The Pennsylvania State University

The Graduate School

ADVANCED HEAT FLUX GAUGES APPLIED TO
TEST TURBINES OPERATING IN STEADY FACILITIES

A Dissertation in
Mechanical Engineering

by

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ABSTRACT

The heat flux into a component dictates its temperature distribution and therefore the durability of the part. Within turbomachinery flows, cyclic variations can cause time-varying thermal stresses that lead to premature component failure in hot, highly stressed parts such as turbine blades. Because these variations are difficult to predict, advanced instrumentation capable of experimentally measuring the mean and time-varying heat flux is critical to validate turbine cooling designs, ensuring component durability requirements. Thin film resistive temperature detector (RTD) heat flux gauges (HFG) have been used for several decades to quantify such time-resolved surface temperatures and heat flux in short-duration turbine testbeds. However, challenges facing adaptation of this technology for a continuous-duration steady facility warrant investigation. This dissertation details several of those challenges and presents solutions that enable increased sensor robustness while maintaining a high frequency response.

The preliminary study in this dissertation validates the fabrication, calibration and processing of RTD HFGs against previous correlations and sensors of a similar type fabricated at partner institutions. Utilizing the Penn State Nanofabrication Lab, the sensors fabricated within this document are composed of two thin film platinum RTDs on opposing sides of a polyimide substrate with well characterized thermal properties. Following an electrothermal calibration for the RTD as well as a thermal property determination for the substrate, a system-level validation of Penn State HFGs is presented, estimating a 10% uncertainty in calculated heat flux.

After the validation of the fabricated gauges, a succeeding study utilized these sensors within the START testbed to investigate turbine rim seal instabilities. The results from this study showcase the first time-resolved thermal measurements to quantify rim seal performance. In general, the inclusion of time-resolved thermal measurements allowed for more direct relationships between engine durability and rim-seal performance. These sensors also provided key insights into time-resolved ingestion within the cavity region.

To increase the accuracy and operability of HFGs, an additional study aimed at improving the calibration techniques of HFGs was completed. Using a sinusoidal current, a frequency domain technique was applied to measure the thermal properties of the HFG, which is a primary source of heat flux uncertainty. Then, this study leveraged that same frequency domain technique to create a relationship between the coefficient of resistance of the RTD and the thermal properties of the substrate which allowed for novel in-situ RTD calibrations. This capability provides substantial value for semi-permanent installations. More frequent calibrations lower the uncertainty in the RTD measurements and therefore the calculated heat flux.

Sensor coatings provide additional protections at the cost of sensor bandwidth. To increase the durability of these sensors, this dissertation created a theoretical framework and provided experimental validation of a coated HFG. Using previously unpublished solutions to the unsteady conduction equation, this work determined the important parameters to the design and processing of coated HFGs. Then, the theoretical framework was experimentally validated using coated HFGs. Results indicate that a frequency response of a coated gauge of 13.8 kHz was achieved. However, associated errors with the thermal and geometric properties of the coated system increase the uncertainty in the time-varying heat flux. Overall, the study illustrated that coated HFGs were more durable than their uncoated counterpart, enabling more robust measurement capabilities.
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<td>convenience variable</td>
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<tr>
<td>b</td>
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<tr>
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</table>
ACKNOWLEDGEMENTS

In many ways, science is a Sisyphean task. All one can do is hope that their contribution adds in a small way to the betterment of society, knowing this communal pursuit will always be an unfinished product. Before my specific acknowledgements, I'm obliged to thank academic mavens such as Newton and Fourier for dedicating their lives to understanding the natural world. The opportunity to recognize and appreciate a faction of their contributions has been a great blessing of my life. Equally important, I acknowledge my debt to every disregarded scientist, engineer, entrepreneur, and enterprise that has been unintentionally instrumental to my successes. My academic feats would not have been possible if not for a spectrum of tools spanning from vapor deposition chambers to duct tape. I was continually humbled by the quality and quantity of physical, computational, and human resources at my availability.

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Doctoral degrees are a sacrifice, not only for the student, but also to their family and friends. Without the love and support of my parents, siblings, and extended family, this pursuit would not have been possible. Sorry for missing so many family functions over the years. To my boys from Curtin Hall, thank you for providing me with friendship and necessary distractions from academic pursuits. Sorry for leaving State College. Lastly, to Alayna, thank you for reminding me that despite my engineering nature, there are times to be illogical. Life is a crazy adventure and I am lucky to have you as a partner. I cannot wait to start the next chapter of our lives together.
1 INTRODUCTION

Heat flux measurements are critical to the design of thermal systems because it dictates the temperature distribution within the system and therefore its durability. A number of techniques exist to quantify the heat flux in a thermal system [1]. However, flexible double-sided thin-film resistive-temperature-detector (RTD) heat flux gauges (HFGs) are the only current option for minimally intrusive measurements capable of deducing the mean and high-frequency components of heat flux in a thermal system [2]. Sensors of this type are not commercially available and relatively few institutions have the abilities to fabricate these devices. Oxford University as well as the US Air Force Research Lab (AFRL), are two research labs that have historically fabricated these HFGs for short-duration testing facilities. By forging a collaboration with their teams, the research conducted as a part of this dissertation advanced heat flux gauge construction and capabilities beyond what is available. In addition, the focus of this research was to make the necessary adaptations for this next generation of HFGs to be used in steady (non-transient) experiments. To accomplish this goal, research was conducted to: (i) advance generalized sensor fabrication with operational guidelines and (ii) apply such sensors to better understand the gas turbine heat transfer. The following section highlights the motivation for such devices in a broad sense before narrowing to specific applications within gas turbine engines.

1.1 Importance of High-Frequency Thermal Measurements

Many thermodynamic cycles convert an input of heat energy to useful work. Powerplants and engines utilizing the Brayton and Otto cycles are the backbone of electricity generation, transportation, and industrial production. Implementation of these thermodynamic cycles relies on highly-engineered components that depend on the repeated motion of constituent parts, such as turbine blades and pistons. This harmonic motion causes time-varying thermal conditions that lead to early failure of stressed hot components, especially in applications prone to instabilities [3].

The frequencies of these harmonic behaviors vary with application, but are commonly associated with the rate at which the device operates and its accompanying harmonics. In many applications, these driving frequencies can be in excess of 25kHz [4]. Core principles used in the design of such systems often rely on steady analyses, neglecting important time-varying phenomena. For that reason, costly unforeseen problems can manifest in the final stages of commissioning or after long-term duration. Accordingly, there is a need for sensors capable of capturing such interactions to quantify the time-resolved thermal histories both in the experimental validation phase and ultimately production components.
1.2 Motivation for Use in Gas Turbine Applications

Gas turbine engines power almost all commercial flights, carrying 4.5 billion passengers and $6 trillion worth of cargo in 2019 [5]. With anticipated economic demand for faster, more efficient transport through aviation comes a responsibility to grow the commerce in a sustainable fashion. In 2008, the aviation industry agreed to the world’s first set of sector-specific climate goals [6]:

1. Improve fleet efficiency by 1.5% annually,
2. Cap net carbon emissions at a 2020 level through carbon neutral growth, and
3. Reduce carbon emissions to half of 2005 emissions by the year 2050.

To date, the industry has performed well toward achieving these lofty targets. For example, absolute emissions from the US airlines have been reduced by 3% since 2000, despite an air traffic increase of 24% [6]. Achievements like this one are possible in part through increases in efficiency from the engines powering the aircraft. For instance, Pratt & Whitney’s Geared Turbo Fan™ has increased engine efficiency by 16% from the previous engine generation [7].

Gas turbine engine manufacturers have been able to accomplish recent increases in efficiency through numerous technological advancements in each component of the engine as well as the overall integration of its parts. Figure 1-1 shows the basic layout of a gas turbine engine. The specific components highlighted are the compressor, combustor, and turbine. These components work in tandem to propel the aircraft.

![Figure 1-1. Cross section of PW GP7200 [8].](image)

The flow within a gas turbine engine is inherently unsteady because components operate at different speeds with unique airfoil counts. Each row of the compressor or turbine produces a non-uniform outlet condition which propagates to downstream rows and affects both aerodynamics
and heat transfer [9]. In some cases, these variations can give rise to aerodynamic and mechanical instabilities, drastically reducing the operational life of the engine, commonly referred to as engine lifing [3]. These time-resolved behaviors can be difficult to predict because they require correct time-accurate simulations that account for the completed assembly of parts. As a result, extensive testing is necessary to certify an engine, especially the durability of the highly-stressed hot components such as turbine blades, which operate on a tight thermal margin.

There exists a compromise between the efficiency of the engine and the durability of the turbine. The efficiency of a gas turbine engine can be improved by increasing the temperature of air entering the turbine. Consequently, production engine turbine inlet temperatures ($T_{TET}$) have risen over the last several decades as shown in Figure 1-2. Around 1970, the $T_{TET}$ grew hotter than the softening temperature for the metal that forms the turbine blades. To continue facilitating increases of $T_{TET}$ and provide durability to the turbine components, advanced cooling schemes were introduced to lower the blade temperature. The cooling effectiveness of these schemes, $\phi$, shows how well the coolant reduces the operating temperature. Generally, higher $T_{TET}$ are enabled through the use of more effective cooling schemes, as shown in Figure 1-2.

![Figure 1-2. Turbine entry temperature and cooling effectiveness as a function of year (adapted [10,11]).](image)

To continue to make efficiency improvements through this manner, the next generation of engines will need to leverage more advanced cooling schemes. One example of an advanced cooling device is a fluidic oscillator that inherently relies on unsteady cooling to increase the
effectiveness [12]. To that end, *precise high-frequency heat flux measurements help predict the efficiency and durability of turbine components while quantifying unsteady interactions that could be detrimental to the engine.* Therefore, this dissertation work focuses on the fabrication and implementation of double-sided thin-film RTD HFGs for turbomachinery applications with an emphasis on the maturation of this sensing technology to more engine representative operation.

### 1.3 Double-sided Thin-film RTD HFGs

Double-sided thin-film RTD HFGs are uniquely capable of quantifying the time-resolved temperature and heat flux in turbomachinery flows [13]. These devices are spatially small (sensing head on the order of microns), which is ideal for capturing precise thermals in components with large gradients. Double-sided thin-film RTD HFGs have a typical time-response in the magnitude of 100kHz, which is necessary for capturing the time-varying behavior within the device. These sensors are also designed to be flexible, meaning that they can be minimally intrusive to the flow field while wrapping around complex airfoil shapes.

