. It was assumed that the heat conduction in the plate/insulation was 1-D and the input radiative flux was as given by the far field radiation boundary condition

$$q = \varepsilon \sigma (T_s^4 - T_w^4) \quad (2)$$

where ε is the plate emittance (assumed to be 0.9), σ is the Stefan-Boltzmann constant, $T_s(t)$ is the shroud (control) temperature as measured experimentally and $T_w(t)$ is the front face wall temperature computed by the model. The results from this direct calculation are shown in Figure 25. The results look amazingly similar to the experimentally measured plate temperatures and the corresponding heat flux calculations.

Figure 21: Heat flux computed from each of the four intrinsic junction back face thermocouples.

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Figure 22: Heat flux estimated from 20 mil sheathed thermocouples

Figure 23: Average heat flux and standard deviation for the 8 back face temperature sensors

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Figure 24: Heat flux for all 12 temperature sensors

Figure 25: Predictions using a simple far field radiation boundary condition

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No parameter adjustment was used to produce these results. One can legitimately argue that some kind of average shroud temperature from the results of Figure 5 would have been more appropriate; the *proper* average to use is open to discussion. A somewhat different average shroud temperature would likely move the curves up or down slightly but without altering their basic shape.

Equation 2 can be used to explain why the heat flux was constant during the 300 °C temperature plateau but not during the rest of the test. At the lower temperatures, the re-radiation from the exposed plate face is sufficiently small that it can be neglected; hence, constant shroud temperature means constant flux. However, for the higher temperatures, re-radiation is more significant and can not be ignored. Since the plate temperature is increasing with time, during the 600 and 900 C temperature plateaus, a constant shroud temperature produces a flux that is decreasing with time as shown by all the experimental results.

The presence of a model opens many possibilities for design and optimization calculations. For example, if a certain flux vs. time was desired, then the model could be used to determine the shroud temperature variation with time to achieve this desired goal. Mathematically, this problem would be posed as follows:

$$\text{Find } T_s(t) \text{ such that} \quad (3)$$

$$T_s(t) \min E = \int_0^t [q_m(t) - q_d(t)]^2 dt \quad (4)$$

where $q_d(t)$ is the desired or design time dependent heat flux variation and $q_m(t)$ is the heat flux computed from the model driven with the shroud temperature variation $T_s(t)$.

7. Summary

Three different experimental techniques have been investigated for making temperature measurements useful for estimating heat flux: coaxial thermocouple, 20 mil diameter sheathed thermocouples and intrinsic junction thermocouples. A plate was instrumented with four each of the three different type sensors and exposed to a radiative heat flux environment. For resolving heat flux on a time scale of 4 s, all three techniques performed equally well. If faster response is needed, then it is anticipated the coaxial thermocouple would have the fastest response followed by the intrinsic junction and then the 20 mil sheathed thermocouple.

A simple far field radiation model was developed and it appears to capture the major physics involved. This model offers the potential for being useful for design/optimization studies in which a shroud temperature variation is desired to produce a specified heat flux as a function of time.

8. Acknowledgements

Jim Nakos (1532) directed the experimental program, Bennie Belone (1532) instrumented the test article and Chuck Hanks (1532) programmed the data acquisition system. Support was provided by the W76-1 Qualification Program and the Campaign 6, Weapon System Engineering Certification Program.

9. Appendix 1 References

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10. Appendix

The thermal properties of the 304 stainless steel and the cerablanket insulation are include for completeness. See Figures 26 and 27.

The finite difference form of the energy balance on the exposed surface node is given by

$$\frac{kA(T_1^{n+1} - T_2^{n+1})}{\Delta x_1} - qA + \rho C \frac{\Delta x_1 A}{2} \frac{(T_1^{n+1} - T_1^n)}{\Delta t} = 0 \quad (5)$$

where q is the applied heat flux. This result was used to compute the time dependent heat flux for the coaxial thermocouples.

Figure 26: Thermal properties of the 304 stainless steel used in the analysis

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Figure 27: Thermal properties of the cerablanket used in the analysis

Appendix 2

Data Analysis of Transverse Seebeck Coefficient Heat Flux Gage

Objective

The objective of this appendix is to discuss the results and data reduction procedures for a new type of heat flux gage. Prof. Tom Diller from Virginia Tech (VT) University developed the gage. The VT flux gage design is based on the “transverse Seebeck coefficient”, hereafter called the “TSC-HFG.” Three types of techniques were used to make the same heat flux measurement, therefore results could be compared. Two tests were performed on 8/17/07. Data from only the second test will be presented because part of the setup needed to be cured during the first test.

Test setup

The set setup consisted of a 6-sided radiant heat array, an 18” diameter x 48” tall shroud, a flat plate, and other misc. equipment. The flat plate contained instruments intended to measure the heat flux to the plate from 3 different sets of data:

- 1) a Schmitt-Boelter type gage, Medtherm Model 64, measuring total heat flux (calibrated to incident flux),
- 2) dual TSC-HFGs mounted on top of the plate, and
- 3) dual thermocouples on the back side of the plate; 2 methods were used to estimate the flux from the TCs:
  - a. SNL-HFG data reduction based on energy balance,
  - b. Inverse heat conduction method using the code called “IHCP1D.”

The 3 measurement locations were spaced about 120° apart on a 3” radius from the plate center. The gauges were spaced apart so one would not affect the other, and far enough off center (3”) to reduce any interactions. They were all located at 3” because it was assumed that the flux to the plate would be azimuthally uniform (i.e., the flux would be the same at same radius). Figure 28 shows the plate installed at the base of the lamp array, before the shroud was put in place. The plate is not oxidized in the figure, but after test #1 it was oxidized. Emissivity measurements on the oxidized plate will be provided later. Figure 29 shows a view farther away from the plate showing the bottom of the lamp panels. In Figure 29 only 4 lamp panels are shown; the remaining 2 panels rotate into place.

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Figure 28: Photograph of Plate Mounted Inside Heater Array

Figure 29: Photograph of Test Setup

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Flux Measurement Methods

(a) *Schmidt- Boelter, Water-Cooled Gage*

Output from the S-B gage was calibrated to incident radiative heat flux by the manufacturer (Medtherm, Corp.). It was mounted flush with the plate surface so it measured the incident flux to the plate surface. It was insulated from the plate to reduce any potential cooling effect on the plate caused by active water cooling of the S-B gage. Because the gage was water cooled, the gage temperature was approximately 20°C. It is assumed that the air temperature near the gage may be approximated by the shroud temperature near the bottom of the shroud.⁵ The closest shroud measurement to the bottom was at 2". The shroud temperature was raised in 3 steps to 600°, 800°, and 1000°C. Therefore, with a temperature difference between the gage and shroud as large as 580°C, 780°C, and 980°C, there could be non-negligible convective input to the S-B gage as well as radiative input.

Manufacturer's literature for S-B type gauges indicates the accuracy is $\sim \pm 3\%$. Strictly speaking, this only applies for the calibration which is performed in a radiative only environment. When used in real applications with non-negligible convection, the overall uncertainty can rise significantly because of several factors. The first factor is that similar to Gardon type gauges, it is believed that the sensitivity of S-B type gauges in a convective environment is not the same as for a radiative only environment, and depends on the convective heat transfer coefficient, h . For Gardon gauges, the reason is believed to be due to non-parabolic temperature profiles across the gage sensing element in convective environments (especially for shear flows). In a radiative only environment the temperature profile on the Gardon gage sensing element is parabolic.

It is not clear why the sensitivity coefficient is different in convection than radiation for S-B type gauges, but it may be due to different sensing element temperature profiles under convection and radiation. Diller, et al. has performed a large series of tests which confirms this effect with S-B type gauges (ref. [8]). This is especially true in shear flows. Another reason is that convective heat transfer coefficients are typically not known very accurately (e.g., $\pm 25\%$), so corrections based on a heat transfer coefficient are also uncertain. These factors combine to raise the uncertainty of S-B type gauges in fire environments to as high as $\pm 30\%$ (see ref. [1]).

Results from the FORUM round robin calibration (ref. [2]) showed the uncertainties of S-B gauges (similar to the one used here) to be $\sim \pm 8-14\%$. It will be assumed that the larger value from the FORUM report is appropriate in this work.

In these tests, there was no forced convection, and free convection was minimized by facing the gage upward. However, based on correlations for a flat disc facing upwards, assuming the gage temperature is 20°C and the free stream temperature is approximately the shroud temperature, the convective heat transfer coefficient at 600°C shroud temperature is about 4.8 W/m²-K, and at 1000°C shroud temperature h is about 5.1 W/m²-K. Using a 580°C temperature difference for the 600°C shroud temperature, the convective contribution is about 2.8 kW/m². For the 1000°C shroud case the convective contribution is about 5.0 kW/m². For now it will be assumed that the S-

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⁵ It is not known how good this assumption is, but it is difficult to measure the air temperature inside this setup without actually withdrawing a sample and measuring that temperature.