Double-sided thin-film RTD HFGs comprise two thin-film RTDs on opposing sides of a thermally and electrically insulating layer as shown in Figure 1-3. The heat flux is calculated by solving the unsteady conduction equation shown in Equation (1-1):

\[
\frac{\partial T}{\partial t} = \alpha_T \frac{\partial^2 T}{\partial x^2}
\]

where \(\alpha_T\) is the thermal diffusivity of the insulating layer and \(T\) is the temperature through the substrate. Sensors of this type are specifically designed to have well known thermal properties for the insulating layer and measure the necessary boundary conditions. The boundary conditions for the temperature are measurements of calibrated platinum RTDs placed on the top and bottom of an insulating medium. With knowledge of the thickness and thermal properties of the insulating medium, a representative heat flux can be calculated.
There are many methods to calculate the heat flux through \( q \) from the temperature difference between the top and bottom RTD \( \Delta T \). The easiest one is Fourier’s steady law of conduction:

\[
q = \frac{k_s \Delta T}{d_s}
\]  

(1-2)

although Equation (1-2) provides a sufficient representation of mean heat flux, it assumes that the thermal waves striking the RTD exposed to air \( T_1 \) have sufficient time to penetrate through the entire substrate and create a rise in temperature on the back sensor \( T_2 \), which is not an appropriate assumption passed the cutoff frequency for a particular gauge [13].

Because one of the primary benefits of this sensor is to resolve high frequency content, it is imperative to consider alternative processing approaches. The reconstruction of the entire frequency spectrum from the DC signal up to the frequency that the thin platinum can respond has been thoroughly studied [13] and various methods for obtaining the full spectrum have been presented. The Cook-Feldmen algorithm [15] provides a common solution using a temporal marching scheme. More advanced schemes for computing the full spectrum involve modeling the heat propagation as a 1D series of capacitors and resistors to mimic the thermal properties [16] or modeling the entire test article to account for 3D effects [17]. A 3D model is not feasible for many applications because the complexity of the geometries and cooling features are difficult to predict with confidence. As an alternative, the impulse response method [18] is a recently-developed 1D analysis tool that offers computational efficiency and accuracy over all frequencies. For these reasons, the impulse response method is a preferred technique for processing data from steady facilities and will be explored in detail later in the document.
Because these sensors are not commercially available, each user is required to create their own fabrication process to meet the requirements of a specific application. The sensors developed throughout this project are intended to meet conditions within Penn State's Steady Thermal Aero Research Turbine (START) Lab [19]. In large, this testbed operates for longer durations at harsher conditions than other experimental turbine facilities that have historically used sensors of this type. Therefore, the existing methods of fabricating and operating these sensors must be adapted.

1.4 Research Objectives

High-frequency thermal measurements are poised to be more generally applied with the advent of direct-deposition of sensors [20] paired with increased demand for thermally-based predictive maintenance [21,22]. However, there exists several technical barriers in extending this technique from established methods to more extreme operating conditions. This document presents solutions to several of these barriers while utilizing the sensors in a practical manner. The research objectives for this dissertation are two-fold: (i) create a generally applicable framework for the operation and implementation of HFGs within steady facilities; and (ii) use these devices within that framework to better understand time-varying phenomena within a gas turbine engine, particularly in the rim seal location.

The first research objective addresses processing improvements that are necessary to ensure the stability of the HFG RTDs and overall HFG robustness in harsher conditions. For that reason, this work describes novel calibration procedures and a theoretical framework for coated HFGs. These two improvements, aimed at increasing the life of these devices, are experimentally validated in the later chapters of the document. Importantly, these processes extend beyond experimental testbeds and provide an outline that is generally applicable to any steady facility, including engine operation.

The second research objective is to showcase the benefits of this advanced instrumentation by quantifying the time-varying thermal effects in a turbine application. To that end, this work presents the successful implementation of these sensors within the START facility. Moreover, the benefit of this instrumentation is displayed by characterizing time-varying shear layer instabilities, directly linking the time-varying thermal behavior to the performance of the turbine.

1.5 Outline of Dissertation

This dissertation encompasses five research works representing the central chapters of this dissertation. Three of these studies have undergone a peer review before being published in either a conference or journal format. The remaining two are currently within the publication process.
Chapter 2 is a study presented at Turbo Expo 2019 and published within the *Journal of Turbomachinery*. This work emphasizes that in order to continue the development of these sensors, the technology must adapt to longer duration testbeds. This work validates the current double-sided thin-film RTD HFG fabrication and operation methods against gauges of the same type from partner institutions.

Chapter 3 is a study presented during Turbo Expo 2021. This work is the introductory study of high-frequency thermal measurements within the START facility. Specifically, sensors were placed between the stationary turbine vane and rotating turbine blade. This area, known as the turbine rim seal, was chosen because time-varying shear layer instabilities form in this region. Within the study, turbine rim seal instabilities are investigated and correlated to steady cavity performance. These sensors directly link time-varying temperature measurements to regions of ingress and egress within the turbine rim seal region. The work serves as an example of the benefit gained through this advanced instrumentation.

To further improve the robustness of these HFGs, the remainder of this document focuses novel calibration procedures and the introduction of protective coatings. Chapter 4 presents a study that has been published in *Measurement Science and Technology* to combat the general issue of thin film RTD calibration stability through the use of novel calibration methods. This work combines existing thermal property measurement techniques with double-sided thin-film RTD HFGs to create an *in-situ* calibration method for the RTDs on the front and backside of the HFG.

Chapter 5 presents the theoretical framework of coated gauge design providing a roadmap to make more robust HFGs without sacrificing necessary frequency requirements. This work can be generally applied to any inverse heat conduction problem so long as two temperature measurements are known. It is currently under consideration for the *International Journal of Heat and Mass Transfer*.

Chapter 6 is the application of the framework presented in Chapter 5. This work serves as example of how that roadmap can be applied, utilizing the START testbed as the end application. This work is to be submitted to *Measurement Science and Technology*.

The final chapter of the document, Chapter 7, recapitulates the findings throughout the dissertation. In addition, this chapter provides insight into the future research direction of this sensor technology.
2 COMPARISON OF THIN-FILM HEAT FLUX GAUGE TECHNOLOGIES EMPHASIZING CONTINUOUS-DURATION OPERATION

2.1 Abstract

Thin film heat flux gauges (HFGs) have been used for several decades to measure surface temperatures and heat flux in test turbines with the majority being used in facilities that are short-duration. These gauges are typically composed of two resistive temperature devices deposited on opposing sides of a dielectric. However, because these sensors have been traditionally applied for measurements in short-duration, transient-type facilities, the challenges facing adaptation of this technology for a continuous-duration steady facility warrant investigation. Those challenges are highlighted, and solutions are presented throughout the paper. This paper describes the nanofabrication process for heat flux gauges and a new calibration method to address potential deterioration of gauges over long runtimes in continuous-duration facilities. Because a primary uncertainty of these sensors arises from the ambiguity of the thermal properties, special emphasis is placed on the property determination and potential errors due to improper thermal properties. Also, this paper presents a discussion on the use of impulse response theory to process the data showing the feasibility of the method for steady-duration facilities after an initial settling time. The latter portion of the paper focuses on comparing well-established heat flux gauges developed for short-duration turbine test facilities to recently developed gauges fabricated using modern nanofabrication techniques for a continuous turbine test facility. Using a commercially available heat flux gauge, capable of measuring a steady heat flux as a reference, the gauges were compared using the test case of an impinging jet over a range of Reynolds numbers. The comparison between the PSU gauge and the reference device indicated agreement within 14%, and similar results were achieved through comparison with established sensors from partner institutions.

2.2 Introduction

For nearly four decades, thin film heat flux gauges (HFGs) have been used to quantify heat transfer metrics for turbine components. Initial development of these sensors for turbomachinery was led concurrently by Oxford University [2,23] and MIT [13]. In the years that followed, this technology expanded to other institutions, including The Ohio State University [24] the von Karman Institute (VKI) [25], Virginia Tech [26], and the U.S. Air Force Research Laboratory

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(AFRL) [27,28]. For each of these users, the HFGs are applied in short-duration, blow-down tests with total operating times on the order of seconds or less. In contrast, continuous-duration facilities operate with time scales of hours and days pose potential challenges for installation and operation of HFGs as well as post processing.

This disparity of run times is illustrated in Table 2-1 showing a representative single-test run time for each facility. Using these run times, a worst-case scenario is calculated assuming three tests per day operating 365 days per year over a period of 20 years. For many of these facilities, a cumulative 20-year test time is on the order of a few hours, a period which will easily be overcome in a single test day for a continuous-duration facility such as the Steady Thermal Aero Research Turbine (START) facility at the Pennsylvania State University. In reality, most of these test articles can only operate one or two tests per day over a maximum of about four days per week, therefore further widening the gap.

This paper focuses on understanding the challenges associated with implementing HFGs in a continuous-duration facility and presenting viable solutions. A new in situ calibration procedure is evaluated, and the influence of thermal properties on calculated parameters is assessed. For these cases, an impulse response filter is used and considered for use in continuous-duration conditions. Finally, tests conducted with sensors manufactured at the Nanofabrication Lab at Penn State are compared with a commercially-available Heat Flux Microsensor (HFM) as well as established gauges currently in use at Oxford and AFRL.

<table>
<thead>
<tr>
<th>Facility</th>
<th>Ref</th>
<th>Length of Test [s]</th>
<th>Tests/Day</th>
<th>Cumulative 20 Year Test Time [hrs]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ohio State TTF</td>
<td>[29]</td>
<td>0.120</td>
<td>3</td>
<td>0.730</td>
</tr>
<tr>
<td>MIT</td>
<td>[16]</td>
<td>0.200</td>
<td>3</td>
<td>1.217</td>
</tr>
<tr>
<td>Oxford ORF</td>
<td>--</td>
<td>0.200</td>
<td>3</td>
<td>1.217</td>
</tr>
<tr>
<td>Oxford OTRF</td>
<td>--</td>
<td>0.500</td>
<td>3</td>
<td>3.042</td>
</tr>
<tr>
<td>VKI</td>
<td>[30]</td>
<td>0.500</td>
<td>3</td>
<td>3.042</td>
</tr>
<tr>
<td>AFRL</td>
<td>--</td>
<td>2.500</td>
<td>3</td>
<td>15.208</td>
</tr>
<tr>
<td>Virginia Tech</td>
<td>[26]</td>
<td>25.000</td>
<td>3</td>
<td>152.08</td>
</tr>
<tr>
<td>Penn State</td>
<td>--</td>
<td>8 [hrs]</td>
<td>1</td>
<td>58,400</td>
</tr>
</tbody>
</table>
2.3 Background

Epstein et al. [13,16] outlined the theory for double-sided heat flux gauges, followed by other practical studies on mid-span turbine heat transfer using the instrumentation with numerical post processing schemes [31,32]. Since then, this technology has enabled the study of unsteady heat flux for turbine components in both the stationary [25,33,34] and rotating [15,16] reference frame of airfoils, in the tip region [37–39], and more, benefitting from a minimally-intrusive design. Different types have been used [40–43], but all operate by solving the unsteady conduction equation through a substrate or substrates given a set of boundary conditions.

These boundary conditions can be obtained in two different ways. For example, a single-sided gauge, as seen in Figure 2-1(a) solves the conduction equation using one measured temperature and applying a semi-infinite approximation. In contrast, a double-sided thin film heat flux sensor, Figure 2-1(b), has a temperature measuring device on both sides of a known substrate.

A double-sided thin film heat flux sensor must be used when operating in steady conditions because the backside boundary condition cannot be treated as semi-infinite. By adding the second sensor, one can always measure the heat flux because the backside boundary condition is measured. This property makes it the right choice for a steady facility. To measure heat flux accurately, there must be a measurable temperature difference across the substrate. Because the substrate is inherently thin for this type of sensor, large heat fluxes are necessary to create a temperature difference discernable from the uncertainty in the measurement. For the START Lab, this large heat flux comes from actively cooling the part which creates an expected gas-to-metal temperature ratio of 1.3 K/K found from CFD predictions.

Although other types exist, the focus of this study is on single- and double-sided thin film HFGs where the dielectric beneath the temperature sensing element is polyimide. This limited scope is due to the way in which the instruments have been used in the past and will be first used in the START facility. However, as sensor fabrication becomes ever more intricate (such as direct deposition to an airfoil [44]) the solutions in this paper to the challenges in adapting heat flux gauges to a steady measurement will still hold.
To obtain the temperatures required to solve the unsteady conduction equation, thin-film resistive temperature devices (RTDs) are often implemented. To create these sensors, manual application of platinum paint may be used, but lithography processes can significantly improve control of dimensionality and alignment for double-sided gauges. A lithography technique typically requires advanced equipment, but has been used to successfully execute gauge designs using nickel [13,45] and platinum [14,25,46] resistive elements.

Beyond sensor fabrication, material properties of the dielectric must be precisely known to achieve accurate calculated results. This study focuses on a polyimide due to its dimensional stability, dielectric properties, and flexibility, but other materials such as quartz [47] and Macor [48] have been used in the past. Most important, it has been previously identified that the bulk thermal properties for polyimide substrates provided by the manufacturer can vary by as much as 20% from the measured value, and therefore must be independently validated [13].

In some cases, previous studies have empirically quantified these thermal properties as lumped parameters, specifically $\sqrt{\rho ck}$ and $k/d$ [43]. As one example of this method, a known heat flux can be introduced to the HFG, and the sensor response can be used to deduce the parameters of interest. Unfortunately, identifying a viable heat flux source can be difficult. Past studies have used an oil bath [45], a laser [13], and even the resistive element itself as the heater [49]. However, many authors have converged on a heated air gun serving as an impinging jet on the gauge for its repeatability and accurate representation of convective experimental conditions [43,45,50].

2.4 Fabrication

Within Penn State’s Nanofabrication Lab, a process was developed to produce single- and double-sided HFGs with high yield and repeatable results. The workflow is similar to that presented by Collins et al. [46]. Following this process, the gauges were manufactured through a combination of subtractive and additive processes, starting with a sheet of Pyralux®, a commercially-available product comprising a polyimide sheet with 9 $\mu$m of copper on each side. In the first part of the...
fabrication process, copper is removed from the Pyralux through an etching process, creating the pattern for the lead wires. The substrate then goes through a patterning process involving a mask for the platinum elements. Platinum is then deposited through an evaporative physical vapor deposition process, which differentiates this process from Collins et al. [46], where the platinum was sputtered. The platinum bridges the two lead wires and functions as the RTD element.

After completing these steps, a similar process is completed on the opposite side of the substrate. Penn State’s capabilities enable front and back side feature alignment within 5 μm. Through an application of the latest nanofabrication techniques, this alignment capability represents a new standard for HFG fabrication, a factor of two improvement over original manufacturing guidelines reported by Epstein et al. [16].

Accurate alignment capability is important for an orthogonal one-dimensional (1D) heat flux assumption to hold. Because the metal temperature is not a fixed parameter in the START turbine, large spatial gradients of temperature across the part could lead to a breakdown of the 1D assumption for these gauges. By ensuring the gauges are aligned to within 5 μm (5% of the smallest geometrical dimension for the present design), the heat flux can be treated as one dimensional and orthogonal to the airfoil surface.

Figure 2-2 compares the design intent to the fabricated gauge for sensors manufactured at Penn State. Because the physical vapor deposition process is more precise than the etching process, the dimensionality of the platinum is more controlled than the copper (as identified by a slight overhang of platinum on the top portion of the gauge). Although these gauges represent a sensing element 90 μm x 500 μm, gauges with features as small as 4 μm have been successfully fabricated at Penn State. Controlling the geometry and the amount of platinum deposited changes the resistance of the sensor. After the fabrication is complete, the sensor must be calibrated before it can be used.
2.5 Calibration

Traditionally, the calibration process has been performed using a calibration bath. This study evaluates a standard calibration bath process and expands the calibration procedures by proposing the feasibility of another technique called the 3-omega method, which enables continuous-duration facilities to routinely check HFG calibration coefficients.

2.5.1 Oil Bath Calibration

Because platinum has a linear resistance behavior over the temperatures of interest, the calibration curve is dependent upon the offset and the slope of the fit. The offset is the reference resistance and the slope of the line is the coefficient of resistance. For this study, a temperature calibration bath was used with silicone oil as the heat transfer fluid. The bath has a uniformity to within 0.02°C and a stability within 0.01°C. A higher degree of accuracy was achieved by pairing the bath with a reference thermometer and an RTD, ultimately yielding an overall accuracy of 0.05°C.

Before the calibration procedure begins, gauges were annealed at a fixed temperature, slightly higher than the expected maximum operating temperature of the sensor, until the nominal resistance of the gauges changed by less than 1% over an hour. This step is necessary to relax the internal stresses in the gauge left during the evaporative deposition process and mitigate hysteresis in thermal properties.

Next, the calibration bath was automatically swept through the predefined temperature calibration range and repeated several times to quantify hysteresis; typical results are shown in
Figure 2-3. The calibration measures the voltage drop across the lead wires as well as the platinum element. Calibrating using the same leads as in the experiment ensures lead effects are taken into account. The resistance is then calculated by dividing by a known current excitation. If each calibrated gauge had an $R^2$ value of greater than 0.999, corresponding to a maximum temperature error of 0.3°C.

![Calibration Curve](image)

**Figure 2-3.** Calibration curve for five different thin film RTDs. The slope of the line represents the coefficient of resistance.

The calibration curves in Figure 2-3 represent five different gauges made from three different institutions. The sensitivity of the gauge is directly dependent on the coefficient of resistance [16], so some users find it advantageous to maximize this quantity. In this case, different levels of deposited platinum directly contribute to the coefficient of resistance: when more platinum is deposited, the coefficient of resistance increases. For deposition thicknesses greater than approximately 100 nm, quantum effects are expected to become negligible [51]. However, Table 2-2 shows that the coefficient of resistance for these gauges is still far from the bulk value for platinum.
Table 2-2. Coefficient of Resistance for Different Sensors

<table>
<thead>
<tr>
<th>Manufacturer</th>
<th>Pt Deposition</th>
<th>Coefficient of Resistance ($\alpha_R$) [$^\circ$C$^{-1}$]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Penn State</td>
<td>Low (50 nm)</td>
<td>7.08E-4</td>
</tr>
<tr>
<td>Penn State</td>
<td>High (200 nm)</td>
<td>1.84E-3</td>
</tr>
<tr>
<td>AFRL</td>
<td>Low</td>
<td>1.45E-3</td>
</tr>
<tr>
<td>AFRL</td>
<td>High</td>
<td>1.62E-3</td>
</tr>
<tr>
<td>Oxford</td>
<td>--</td>
<td>1.67E-3</td>
</tr>
<tr>
<td>Platinum [52]</td>
<td>--</td>
<td>3.85E-3</td>
</tr>
</tbody>
</table>

There are two primary explanations for the discrepancies identified in Table 2-2 compared with the expected value for bulk platinum. First, the platinum may be improperly annealed, as temperatures higher than the polyimide decomposition point are required to achieve a fully-annealed state [53]. Second, the polyimide surface roughness may contribute. For example, platinum thin film RTDs on the order of 350nm deposited on silicon showed higher coefficients of resistance closer to the bulk values [54]. Ultimately, although sensitivity increase may be desirable, gauges with coefficients of resistance similar to Table 2-2 have been used successfully for several decades.

2.5.2 3-omega Calibration Check

Because continuous-duration facilities are subject to long run times, the potential for gauge erosion over time necessitates an in situ calibration capability. If the gauge calibration shifts for any reason during a test, it would not be feasible to recalibrate in an oil bath before and after test campaigns due to the extended times required to remove and install necessary hardware.

To address this need, the 3-omega technique [55] states that exciting a properly-designed gauge with a sinusoidal current at frequency $\omega$ will introduce Joule heating and a corresponding temperature rise at a harmonic frequency, $2\omega$. In turn, this creates a voltage across the resistor that has a dominating $1\omega$ component with a small $3\omega$ component. By independently measuring the amplitudes of these harmonic components, Equation (2-1) can be applied.

$$ \varphi_{3\omega} = \frac{13}{16\omega} \pi k \int_0^\infty \frac{\sin^2(\eta b) d\eta}{(\eta b)^2 \sqrt{\eta^2 + \gamma(\omega)^2}}, $$

(2-1)

which can be solved numerically. This solution is presented in Figure 2-4 with a plot of thermal wavelength, given by
\[ \lambda = \sqrt{\frac{k}{\rho c^2 \omega}} = \frac{1}{Re\{\gamma\}}. \] (2-2)

To achieve accurate measurements of thermal conductivity with this method, the thermal wavelength should be less than the thickness of the material to satisfy the semi-infinite boundary condition requirement [56]. In this study, the thickness of the etched Pyralux (EP) was 50 \( \mu \)m. Based on these dimensions, the dashed red lines in Figure 2-4 illustrate the region where the linear approximation is most appropriate for the current gauge design.

![Figure 2-4](image_url)

**Figure 2-4.** Numerical solution to the third harmonic of voltage solved as a function of a given heating frequency.

Simplifying Equation (2-1) for the linear region of the solution, Equation (2-3) is obtained with a log-base slope dependent upon the thermal conductivity of the polyimide and the coefficient of resistance of the heater.

\[
\tilde{V}_{3\omega} = \frac{-V_{3\omega}^3}{4\pi R_e L k} \alpha_R \left[ \ln(2\omega) + \ln \left( \frac{h^2 \rho c}{k} \right) - 2\xi \right] - i \frac{V_{1\omega}^3 \alpha_R}{8LkR_e} \] (2-3)

In the above equation, \( \tilde{V}_{3\omega} \) is measured, and \( R_e \), \( L \), and \( b \) are known from the gauge characterization. The thermal properties \( \rho \), and \( c \) are also considered known parameters (more
details on these measurements are offered later in the paper). The frequency, $\omega$, is controlled experimentally, therefore leaving two unknowns: $\alpha_R$ and $k$. If one of these parameters is known, then the technique can be used to solve for the other.