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B gage sensitivity for convection is close enough to that for radiation that it can be used for either (mainly because no other data are yet available).

(b) Flux Estimation Using Plate Temperature

i. Energy Balance

The second method consisted of mounting one or more thermocouples on the back side of a thin plate with known properties. Stainless steel is most often used but none was available for this test so a 62 mil thick nickel plate was substituted. Plate diameter was about 16". The temperature response of the plate, along with the assumption of 1-dimensional conduction and an insulated back surface allows one to use two methods to estimate the net flux to the surface from the TC measurements. Data reduction can be performed using either the Excel macro developed for the Sandia Heat Flux Gage (SNL-HFG) or by use of an inverse heat conduction code (e.g., IHCP1D).⁶ The data reduction scheme was documented in ref. [3].

The SNL-HFG method provides an estimate of incident heat flux using a model based on the energy balance on the front surface. Because the nickel plate was thin (62 mils) it was assumed that the front and back face temperatures were almost equal. The assumptions used in the macro were specifically tailored for the SNL-HFG design so have additional uncertainty when used for the nickel plate. However, they do provide an independent and reasonable estimate of flux from the TC measurements that can be compared with flux estimated from the other methods.

ii. Inverse Heat Conduction

IHCP1D provides a measure of the net flux, which has to be added to an estimate of the emitted flux to obtain the incident flux. Net flux estimates from an inverse method are not unique, and depend on a number of different variables (e.g., the number of future times). Although a powerful and useful method, when one estimates incident heat flux using an inverse methods uncertainties of $\sim \pm 15\text{-}20\%$ are common. (ref. [4]).

IHCP1D uses the backside temperature and plate properties, along with the assumption of 1-dimensional conduction and an insulated backside, to estimate the net flux to the sensing surface. The net flux includes all modes of heat transfer (e.g., radiation and convection).

(c) Transverse Seebeck Coefficient Heat Flux Gage

The dual TSC-HFGs were mounted on top of the heated side of the plate surface. They were bonded in place with Cotronics alumina adhesive (#989). Adhesive covered both the bottom and sides of the gauges. This adhesive was chosen for several reasons. First, a high temperature adhesive was required to hold the gage in place up to about 1000°C. The #989 adhesive is rated up to 1650°C. Second, the adhesive was an electrical insulator for the gage. Third, we wanted to try to limit heat transfer to-from the gage from the sides, which would introduce 2-dimensional

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⁶ IHCP1D = Inverse heat conduction program, 1-dimensional, by Beck Engineering.

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effects. Less adhesive was used on VT#2 than VT#1 to limit potential perturbations due to the gage. As it turned out it was best to use more adhesive for a more secure attachment.

One TSC-HFG (VT#1) had the original large diameter cabling (2 cables @ ~ 1/8" diameter) while the other gage, (VT#2, which had to be repaired), was fitted with much smaller (2 @ ~1/16" diameter) flexible cable (chromel-alumel wire, Nextel insulation, inconel sheathing over-braid).

Both TSC-HFGs were about 5/8 in long x 9/16 in wide x 3/16 in thick. In this type of gage the temperature difference is generated in the same direction as the incoming flux (vertical in this case), same as other types of flux gauges, but the output voltage is generated horizontally. The TSC-HFG was fabricated from multiple layers of chromel and alumel sheets (stacked vertically) welded together at alternating top and bottom surfaces. Gage output can be increased by adding additional chromel-alumel layers. TSC-HFGs are surface mounted and have no active cooling. They do require a gage temperature measurement as well as a gage output measurement to infer incident heat flux. Ref. [5] provides additional information about the transverse Seebeck coefficient gage concept. As of this data there are no estimates of the temperature uniformity on the TSC-HFGs.

All 3 methods were used to compare the flux obtained from the TSC-HFGs. The gauges indicate different fluxes so one or the other is really not the "true" heat flux. For consistency, the S-B gage is used as the point of comparison.

Shroud Temperatures

The shroud, an 18" diameter x 48" long x 1/16" thick inconel cylinder, acted as the heat source. Banks of 6 kW lamps arranged in a hexagonal configuration surrounded the shroud. The sensors were mounted in the plate, which was placed at the bottom end of the shroud. Gauges faced upward and "saw" the inside of the shroud. Shroud temperatures were measured in a number of axial and circumferential locations. Shroud temperature at the control points about 24" from the bottom was ramped from ambient to 600°C, held there for 10 minutes, then increased to 800°C, held there for 10 minutes, then increased to 1000°C, then held there for 10 minutes. Ramp rates were the same for all three ramps, ~ 200°C/minute. Figure 30 shows the shroud temperatures as well as the plate temperatures at the 270° orientation. As one might expect the plate lags behind the shroud and does not reach the highest shroud temperature. The two plate temperatures are almost the same (it is difficult to distinguish them).

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VT Transverse Seebeck Coefficient Flux Gage Test #2 (8/17/07): Shroud & Plate TCs

Figure 30: Shroud and Plate Temperatures, Test #2

Temperatures near the very top (Shroud 0 deg 46") are much lower than others because the lamp array is only ~46" tall.

Using equation {1} one can estimate the flux at the plate from the shroud temperature:

$$q_{inc} = \varepsilon * \sigma * F * T^4 \quad \{1\}$$

where ε is the shroud emissivity (~0.85), σ is the Stefan-Boltzmann constant ($5.67E-11 \text{ kW/m}^2\text{-K}^4$), F is the view factor, and T is the nominal shroud temperature (600°C , 800°C , and 1000°C). No attempt was made to use the shroud temperature profile; only a constant temperature from the control location was used. The view factor from an end plate to the shroud is about $F=0.97$. At the 3 nominal shroud temperatures the incident flux is estimated to be about 27 kW/m^2 for a 600°C shroud temperature, about 62 kW/m^2 for the 800°C shroud temperature, and about 123 kW/m^2 for the 1000°C shroud temperature. These values will be compared with measurements.

Data Reduction for the S-B Gage

This gage was calibrated to incident radiative heat flux by the manufacturer. A sensitivity coefficient was provided for this gage (S/N #149489) and was $\sim 4.184 \text{ kW/m}^2/\text{mv}$. When multiplied

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by the gage output the data are converted directly to flux. Figure 31 shows incident heat flux results from the S-B gage for test #2.

Figure 31: S-B Gage Incident Heat Flux Profiles

Average values for each of the 3 plateaus are shown on the figure; they are ~30, 70, and 143 kW/m². These will be used to compare with other methods. Convection was reduced by orienting the gage upward, which would generate the least convective transfer of the 3 orientations possible (vertical facing up, vertical facing down, and horizontal). But the total flux shown does contain both radiative and convective contributions. Note that all 3 plateau averages are greater than the flux estimates from nominal shroud temperatures, indicating the average shroud temperatures may be higher or the convective contribution greater. For example, if at the 1000°C plateau the temperature was 5% higher (1050°C), then the flux would be ~143 kW/m², which agrees with the S-B gage output.

Using correlations for a cooled disc facing upward and assuming the air temperature was equal to the shroud temperature, estimated convective flux is 2.8 kW /m² at 600°C (~9% of total) and 5.0 kW /m² at 1000°C (~3.5% of total). Because the S-B gage was water cooled, it absorbed convective energy from the surrounding air, more than to an uncooled plate. Any gage, including the S-B gage, responds to all heat transfer modes absorbed into the sensing element. Therefore, one might expect that the S-B gage would measure a higher incident flux than any of the other methods. This is the case.

Data Reduction for Cold Side TCs

(a) SNL-HFG Data Reduction Macro

An Excel macro was developed for data reduction for the Sandia-HFG (ref. [3]). The basis for the macro was an energy balance on the front or hot surface of a Sandia-HFG. Details of the construction of the Sandia-HFG were incorporated into the macro. The sensing plate was a 10 mil thick 304 stainless steel plate painted with high emissivity Pyromark™ black paint. A 1/16" diameter Type K (chromel-alumel) was attached to the back (or cold) side. Insulation (Fiberfrax blanket, 8 lbs/ft³) touched the backside of the plate. The macro has been modified to use the same basic data reduction scheme but for different plate thicknesses and insulation thicknesses. Temperature varying material properties were used wherever possible. In this manner it could be used for any number of different "calorimeter" designs. The macro uses two inputs: sensing plate thickness and insulation thickness. Output is an estimate of incident heat flux which includes re-radiation from the sensing element and a convective heat flux contribution. Figure 32 shows the results of the macro output. Both the 40 mil and 62 mil diameter TCs were used to reduce the data – results are essentially identical.