The technique offers two primary benefits. First, because the coefficient of resistance is known through a traditional oil bath calibration, the 3-omega method can be used to obtain thermal conductivity across a range of temperatures. Second, with knowledge of thermal conductivity as a function of temperature, the same method can be subsequently used to obtain the \textit{in situ} coefficients of resistance, assuming the thermal properties of the substrate do not change over time. Work is currently being conducted to verify this assumption. This feature allows for the HFGs to be easily calibrated before, during, and after each measurement.

To test this theory, thermal conductivity was measured using the 3-omega method at room temperature using the gauge shown in Figure 2-2. The 3-omega technique was applied over a range of frequencies, and the results are reported in Figure 2-5. In Figure 2-5, the dashed lines represent the analytical solution of both the in-phase and out-of-phase signal. To obtain these curves, the thermal conductivity of the material was assumed to be a fixed known value representative of EP. Using this value as a standard, the thermal conductivity determined from the 3-omega method differed by as much as $\sim 20\%$. This discrepancy is attributed to a narrow range of frequencies used to obtain the results, which will be improved by a gauge geometry change for future tests. By having a larger range of frequencies to test, a more representative thermal conductivity can be found.

Although this method is still in its preliminary stages of development for HFG use, it has been used in for decades in the nanofabrication field to characterize the thermal conductivity of thin films. Potential benefits exist by leveraging this established technique with HFG operation, especially in continuous-duration facilities.
2.6 Thermal Property Determination

The material properties required to transform measured temperature to heat flux for a double-sided type gauge are density, specific heat, and thermal conductivity, as well as the thickness of the dielectric. More precisely, the heat flux depends upon the thermal product \((\sqrt{\rho c k})\) and the ratio \(k/d\). Quantifying these parameters and understanding the associated uncertainties is necessary to bound the corresponding uncertainty in the heat flux calculations. The properties presented here represent measurements of EP, and comparisons are drawn with other polyimides from previous studies.

Specific heat measurements were obtained using differential scanning calorimetry following ASTM E1269 [57]. The overall uncertainty for this method was 10%. Figure 2-6 shows the specific heat as a function of temperature, which exhibits a 45% variation over the range of temperatures tested and aligns well with measurements from previous studies. In previous studies, Choy [58] measured Kapton H while Kotel’nikov [59] and Lambert [60] measured generic polyimide film. In Figure 2-7, the value for EP reported by the manufacturer effectively represents the mean value across the selected temperature range but does not appropriately account for variations with temperature.
The density of the EP was measured following ASTM D6226 – 15 [61]. This volume displacement measurement found the density to be 1510 kg/m$^3$ at 20°C with an accuracy of 2%. Because the thermal coefficient of expansion for these polyimide films is known with high stability, it is possible to approximate changes of density as a function of temperature using the coefficients shown in Table 2-3.

**Table 2-3. Coefficient of Thermal Expansion based upon Kapton HN**

<table>
<thead>
<tr>
<th>Thermal Coefficient of Expansion [ppm/°C]</th>
<th>Temperature Range [°C]</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\tau_T_1$</td>
<td>17</td>
</tr>
<tr>
<td>$\tau_T_2$</td>
<td>32</td>
</tr>
</tbody>
</table>

Because the mass of the sample remains unchanged, the density change can be correlated with volume using Equation (2-4) assuming isotropic expansion.

$$\frac{\Delta V}{V_{20^\circ C}} = \frac{V_{150^\circ C} - V_{20^\circ C}}{V_{20^\circ C}} = \frac{3\Delta L}{L} = 3[\tau_{T_1}\Delta T_1 + \tau_{T_2}\Delta T_2]$$  \hspace{1cm} (2-4)

Choosing $\Delta T_1 = 70^\circ C$ and $\Delta T_2 = 50^\circ C$, the corresponding volume change is less than 1%, and serves as a first-order direct approximation of density variations over this temperature range, which represents a variation much less than the other properties of interest.
Thermal conductivity measurements were collected using ISO Standard 22007-2. Three measurements were collected: Kapton HN at 23°C, EP at 23°C, and EP at 150°C, and these results are reported in Figure 2-7. The absolute uncertainty of the measurement is 5%, and the relative standard deviation was less than 2.3%. Although only two points were measured for EP, the thermal conductivity of the samples varied by more than 27% over the temperature range of interest.

![Figure 2-7. Thermal conductivity measurements for EP as a function of temperature.](image)

After each thermal property was individually characterized, the thermal product ($\sqrt{\rho \kappa}$) was examined. Table 2-4 compares the present study with measurements at two temperatures to previously published data from AFRL [14], Oxford [42,43], MIT [13], and VKI [50] as well as manufacturer-reported specifications [62–64]. Although this table shows a wide range of values, this spread is not unexpected. Each institution characterized their own materials, and the methods for quantifying thermal properties varied between studies. In the studies from VKI and Oxford, the material the gauge was mounted upon was taken into account, which is why Table 2-4 has multiple studies or values from the same institution. Details about the methods can be obtained from the individual references. Although the dielectric was always a polyimide, manufacturers and batch-to-batch variations contribute noticeably to the result. Based on this observation, it is increasingly important for users to regularly measure the thermal properties of the dielectric for each sensor build to obtain high-quality heat flux measurements.
Table 2-4. Comparison of Thermal Properties from Various Studies Used to Calculate Heat Flux through Thin Polyimide Films

<table>
<thead>
<tr>
<th>Institution</th>
<th>Ref</th>
<th>Material</th>
<th>$\rho$ [kg/m$^3$]</th>
<th>$c$ [J/(kg·K)]</th>
<th>$k$ [W/(m·K)]</th>
<th>$\sqrt{pc}$ k [J/(m·K)$^{3/2}$]</th>
<th>% Diff Relative to $T_{prop}$=50°C</th>
<th>% Diff Relative to $T_{prop}$=150°C</th>
<th>Uncertainty $\sqrt{pc}$ k</th>
</tr>
</thead>
<tbody>
<tr>
<td>Present Study [50°C]</td>
<td>--</td>
<td>Etched Pyralux</td>
<td>1510</td>
<td>876</td>
<td>0.19</td>
<td>496</td>
<td>--</td>
<td>30.85%</td>
<td>5.89%</td>
</tr>
<tr>
<td>Present Study [150°C]</td>
<td>--</td>
<td>Etched Pyralux</td>
<td>1510</td>
<td>1243</td>
<td>0.22</td>
<td>649</td>
<td>30.85%</td>
<td>--</td>
<td>6.25%</td>
</tr>
<tr>
<td>Air Force (2011) [23°C]</td>
<td>[27]</td>
<td>Kapton HN</td>
<td>1410</td>
<td>1058</td>
<td>0.18</td>
<td>518</td>
<td>4.44%</td>
<td>20.18%</td>
<td>6.52%</td>
</tr>
<tr>
<td>Oxford (1999)   [24]</td>
<td></td>
<td>Upilex</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>495</td>
<td>0.00%</td>
<td>23.73%</td>
<td>4.20%</td>
</tr>
<tr>
<td>MIT (1985)      [3]</td>
<td></td>
<td>Kapton HN</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>575</td>
<td>15.93%</td>
<td>11.40%</td>
<td>5.00%</td>
</tr>
<tr>
<td>VKI (2002)      [32]</td>
<td></td>
<td>Upilex-S</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>699-731</td>
<td>47.37%</td>
<td>12.63%</td>
<td>8.80%</td>
</tr>
<tr>
<td>DuPont           [47]</td>
<td></td>
<td>Etched Pyralux</td>
<td>1430</td>
<td>1089</td>
<td>0.26</td>
<td>636</td>
<td>28.23%</td>
<td>2.00%</td>
<td>--</td>
</tr>
<tr>
<td>DuPont           [45]</td>
<td></td>
<td>Kapton HN</td>
<td>1420</td>
<td>1090</td>
<td>0.12</td>
<td>431</td>
<td>13.10%</td>
<td>33.59%</td>
<td>--</td>
</tr>
<tr>
<td>UBE              [46]</td>
<td></td>
<td>Upilex-S</td>
<td>1470</td>
<td>1130</td>
<td>0.29</td>
<td>649</td>
<td>30.85%</td>
<td>0.00%</td>
<td>--</td>
</tr>
</tbody>
</table>

Beyond the discrete results provided for the present study in Table 2-4, the combination of measured properties over a range of temperatures yields thermal product for all temperatures between 50°C and 100°C. In this case, the thermal conductivity was only measured at two temperatures, but a linear approximation between those two points is appropriate [58]. The results in Figure 2-8 are presented as a variation normalized by their mean values across the range of measurements. From this graph, it is easy to see that the specific heat contributes the most to the change in thermal product followed by the thermal conductivity. The density variations are an order of magnitude less than the other two parameters.

Based on the variations identified in Figure 2-8, it is important to quantify thermal properties at the temperature of operation to reduce errors. This importance increases for continuous-duration facilities that may have a larger variance of local gauge operating temperatures. In Table 2-4, Oxford, MIT, and VKI implicitly obtained thermal properties as lumped parameters, but discussions of varying temperatures were not included. In contrast, individual parameter measurements in the present study provide an inherent ability to control the temperature.
2.7 Error Sensitivity Analysis

Although this study considers both the single- and double-sided gauges, double-sided gauges must be used in a continuous-duration facility because the backside (metal) temperature will not fulfill a semi-infinite assumption. For this reason, the subsequent error sensitivity analysis was completed for a double-sided gauge only.

The uncertainty analysis was completed using a perturbation method in conjunction with Oldfield’s Impulse Response Theory (IRT) toolbox [18]. This toolbox takes the temperature signals from the gauge, $T_1$ and $T_2$, as well as the material properties as inputs. The output is the corresponding heat flux. Since the thermal properties are a function of temperature, it is advantageous to quantify the uncertainty in the output, $q$, for thermal properties at different temperatures.

To this point, calculated heat flux has been introduced as a function of the top and bottom gauge temperatures, the thermal product, and the thermal conductivity over the thickness of the substrate. As shown in the last section, the thermal conductivity and specific heat are direct functions of temperature. To obtain accurate measurements, the temperature of the EP during test operation must be known, and the operating temperature must fall within the temperature range over which the thermal properties were characterized. To illustrate the importance of these steps, a synthetic test signal is considered whose data are captured at an EP temperature of 100°C and creates a constant heat flux of 50,000 W/m² as shown in Figure 2-9. The temperature traces come
from analytical solutions while the thermal properties come from experimental conditions. By using analytical temperature traces, the influence of thermal properties can be isolated from signal noise and an imperfect experimental set-up.