Average values are also shown on Figure 32. Comparing these values to the averages shown on Figure 31 it is seen that the Sandia-HFG fluxes are all lower than the S-B fluxes. Differences are greater for the lowest flux level (~40% or 12 kW/m²) and least for the highest flux (~13% or 18 kW/m²). The SNL-HFG fluxes also rose slower (have a slower response time) and gradually increase as time progresses even though the shroud temperatures are constant. This slower response is not surprising because the S-B gage has a better transient response. Note that the amount the Sandia-HFG was low compared with the S-B gage results is partially made up by the convective fluxes to the S-B gage (2.8 kW/m² for 600°C and 5.0 kW/m² at 1000°C).

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VT Transverse Seebeck Coefficient Flux Gage Test #2 (8/17/07): Incident flux estimated from SNL HFG analyzer

Figure 32: Incident Heat Flux from SNL-HFG Analyzer

(b) Inverse Heat Conduction Program (IHCP1D)

IHCP1D was used with the actual plate properties for nickel. Property variations with temperature were found in ref. [6]. The nickel plate was 62 mils thick. Posttest emissivity measurements of the plate were made with a Surface Optics reflectometer (Model ES-200) that provides an estimate of emissivity at 6 different wavebands at two different angles (20° and 60°). A single average estimate of plate emissivity for the plate and one TSC-HFG was estimated. The plate emissivity was ~0.63, higher than published values, while the TSC-HFG emissivity was ~0.76. Published values for “stably oxidized nickel” are 0.40-0.57 from ref. [6].

For the inverse method, the net flux is estimated from the cold side temperature measurement, which includes contributions from both radiation and convection.

Once the net flux was estimated using the inverse heat conduction program, the incident flux was estimated using equation {2}:

$$q_{inc} = (q_{net} / \varepsilon) + \sigma T^4 \quad \{2\}$$

In this case q_{net} includes the convective component, but a much smaller one than for the S-B gage because the plate temperature is much hotter (see Figure 30). Results for the two TCs are very close, so only data from the 40 mil TC is shown in Figure 33. They compare favorably to the

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results in Figure 32; averages being slightly higher for the IHCP1D estimated flux. But they are less than the fluxes from the S-B gage, especially during the transients.

VT Transverse Seebeck Coefficient Heat Flux GageTest #2 (8/17/07): Incident flux from IHCP1D, 40 mil TC @ 270deg

Figure 33: Flux from IHCP1D

An obvious explanation for the differences is due to the much smaller convective component. Other possible explanations for the differences are the large uncertainty in the net flux measurement (15-20%) and 2-d conduction. Radial conduction due to flux varying with radius could affect the temperature readings. Nickel has high thermal conductivity and so this could have affected the results.⁷

⁷ Radial conduction generated uncertainties of up to -35% in the NASA JPL ATJ graphite plate calorimeters. (Source: N. Keltner, F.I.R.E.S, Inc., 2007).

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Data Reduction for the TSC-HFGs

The TSC-HFGs respond to the net flux absorbed into their surfaces. Edge effects are neglected in this analysis. The “net flux” is the net of what is absorbed less what is emitted and/or convected from the surface. Equation {3} shows the relationship between the fluxes and represents an energy balance on the gage surface.

$$q_{net} = \alpha I - \varepsilon \sigma T_s^4 - h(T_s - T_\infty) \quad \{B-3\}$$

In equation {B-3} “I” is the irradiance (or “incident heat flux”), α and ε are the absorptivity and emissivity, respectively, of the TSC-HFG surface, T_s is the TSC-HFG surface temperature, ‘h’ is the convective heat transfer coefficient, and T_∞ is the free stream air temperature. Because the TSC-HFGs operate without any cooling, the convective input is much less than for the S-B gage. Convection is neglected for now.

The experiment was designed to minimize convection by orienting the gauges such that they were “looking up” at the hot surface. Convection is believed to be small in this orientation. The TSC-HFG gage temperature and shroud temperatures are shown in Figure 34. Similar to the plate temperatures in Figure 30, the TSC-HFG temperatures lag behind the shroud temperatures, but eventually rise to near the shroud temperatures at the end of the steps.

Figure 34: Shroud and TSC-HFG Temperatures

To make the calculations simple, it is assumed that the surface absorptivity and emissivity are equal, therefore, equation {3} can be re-arranged to equation {4}:

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$$I = (q_{net} / \varepsilon) + \sigma T_s^4 \quad \{4\}$$

Equation {4} is the same as equation {2}. Using an “appropriate” sensitivity coefficient for the TSC-HFG, one can estimate the net flux (q_{net}) by multiplying the gage output by the sensitivity coefficient. The TSC-HFG temperature was measured and it was assumed to be close to the surface temperature (although it is really measured on the side of the gage in 2 places) so there is an estimate of T_s . The TSC-HFG emissivity was measured after the second test, so all the required parameters in equation {4} are available.

As a starting point, Prof. Diller said a reasonable TSC-HFG sensitivity would be 1 mV per 1 W/cm². This translates to 10 kW/m²/mv. For an initial check this value was used, but realizing it may be only just “in the ballpark.”

Using the sensitivity of 10 kW/m²/mv, the net flux from the TSC-HFGs is shown in Figure 35. As one might expect, the net flux rises fast during the initial transient, then drops to zero as the gage heats up and steady state is reached. It is clear the early time flux from equation {4} is mostly from the first term, while the late time flux is from the second term.

General trends in Figure 35 show the initial peaks followed by an exponential like drop in flux. The peaks rose with shroud temperature, as expected. Gage VT#2 had separated from the plate surface, so shows reduced flux levels as compared with VT#1. Therefore, data from VT#2 is somewhat suspect.

Figure 36 shows incident flux estimated from equation {4} using the net flux from Figure 35 and the gage surface temperature from Figure 34. Near the end of the steps where the gage output is almost negligible, the incident fluxes in Figure 36 are comparable but below the S-B fluxes shown in Figure 31.

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Figure 35: Net Flux from TSC-HFGs (Test #2)

Figure 36: Incident Flux from TSC-HFGs (Test #2)

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Average fluxes were estimated towards the end of the steps, even though the flux kept slowly rising, to get a feel for how the average compared with the S-B fluxes. Fluxes were always below the S-B averages (6 kW/m² or ~21% below at the lowest level, and 13 kW/m² or ~9% below at the highest level). This is partially due to the different convective contributions to cooled and un-cooled gauges.

The initial parts of the transient are governed mostly by the gage output rather than surface temperature, since the surface temperature has not yet risen appreciably. There might also be an effect caused by different dynamic responses of the hot and cold junctions.

Therefore, the early transient response of the TSC-HFG was matched to S-B gage data. Figure 36 shows that for all 3 steps, the initial transient for the TSC-HFG is below the S-B gage output. Figure 37 shows data for a sensitivity of 20 kW/m²/mv (arbitrary change); initial peaks are more like the S-B gage output, but late time averages are still below the S-B gage.

The TSC-HFG fluxes in Figures 36 and 37 are closer to the fluxes from both the SNL-HFG analyzer and the IHCP1D analysis than they are to the S-B gage.

Figure 37: TSC-HFG Fluxes, 20 kW/m²/mv sensitivity (Test #2)

Summary & Conclusions for TSC-HFGs

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- 1) Robustness – would the new gage designs hold up under a typical radiant heat environment – the answer is yes.
- 2) Mounting scheme – how do we mount these gauges? In these tests we used an alumina based ceramic adhesive (Cotronics model 989). It worked well in one case but the second gage (VT#2) broke free of the surface. Even so, data was not too bad for VT#2. We can continue to use this adhesive but use more (VT#1 had more than VT#2).
- 3) How do we reduce the data? Since these gauges sense output based on net flux, if you don't provide a method of withdrawing the heat their output will reduce to zero. These tests used an insulated plate so we could get fluxes from the inverse program and SNL-HFG analyzer, so this was a hard test for the TSC-HFGs. We should try additional tests where the underside of the TSC-HFGs is not insulated. The data reduction scheme, once a sensitivity coefficient is determined, is easy (see equation {4}).
- 4) We need to figure out how best to determine the sensitivity coefficient for the TSC-HFG.
- 5) For our application these gauges show promise. They do not require any active cooling, are small, thin, can be installed on top of a surface, and have a relatively simple method for data reduction. Initial tests indicate they are robust. Negatives include more data reduction than just a sensitivity, more than one measurement required (gage output, gage temperature, gage emissivity), and uncertainties in each of the measured parameters.
- 6) The four methods show results that are substantially the same. The S-B gage output is higher than any of the un-cooled gauges presumably because this cooled gage accepts a much larger convective contribution than do the un-cooled gauges. All three un-cooled gage fluxes were less than the S-B gage result.
- 7) At present we are working with methods that typically give heat flux measurements in fires with uncertainties of $\sim\pm 30\%$. Realistically, if we can reduce the uncertainties to $\sim\pm 15\%$ we would be doing much better.

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