Figure 2-9. Analytical temperature traces from two sides of a double-sided gauge corresponding to a constant heat flux.

To understand the influence of thermal property variations with temperature on calculated heat flux, the same temperature traces are considered (i.e., with a substrate temperature of 100°C), but the results were processed (with IRT) using thermal properties of the EP substrate quantified at temperatures different from the operating temperature. Following this process, Equation (2-5) shows the percent error, $\varepsilon$, introduced by variations of thermal properties with temperature, defined by:

$$
\varepsilon = \frac{q_{\text{top}} - q_{\text{prop}}}{q_{\text{prop}}} \times 100
$$  \hspace{1cm} (2-5)

The abscissa in Figure 2-10 is denoted as $T_{\text{top}} - T_{\text{prop}}$ where $T_{\text{top}}$ is the operating temperature of the gauge and $T_{\text{prop}}$ is the evaluation temperature of the thermal properties. Based on Figure 2-10, the operating temperature must be within ±10°C of the temperature at which properties were measured to achieve a corresponding error less than 3%. As a corollary to this statement, if a discrepancy between operating temperature and characterization temperature of 50°C occurs, an error of up to 14% may be introduced. This error arises because the temperature traces used to create the constant heat flux will alter the shape of the resulting heat flux when processed with different thermal properties. As the trend of thermal product with temperature
shown in Figure 2-8 is approximately linear, the corresponding influence on error propagated through the processing algorithm yields a pattern in Figure 2-10 that is approximately symmetric. The results in Figure 2-10 were also evaluated for different levels of mean heat flux and different operating temperatures, yielding negligible differences on the magnitude or trend of error as a function of temperature.

In this assessment, the error quantified in Figure 2-10 is independent of the accuracy of the measured properties themselves. Using information from the previous sections, the uncertainties of thermal conductivity ($k$), specific heat ($c_p$), and density ($\rho$) are 5%, 10%, 2%, respectively. Other factors include uncertainty of measured temperature, dielectric thickness (believed to be 10% [62]), and calibration coefficient of resistance of the RTD (0.5%). Assuming that the thermal properties are known for the temperature at which data is recorded (i.e., independent of the errors introduced in Figure 2-10), the overall effect on the calculation of heat flux from the above uncertainties is approximately 8%, and was calculated using a perturbation method [65]. A combination of these relative errors would be applied for each individual application to calculate a representative overall uncertainty.

![Figure 2-10. Error in the quantified heat flux based on a temperature difference between gauge operating temperature and thermal property evaluation temperature.](image)

2.8 Steady Facility Post Processing

Although many post processing schemes for double-sided heat flux gauges exist, the impulse response theory (IRT) introduced by Oldfield [18] is widely used for its low numerical
error and computational efficiency. However, this method assumes the measurements start from a zero condition, dictating an inherent settling time that appears when processing data beginning from a steady operating condition. This settling time is derived from the Laplace transform and one-sided z-transform both starting at \( t = 0 \) in the IRT analysis. Therefore, to apply this processing technique for continuous-operation facilities, the settling time and the associated errors must be quantified by comparing the analytical and numerical solution. Although the error has been studied previously for a step change in heat flux (i.e., short-duration blow-down tests), a step change does not correlate well to continuous-duration facility operation. To more accurately represent the conditions in a continuous duration flow facility, synthetic temperature traces were created which correspond to a sinusoidal heat flux around a known mean value, and these signals were filtered using Oldfield’s toolbox for comparison with the analytical solution. Starting with the boundary condition for heat flux experienced by the gauge,

\[
q(t) = -k \frac{\partial T}{\partial x} \bigg|_{x=0} = \bar{q} + \bar{q} \sin(\omega t),
\]

where \( x = 0 \) is the top (air side) of the gauge and \( x = d \) at the back (metal side), an analysis was performed for a sensor neglecting glue layers and protective layers on top of the gauge.

Synthetic temperature profiles for the top and bottom of the gauge were found solving the unsteady conduction equation. A full analysis of the analytical solution can be found in previous work [13]. Figure 2-11 shows the result comparing the analytical solution with processed data using the IRT method for a single sinusoidal frequency, \( \omega \). In Figure 2-11, the analytical solution is represented by the dashed line. For times at the beginning of the processed data window, there is a discrepancy between the heat flux calculated through Oldfield’s IRT method (solid line) and the analytical solution. Because the IRT solution asymptotically approaches the analytical solution, the time at which one full period is within 1% of the analytical solution is denoted \( t_{\varepsilon1} \).
Following the procedure outlined in Figure 2-11, a series of cases were tested over a frequency range from 0-50 kHz (covering several harmonics of potential blade passing frequencies). The $\bar{q}$ and $\hat{q}$ values were varied as seen in Table 2-5. These were chosen to account for different fluctuation levels in the signal.

Table 2-5. Parameters Tested for IRT under Steady Operation.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Tested Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\omega$</td>
<td>[kHz] 0-50</td>
</tr>
<tr>
<td>$\bar{q}$</td>
<td>[W/m$^2$] [2, 20, 2×10$^5$]</td>
</tr>
<tr>
<td>$\hat{q}$</td>
<td>[W/m$^2$] [0.01, 0.1, 0.5]</td>
</tr>
</tbody>
</table>

For all test cases, the settling time to reach an error below 1% was less than 0.01 seconds over the entire range of frequencies. These results, shown in Figure 2-12, are a function of the ratio of the amplitude to the mean value, with an inverse relationship between heat flux amplitude and calculated settling time. Phase lag and RMS error were also calculated with influences less than 0.5% for all values tested. Because all the error metrics tested were found to be less than 0.5% after the settling time, this method of data processing can be applied for continuous-duration facilities if
the user collects more data than necessary and identifies that an initial portion must be removed to account for the settling time associated with the processing technique.

2.9 Impinging Jet Experimental Setup

After characterizing the individual properties of the gauges, a shutter-rig-type experiment was setup to test the whole gauge at the device level. This test apparatus consists of a heat gun mounted at a fixed distance from an HFG sensor. The heat gun was experimentally characterized as a function of device control settings (outlet temperature and flow level) with the use of a pitot-static probe and thermocouple to measure the exit conditions of the device. Through this process, three Reynolds numbers based on nozzle diameter, \( \text{Re}_D \), were selected for assessment: \( 5 \times 10^3 \), \( 10 \times 10^3 \), and \( 12 \times 10^3 \).

To quantify the heat flux from the heat gun, a Vatell Heat Flux Microsensor (HFM) was mounted at a spacing of 4D from the outlet of the heat gun, as shown in Figure 2-13. This sensor has an uncertainty of 5% and was used as the reference in this study [66], and the spacing was chosen to compare with literature [67]. Results from the impinging jet at all \( \text{Re}_D \) were found to be within 10% of values for \( \text{Re}_D = 10 \times 10^3 \) and \( \text{Re}_D = 12 \times 10^3 \) reported in the literature study.
2.10 Comparison of Gauge Performance

Three different thin film heat flux gauges were used under the shutter rig. The first was a double-sided thin film heat flux gauge manufactured by AFRL. The second and third were a single-sided gauge manufactured at Penn State and Oxford. Note that because of the experimental setup, both double- and single-sided gauges could be used. Although double-sided gauges must be used in a steady facility, for a transient jet, both are applicable.

Each gauge was excited using a current amplitude selected to yield a voltage drop across the resistor of 0.25 V at room temperature. Because the single-sided gauges require accurate thermal properties of the material onto which the gauges are mounted, the gauges were adhered onto an aluminum block whose thermal properties were measured using the same techniques outlined above. The aluminum block was instrumented with thermocouples to quantify the backside temperature of the block, as well as the temperature near the surface to compare to the thin film RTDs. When accounting for the uncertainty in the second layer thermal properties, the uncertainty in the measured heat flux from a single-sided PSU gauges is 10%, calculated by using the upper and lower bound for the thermal properties to determine the heat flux and comparing the difference.

These results, shown in Figure 2-14, were averaged over 0.3 seconds of steady data after the shutter was released (sampled at 5 kHz). Also in Figure 2-14 are values from Goldstein et al who measured the heat flux from an impinging jet over a similar range of Re_D using a temperature...
sensitive liquid crystal technique (LCT) [67]. For each gauge, the heat flux was calculated using the thermal properties provided by the respective institution and reported in Table 2-4. Because the thermal properties of the Penn State gauge were known for a range of temperatures, the mean temperature of the sensor over the 0.3 second averaging window was used to calculate the heat flux, which was not possible with the single values from other institutions. To calculate the thickness of the sample, a three-dimensional microscope was used with a resolution of 4 μm. Combining this information, error bars in the figure were calculated using the quoted uncertainty from each institution.

Overall, the gauge in closest agreement with the reference was Penn State. Because all three gauges were calibrated and excited with the same system, the errors between the gauges must be from the thermal properties used to process the data. Since the Penn State property determination came from a piece of EP near where the gauge was built, it was potentially more representative of the dielectric than the values quoted by the other institutions. For this reason, this specific gauge read closer to the commercially available gauge. This statement reiterates the importance of quantifying thermal properties for each set of gauge builds.

![Figure 2-14. Comparison of gauges under impinging jet using shutter rig apparatus with literature values.](image)

2.11 Conclusions

This study addresses the development and implementation of thin film heat flux gauges specific to a continuous-duration test facility, such as the Steady Thermal Aero Research Turbine Laboratory at Penn State. The fabrication of the gauges has been outlined and shown to have
alignment within 5 microns which is critical to the orthogonal 1D assumption in a steady facility. Although the fabrication is an important step, gauge deterioration is a major concern for steady-operating facilities.

To address this concern, unique calibration procedures involving the 3-omega method show utility as a method to assess gauge deterioration and perform in situ calibration verifications. This method enables more frequent calibrations, thereby lowering uncertainty in the measurements associated with long run times.

Thermal property determination, which represents a significant contribution of error in heat flux measurements, was addressed by independently measuring the density, specific heat, and thermal conductivity over a range of temperatures. These measurements showed an overall thermal product variation of more than 30% over the range of temperatures studied. Using this information an analytical solution to a step change in heat flux was used to determine the error in the heat flux measurement introduced by evaluating heat flux with thermal properties quantified at a temperature different from the sensor operating temperature through Impulse Response Theory. Based on these measurements with polyimide films, thermal properties should be measured within 10°C of desired test conditions to maintain a propagated error less than 3%.

For post processing, the IRT method was used for its computational efficiency. Post processing errors from the IRT method were calculated using an analytical solution to a sinusoidal heat flux. Over a range of frequencies, the associate settling time to achieve a processed error less than 1% is less than 0.01 second, and the settling time is independent of mean heat flux level. The RMS and mean value errors for the method were also found to create less than a 0.5% error over all frequencies.

Finally, the mean heat flux from an impinging jet was measured using thin film heat flux gauges from Penn State, AFRL, and Oxford. All gauges show agreement with a defined standard that was within their uncertainty over all conditions tested. Because the accuracy of these gauges depends on thermal property determination, the importance of correct thermal property determination is shown exemplifying that to obtain accurate measurements, thermal properties must be checked often for the specific polyimide beneath the gauge.

Beyond steady facilities, the process outlined in this paper shows the course of HFG implementation in any steady environment, including gas turbine engines themselves. Although the fabrication techniques must advance before such implementation, the process of calibration, thermal property determination, and data post-processing can stay the same to those outlined in this paper.
3 CORRELATING CAVITY SEALING EFFECTIVENESS TO TIME-RESOLVED RIM SEAL EVENTS IN THE PRESENCE OF VANE TRAILING EDGE FLOW

3.1 Abstract

The cavity region between the rotor and stator relies on hardware seals and purge flow to discourage hot gas path air from being ingested into the unprotected wheel space. However, ingestion can occur due to a combination of disk pumping, periodic vane-blade interactions, and three-dimensional seal geometry effects. These mechanisms create flow instabilities that are detrimental to cavity seal performance under certain conditions. In this paper, a one-stage turbine operating at engine representative conditions was utilized to study the effect of steady and time-resolved under-platform cavity temperatures and pressures across a range of coolant flow rates in the presence of vane trailing edge (VTE) flow. This study correlates time-resolved pressure with time-resolved temperature to identify primary frequencies driving ingestion. At certain flow rates, the time-resolved pressures are out of phase with the temperatures, indicating ingestion. These same flow rates were found to correlate to an inflection region in the cooling effectiveness curve where the maximum amplitude of the time-varying behavior coincides with the cooling effectiveness inflection point. Using a time-accurate computational model, simulations near this inflection region illustrate ingestion of high-swirl VTE flow into the cavity region which creates a buffer in the rim seal between swirled main gas path flow and axially injected purge coolant helping to suppress the amplitude of time-resolved behavior.

3.2 Introduction

As lofty goals are targeted by the aviation industry to reduce emissions [6], greater efficiency is required for gas turbine engines. One of the driving factors of efficiency is the turbine entry temperature. For several decades, this parameter has surpassed the softening temperature of the turbine components [11] creating durability challenges in the downstream parts.

One area particularly affected by the harsh environment is the turbine cavity region between rotating and stationary components. This region is designed to seal the unprotected wheel space from hot gas path ingestion. Although many types of seals exist, most aviation engines discourage ingress through a combination of various axial and radial clearance changes. The outermost seal where the platform of the blade and platform of the vane overlap is often referred to as the rim seal.

Rim seals are subject to ingestion because of their proximity to the main gas path. For that reason, measurements in the rim seal are the main focus of this paper. Secondary air from the compressor is injected at a low radius to pressurize the cavity. Because this bleed air causes a penalty to the efficiency of the engine, it is imperative to develop an understanding of the physical mechanisms that drive the ingestion and ensure effective use of secondary air.

Attempts to understand and predict the sealing effectiveness of first stage turbine cavities through computational modeling are powerful, yet limited. Because this region is a low-potential three-dimensional flow field with time-varying vane-blade interactions, models are either computationally expensive or oversimplified. Under certain flow conditions where the time-varying pressure field drives ingress, such models often result in inaccurate predictions [68,69]. As a result, there is a need for time-resolved measurements to develop time-accurate models and understand how to control and manage these flow behaviors.

This paper utilizes a one-stage turbine with engine-representative hardware and seal geometries to provide a time-resolved dataset linking pressure and temperature events to the steady cooling effectiveness in the rim seal cavity. First, the impact of vane trailing edge (VTE) flow on steady cavity performance is analyzed. Then, the time-resolved pressure and temperature are correlated indicating a connection between coolant flowrates and time-varying ingress. This ingress is further examined through spectral decomposition using VTE flow to investigate how pressure asymmetries around the annulus affect instabilities in the cavity region. Finally, a computational model is utilized to augment the experimental data by tracking VTE flow ingestion and changes in cavity dynamics with and without VTE flow present.

3.3 Literature Review

A plethora of literature exists on cavity ingestion mechanisms. Johnson et al. [70] outlines several mechanisms in a holistic review article of cavity ingestion including: disk pumping, periodic vane and blade pressure fields, 3-D geometric effects, asymmetries in the rim seal, turbulent transport, and flow entrainment. The more recent literature has focused on the sealing effectiveness parameter, $\varepsilon$, for various types of seals [71]. The sealing effectiveness is an important parameter in cavity performance because it provides a direct quantification of the ingress of the main gas path flow through concentration measurements of a tracer gas [72]. This literature review will focus on isolating the periodic effects from the blade-vane interaction as well as the time-varying flow instabilities that can arise in the cavity region. Particular focus will be placed on how the time-varying flow can influence the sealing effectiveness.
The majority of studies conducted to date compare cavity sealing performance with an analytical model [73–75]. One established model for sealing effectiveness by Sangan et al. [76,77] simplifies the rim seal geometry into a ring with two orifices, one for ingress and one for egress, to develop an analytical relationship. The model accounts for externally-induced ingress, rotationally-induced ingress, and swirl effects in the cavity. However, theoretically-indeterminate empirical constants are needed to solve the effectiveness equations. Because of this, it is difficult to incorporate as a true predictive tool, but its integration provides a valuable relationship with data that is grounded in physics. Although the model by Sangan et al. has been successfully applied in various studies, there are conditions for which the trend predicted by the model cannot fully explain certain behaviors in the sealing effectiveness curve [68,69,78]. The discrepancy between the model and the data in these situations has been attributed to instabilities in the cavity causing increased ingestion [78]. Although these instabilities have been studied, they are not currently well understood.

One possible onset of the time-varying ingress is the asymmetric pressure field from the vane-blade interaction [79]. Bohn et al. [80] studied the effect vanes have on ingress in a single stage turbine rig without blades through pressure and velocity measurements indicative of ingress. Without vanes, results indicate that a fully-sealed condition can occur at high coolant flow rates but with the introduction of wakes from the vane, hot gas path ingestion cannot be completely suppressed. Later, Bohn et al. [79] conducted a supplemental study using CO$_2$ as a tracer gas to measure the sealing effectiveness of the cavity. In this study, the effect of rotational and blade Reynolds numbers ($Re_\Omega$ and $Re_x$) on ingestion was investigated for two simple seal designs. Bohn et al. showed that as the blade Reynolds number increased, the ingress also increased due to the larger velocity deficit in the wake of the vane. Bohn et al. [81,82] provided a comprehensive look at the investigation with the addition of blades. This work concluded that for different seal geometries, the presence of rotor blades could either significantly improve or hinder the sealing performance of the cavity based on relevant geometric parameters and how the blade interacts with the vane wake. Overall, these series of experiments show the large effect the vane wake has on the cavity performance and how blades can create additional impacts on the seal. They also show that the rim seal performance is truly a time-varying behavior that is not effectively characterized through steady evaluation techniques.

In addition to the time-varying vane-blade interaction, large-scale rotating pressure structures in the cavity region have been experimentally measured and numerically predicted [83,84]. These structures are not captured in the orifice model outlined by Sangan et al. [76,77] and may be an additional cause of deviation from predicted trends. Using computational predictions, rotating
pressure structures have been found to move at a variety of speeds with a range of cell numbers at frequencies uncoupled to the vane and blade count [85]. Furthermore, structures moving at low frequencies have been shown to exist without blades and vanes, suggesting the cause of the instability is not necessarily externally driven [78,84]. This observation is of particular interest for the present study.

Rabs et al. [86] attributed the presence of large-scale rotating pressure structures to the Kelvin-Helmholtz instability. Kelvin-Helmholtz instabilities are formed in the presence of a velocity shear layer. In a turbine rim seal application, the method of coolant flow injection can play a critical role in determining the velocity shear layer and, hence, the formation of these instabilities. Patinios et al. [87] studied the effect of different coolant injection methods in a single-stage turbine cavity with simplified seals. In this study, Patinios et al. concluded that the coolant injection method has a large impact on the sealing performance of the cavity. Particularly, bore flow cooling (low radius injection) is more effective than purge flow cooling (high radius injection) for a given cavity design. Darby et al. [88], used the same facility to evaluate the effect of angling the purge injection. In the study, co-swirl (direction of the vane exit velocity), contra-swirl (opposite direction of the vane exit velocity), and axial injection were considered. The co-swirl injection method proved to be a much more efficient choice, improving cavity sealing by as much as 15% over the other two for the same flow rate. Darby et al. also showed an inflection present in the sealing effectiveness curve was less pronounced with the co-swirl injection than contra-swirl or axial injection.

Haulca et al. [78] also used the same facility as Darby et al. to study the effect of blades and vanes on ingress in gas turbines. Using a series of fast response pressure transducers, their study concludes the presence of time-resolved pressure events in the cavity are correlated to the inflection point of the sealing effectiveness curve when both vanes and blades are present. These structures were measured to be rotating at the disk speed with a frequency equal to half the blade passing frequency.

Presently, many seal geometries and cooling conditions show good agreement with simplified models, but under certain conditions, a combination of vane-blade interaction and cavity instabilities cause unpredicted behavior in the sealing effectiveness curve. The current study adds to the collection of work on rim events by building additional understanding of the critical vane-blade interaction through the use of time-resolved measurements in the presence of VTE flow. Unsteady pressure events are correlated to ingress in the cavity by incorporating both high frequency pressure and high frequency temperature sensors. Through the use of vane trailing edge flow, the effect of reducing the vane wake velocity deficit is analyzed and shown to be a key contributor to the strength of cavity instabilities.
3.4 Experimental Setup

The measurements for this study were collected in the Steady Thermal Aero Research Turbine (START) Lab at the Pennsylvania State University. This single-stage turbine is capable of engine-relevant operating conditions with true-scale engine hardware. The facility houses two 1.1 MW (1500 hp) compressors capable of a providing continuous mass flow of 12.2 kg/s (25 lbm/s) at a maximum pressure of 480kPa (70 PSIA) to the test section. An in-line 3.5 MW (4700 hp) natural gas heater is capable of raising and maintaining a gas temperature between 395 K to 672 K (250°F to 750°F).

For the current turbine test section design, three independent coolant lines (supplied by bleed air from one of the compressors) distributed to the VTE flow, purge flow, and tangential on-board injection (TOBI) flow (not used in this study). Each of these lines is independently controlled and metered with a discharge temperature of 273 K (32°F) after passing through a shell-and-tube heat exchanger. A full facility description was presented by Barringer et al. [19] with upgrades to the facility highlighted in Berdanier et al. [68].

3.4.1 Turbine Operating Point and Instrumentation

The test section for the experiment is shown in Figure 3-1. The single-stage turbine comprises a true-scale vane and blade representative of a modern aero gas turbine. The work presented in this paper focuses on the rim seal measurement plane denoted in Figure 3-1 as “time-resolved sensor location” (r/hr, = 0.981). The rim seal in this study is defined from the pre-swirl discourager (r/hr, = 0.958) to the inner diameter of the vane platform (r/hr, = 0.990). The minimum seal clearance, s, is at the interface of the rim seal and main gas path measuring s/hr = 0.1. Further details about the cavity geometry is given by Robak et. al [89].
Figure 3-1. Diagram of the start test section emphasizing the spatial location of high frequency sensors and their experimental integration.

In this study, purge flow enters the cavity axially through a set of 150 circumferentially spaced holes fed by a vane under platform plenum. Because the testbed incorporates true-scale engine hardware, the injected coolant flow can go into the cavity, through the blade’s internal flow path, or radially inward. Understanding this complex flow with engine parts at engine realistic conditions makes this dataset particularly unique.

The vane ring contains engine-run hardware as well as several “doublets” that were additively manufactured through a metal sintering process. Of the additive doublets: two contain internal passageways for low frequency pressure and gas concentration measurements and one is specifically designed for high-frequency measurements of temperature and pressure in the rim seal region. All of the vanes include VTE cooling in the form of radially spaced coolant holes spanning from the hub to the tip of the airfoil fed by a plenum radially outboard of the vane ring. Aside from the VTE cooling passages, the vanes and blades in this study operate as uncooled airfoils.

The test section is highly instrumented and the measured quantities relevant to this study are highlighted in Figure 3-1. The inlet plane total temperature and total pressure are measured through a series of sensors around the annulus approximately eight axial chords upstream of the vane. The main gas path and coolant flows are measured through Venturi flow meters upstream of the test section. Additional pressure and temperature sensors located at the inboard coolant plenum characterize the purge flow. The pertinent turbine operating conditions in this study are located in Table 3-1. These conditions are maintained over a period of hours as the test section thermally stabilizes. During steady data collection periods, the maximum deviation of specified operating conditions is 0.7% for all parameters listed in Table 3-1[90].
Table 3-1. Turbine Operating Conditions

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Vane Inlet Mach Number</td>
<td>0.1</td>
</tr>
<tr>
<td>Vane Inlet Axial Reynolds Number, Re_x</td>
<td>1.1 x 10^5</td>
</tr>
<tr>
<td>Blade Inlet Axial Reynolds Number, Re_x</td>
<td>1.1 x 10^5</td>
</tr>
<tr>
<td>Rotational Reynolds Number, Re_Ω</td>
<td>4.0 – 9.6 x 10^6</td>
</tr>
<tr>
<td>Density Ratio, ( \rho_p/\rho_{MGP} )</td>
<td>1.0 – 2.0</td>
</tr>
<tr>
<td>Relative Purge Flow Rate, ( \Phi_p/\Phi_{ref} )</td>
<td>0.1 – 1.3</td>
</tr>
<tr>
<td>Relative VTE Flow Rate, ( \Phi_{VTE}/\Phi_{ref} )</td>
<td>0.0 &amp; 0.4</td>
</tr>
</tbody>
</table>

This study compares the time-resolved measurements to cooling effectiveness measurements for an identical seal geometry previously outlined for the same test article [90]. The turbine operating conditions between this study and Monge-Concepción et al. [90] are set identical for data comparison. In Monge-Concepción et al., a CO₂ tracer gas was systematically added to various cooling flows to get the relative contribution from the VTE flow and the purge flow to the cavity cooling effectiveness, \( \varepsilon_{cc} \) given in Equation (3-1) as

\[
\varepsilon_{cc} = \frac{c_{rs} - c_{MGP}}{c_{s} - c_{MGP}}
\]  

(3-1)

where \( c_{rs} \) is the CO₂ concentration measurement in the rim seal, \( c_{s} \) is the CO₂ concentration of the coolant flow, and \( c_{MGP} \) is the concentration at the inlet. This methodology allows a mass-transfer analysis for the combined coolant effectiveness on the cavity. Note that multiple coolant sources contribute to the \( c_{s} \) term including the VTE and purge flows. The foundation of measurements collected by Monge-Concepción et al. enable the time-resolved pressure and temperature measured in this study to be correlated with cavity cooling effectiveness. Full descriptions of the concentration measurement methods in this testbed have been outlined in previous studies [68,72,90].

3.4.2 High Frequency Sensor Calibration

This study utilized both high-frequency pressure transducers and high-frequency temperature sensors to quantify time-resolved events. Figure 3-1 shows the design and integration of the sensors. The two high-frequency pressure transducers were standard piezo-resistive sensors spaced
one-fifth $P_v$ circumferentially. Between the two pressure transducers, a thin film resistive temperature detector was installed. This temperature sensor was designed and fabricated using nanofabrication infrastructure at Penn State [91]. The signals from both sensors were digitized at a rate of 100kHz, and a common shaft encoder signal was sent to each acquisition device to ensure signal alignment.

Figure 3-2(a) illustrates the calibration of the high frequency temperature sensor used in this study. Note, this sensor is insensitive to changes in pressure. Figure 3-2(b) shows the calibration curve for one of the high frequency pressure sensors; these devices are a primary function of pressure with secondary temperature effects. Therefore, a calibration surface was fitted that is both a linear fit with pressure and temperature. If the temperature compensation was not used for the high frequency pressure transducers, an error of $\pm5\%$ would have been introduced. To determine the pressure from the surface, the voltage was measured and the temperature was approximated using the nearby high-frequency temperature sensor.

![Figure 3-2. Example calibration results: a) calibration curve for the high frequency temperature sensor; b) calibration surface for the high frequency pressure transducer.](image)

The uncertainty in the low frequency measurements as well as the high frequency measurements were assessed accounting for the bias and precision error from each sensor. Table 3-2, lists the uncertainty of calculated parameters presented throughout the remainder of the paper.
Table 3-2 Measurement Uncertainties

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Total Uncertainty</th>
</tr>
</thead>
<tbody>
<tr>
<td>Main gas path flow rate, $\dot{m}/\dot{m}_{\text{ref}}$</td>
<td>±0.004</td>
</tr>
<tr>
<td>Shaft rotational speed, $\Omega/\Omega_{\text{ref}}$</td>
<td>±0.001</td>
</tr>
<tr>
<td>Pressures, $P/P_{\text{ref}}$</td>
<td>±0.001</td>
</tr>
<tr>
<td>Temperatures, T</td>
<td>±0.4 K</td>
</tr>
<tr>
<td>1.0 stage pressure ratio, $\text{PR}/\text{PR}_{\text{ref}}$</td>
<td>±0.005</td>
</tr>
<tr>
<td>Purge flow rate, $\dot{m}<em>{\text{P}}/\dot{m}</em>{\text{P,ref}}$</td>
<td>±0.018</td>
</tr>
<tr>
<td>Cooling effectiveness, $\varepsilon_{\text{cc}}$</td>
<td>±0.015 to ±0.025</td>
</tr>
<tr>
<td>Time Resolved Pressure, $P_{\text{pl,eq}}/P_{\text{ref}}$</td>
<td>±0.0035</td>
</tr>
<tr>
<td>Cooling Efficiency, $\theta$</td>
<td>±0.026 to ±0.061</td>
</tr>
<tr>
<td>Normalized Cooling Efficiency $\theta^*$</td>
<td>±0.0057</td>
</tr>
<tr>
<td>Normalized Pressure Coefficient, $C_p^*$</td>
<td>±0.00005</td>
</tr>
<tr>
<td>Nondimensional Purge Flow Rate, $\Phi_{\text{p}}/\Phi_{\text{ref}}$</td>
<td>±0.018</td>
</tr>
</tbody>
</table>

3.5 Vane Trailing Edge Flow Impact on Cavity Temperature

Previous tracer gas measurements by Monge-Concepción et al. [90] qualitatively showed that VTE flow is ingested into the under-platform wheel space, but the study did not directly quantify the thermal benefit or detriment to the cavity performance. Figure 3-3 displays both the cooling effectiveness, $\varepsilon_{\text{cc}}$ (filled markers) described in the previous section and the thermal efficiency, $\theta$ (open symbols) of the cavity defined as

$$\theta = \frac{T_{\text{mgp}} - T}{T_{\text{mgp}} - T_p}$$  \hspace{1cm} (3-2)

where $T_{\text{mgp}}$ is the main gas path temperature, $T$ is the rim seal vane temperature and $T_p$ is the purge inlet temperature. These parameters are shown as a function of the nondimensional purge flow rate, $\Phi_{\text{p}}/\Phi_{\text{ref}}$ where $\Phi_{\text{ref}}$ is the nondimensional coolant flow rate required to seal the cavity at the coolant injection plane. $\Phi_{\text{ref}}$ was chosen for consistency with previous studies and serves as a reminder that the radial location of the measurement is integral to the quantification of the sealing effectiveness.
Figure 3-3. Thermal efficiency and cooling effectiveness as a function of nondimensional purge flow rates with and without VTE flow.

The dashed lines in Figure 3-3 represented cases without VTE flow and the solid lines represent cases with VTE flow. This convention is followed throughout the paper. Figure 3-3 also denotes several key conditions in dashed boxes that will be referenced throughout the paper. The three main sealing regimes are represented as NS (not sealed), PS (partially sealed), and FS (fully sealed). The partially-sealed region is further divided into three subsections for subsequent comparison: PSa, PSb, and PSc. Accompanying the experimental measurements, Figure 3-3 contains the CFD predictions for PSb with and without VTE flow shown as a star and diamond respectively. Note the agreement between the experiment and computation. These results will be discussed in further detail later.

Several key points are illustrated through Figure 3-3. First, the presence of VTE flow increases $\varepsilon_{cc}$ for all flow rates tested, as highlighted by Monge-Concepción et al. [90]. This observation independently confirms ingestion of VTE flow into the under-platform region. The presence of the VTE flow acts to cool the cavity across the range of $\Phi_p/\Phi_{ref}$, as shown Figure 3-3 by an increase in $\theta$ of 7-10%.

The VTE presence allows two mechanisms to work together to impact the cavity temperature. First, when the coolant flow is injected into the vane, the entire airfoil temperature decreases due to conduction. The conduction path through the vane and into the cavity impacts the thermal efficiency. Second, the additional airflow in the cavity changes the fluid dynamics, increasing heat transfer by convection. Additionally, this ingested VTE flow displaces hot main gas path flow from being ingested. Although the present study cannot distinguish the individual contributions of the VTE flow, the combined effect demonstrates that cooled turbine designs can
accommodate ingress that is not necessarily a detriment – an observation that may be counter to traditional cavity design criteria.

The primary goal of cavity design is to prevent ingress of hot gas path air to keep critical inner radius components cool. Concentration measurements quantify the cavity sealing performance, but must infer how sealing effectiveness relates to cavity temperature. Therefore, a cavity should be quantified by coupling cooling effectiveness measurements with thermal efficiency measurements. This dataset provides that link under a realistic engine configuration. The added benefit of the thermal sensor links ingestion to temperature, more clearly connecting ingestion to component durability.

There is a range of partially-sealed (PS) purge flow rates, denoted in Figure 3-3 as the inflection region, which should be of particular interest to engine designers. In this region, increasing the coolant flow rate has a minimal impact on the cooling effectiveness of the cavity. This phenomenon has also been identified in previous studies [78,92], and it has been associated with time-varying flow variations in the cavity. Therefore, it is important to understand how the steady cooling effectiveness and thermal efficiency measurements relate to time-varying events.

Furthermore, the addition of VTE flow shifts the onset of this inflection region to higher purge flow rates in Figure 3-3. This observation suggests that the VTE flow, which affects the vane-blade interaction, also influences ingestion mechanisms. Current cavity effectiveness models [76,77] do not account for external cooling flows, such as VTE flow, nor do they explain the inflection region. Nonetheless, most modern gas turbines include VTE flow to control the velocity deficit created in the vane wake and cool the trailing edge of the vane. Therefore, it is critical to understand both steady and time-resolved interactions of VTE flow with the cavity.

3.6 Correlation of Pressure to Temperature Wave Propagation

The time-resolved pressure and temperature measurements allows a methodology of correlating cavity pressure and temperature to ingress. Following this methodology, it is shown that fluidic structures with the largest impact to ingestion are present in the PS region shown in Figure 3-3.

Figure 3-4(a) presents normalized temperature, $T'$, and normalized pressure coefficient, $C'_p$, over one rotor revolution for the partially-sealed case, PSb, with VTE flow as shown in Figure 3-3. This single test condition was chosen to highlight flow features near the inflection region and serves as an example for the analysis. The datasets were normalized by the minimum and maximum of the dataset such that the normalized quantity, $N'$ is represented as
\[ N' = \frac{N - N_{\text{min}}}{N_{\text{max}} - N_{\text{min}}} \] (3-3)

where \( N \) represents the parameter of interest, such as rim seal vane metal temperature and pressure coefficient. It should be noted that the data presented in this section were filtered during post-processing using a zero-phase low pass digital filter at \( f/f_D = 20 \) to remove the blade passing events and other high frequency noise contributions. Through this filtering process, the remaining low-frequency oscillation patterns were identified.

Several significant flow characteristics are shown in the time-resolved pressure and temperature displayed in Figure 3-4. First, there is a driving frequency at approximately \( f/f_D = 5 \) as seen by the five peaks over the rotation in both pressure sensors (\( C_{pA}' \) and \( C_{pB}' \)) as well as the temperature sensor (\( T' \)). Second, the temperature and pressure of these peaks are out of phase. The temperature phase lag, \( \Psi_T \), is defined as the shift of the temperature trace to the pressure trace in degrees and is explicitly called in Figure 3-4(a). \( \Psi_T \) is an important parameter because it correlates pressure to temperature. Specifically, when the pressure and temperature are out of phase, it indicates time-varying ingress in the cavity. One purpose of the coolant is to pressurize the cavity, sealing it from main gas path ingestion. This means a local maximum of pressure indicative of coolant should coincide with a local minimum in temperature, as shown in Figure 3-4(a).

Figure 3-4(b) displays a normalized thermal efficiency and normalized pressure coefficient for the same data as Figure 3-4(a). All of the traces in Figure 3-4(b) are in phase because the thermal
efficiency of the cavity increases as the high-pressure coolant passes, following the definition of thermal efficiency in Equation (3-2). When the data are nondimensionalized in this manner, regions of ingress (minima) followed by regions of improved thermal performance (maxima) can be identified from the sensors in the cavity. These regions of thermal cycling contribute to the time-varying sealing effectiveness in the cavity.

In Figure 3-4(b), a small phase lag is still present between the pressure signals. This minor phase change corresponds to the angular separation between the two pressure sensors as shown in Figure 3-1. As the pressure waves propagate, it will contact sensors sequentially in the rotation of the pressure wave movement. Particularly, since the pressure sensors were at known locations, it is possible to get the speed of the cell propagation by calculating the phase shift between pressure sensors, $\Psi_p$. Monge-Concepción et al. [93] analyze a dataset similar to the present study to determine the speed and cell count of the time-resolved pressure events and their impact on cavity performance. Although these events could be calculated from the two pressure signals, the phase between the temperature sensor and pressure transducer could not reliably be used to calculate the speed of these cells since the temperature sensors measure the conduction through the part as well as the convection at the surface. The presence of these pressure cells (instabilities) is referenced throughout the current paper, although it is not the primary focus of this study.

To further quantify $\Psi_T$, a spectral analysis was performed. A total of 18 different test conditions over a range of flow rates were analyzed, and each dataset contained approximately 500 revolutions. The spectral analysis included an iterative zero padding routine with a convergence criterion of 0.1% of the maximum normalized peak value to ensure consistent peak resolution between cases. From this spectral pressure analysis, peaks were selected such that any normalized peak with an amplitude greater than 1% of the mean value was chosen as a potential driver of ingestion. Using the phase information contained in the spectral analysis, $\Psi_T$ can then be calculated for those peak frequency values. Figure 3-5(a-c) outlines these steps in the analysis for the same dataset shown in Figure 3-4.
Figure 3-5. a) DFT of normalized pressure coefficient and arbitrary phase for peak frequencies. b) DFT of normalized temperature and arbitrary phase for selected frequencies. c) The pressure to temperature phase shift of selected peak frequencies as a function of normalized disc frequency.

Figure 3-5(a) and Figure 3-5(b) display the discrete Fourier transform (DFT) from the normalized pressure coefficient and temperature respectively as a function of engine order ($f/f_D$). Stems denote the relative phase of the signal at peak frequencies in Figure 3-5(a) on the right.
ordinate. Figure 3-5(b) utilizes identical frequency values from Figure 3-5(a) to calculate the relative phase values for the normalized temperature. Using the two 𝜈 values from Figure 3-5(a-b), the difference is calculated to correlate pressure to temperature and is denoted as 𝜈Τ in Figure 3-5(c). This analysis is possible due to the cross-correlation theorem.

One advantage of this method over other correlation methods is that 𝜈Τ can be computed independently over a range of frequencies, a capability that is highlighted in Figure 3-5(c). In the range of f/f_D = 4 to f/f_D = 7, the pressure and temperature are out of phase, indicating unsteady ingestion occurring across this region of frequencies. Correlating in this way provides the critical frequencies driving the performance of the cavity to be separated from those with a minor impact.

The analysis described above was performed for three cases over a range of sealing conditions: NS, PS_b, and FS as shown in Figure 3-3. Temperature phase lag, 𝜈Τ, for these three different cases is presented in Figure 3-6 as a function of normalized frequency, f/f_D. The dashed lines with open symbols represent cases without VTE flow, and the solid lines with closed symbols represent cases with VTE flow.

![Figure 3-6. Lag between pressure and temperature traces as a function of normalized frequency.](image)

Some primary conclusions can be drawn from Figure 3-6. First, at the NS condition, 𝜈Τ is less than 180° across all frequencies. This result is not indicative of time-varying ingress. Although ingress occurs at the NS case (as seen through the steady ε_cc values in Figure 3-3), time-varying structures do not contribute to the sealing performance at the rim seal plane. Conversely, the PS_b case (circles in Figure 3-6), indicates time-varying ingress through temperature and pressure signals that are out of phase. As shown in Figure 3-4(a-b), this ingress is related to presence of time-resolved pressure fluctuations and will be discussed in detail in the following section. Finally, for
the FS case (triangles), there are low-frequency components that contribute to ingestion to a lesser degree, particularly around $f/f_D = 2$, but this region shows fewer out-of-phase frequency contributions than the condition in the inflection region. By relating these findings to the steady sealing effectiveness data, the evaluation method highlighted in Figure 3-6 shows the inflection region exhibits key frequencies that contribute to ingress and help to explain the deviation from simplified models.

Previous experimental studies have coupled unsteady pressure measurements to the appearance of rotating structures in the cavity region, but could not directly link them to cavity ingestion or the thermal performance of the cavity [78]. This missing relation is most apparent in Figure 3-7 which displays sealing effectiveness measurements taken by Hualca et al. [78]. The purpose of Figure 3-7 is not to compare the differences from Hualca et al. [78] to the current study, but to showcase that a cavity instability does not necessarily lead to an inflection in sealing effectiveness. Figure 3-7 presents sealing effectiveness as a function of $\Phi^*$ normalized by the minimum $\Phi$ value to seal the cavity through

$$\frac{\Phi^*}{\Phi_{\text{min}}} = \frac{\Phi_p\Phi_{p0}}{\Phi_{\text{min}}} \quad (3-4)$$

where $\Phi_p$ is the nondimensional purge flow rate, $\Phi_{p0}$ is the minimum nondimensional purge flow rate required to start sealing the cavity (accounts for leak paths), and $\Phi_{\text{min}}$ is the minimum nondimensional purge flow rate required to seal the cavity at the measurement plane. Note, this is different from $\Phi_{\text{ref}}$ which is the minimum nondimensional purge flow rate required to seal the cavity at the injection plane.
Hualca et al. [78] compares the sealing performance for a double overlap seal with blades (circles) and without blades (triangles). Using high-frequency pressure transducers, Hualca et al. were able to measure time-resolved pressure structures in the cavity for both test cases. However, the sealing effectiveness curve only showed an inflection region in the presence of blades. Thus, even though the pressure structures occur for both conditions, they affect the sealing effectiveness curve in different ways. The interaction of the rotating airfoil in the vane and blade case drives pressure structures to cause ingress. Hualca et al. details that the presence of blades increases the swirl in the cavity creating an instability responsible for the nonmonotonic sealing curve. However, limited details are provided to explain why those structures contribute to ingestion only in the presence of blades and do not create an inflection in the sealing effectiveness curve without blades.

The sealing effectiveness curve for the current study (zero VTE flow case) is also shown in Figure 3-7 through green squares. In addition, an orifice model [68] was approximately fitted to the current study experimental data using a ratio of discharge coefficients, $\Gamma=5.2$. Note that because the orifice model must be monotonically increasing, the model fits the data at low and high $\Phi^*/\Phi_{\min}$ values, but does not capture the inflection region in the partially sealed region.

To supplement the sealing effectiveness, the $\Psi_{T_{\max}}$ parameter (filled stars), defined as the maximum lag across all frequencies for a particular $\Phi_p/\Phi_{ref}$ value is plotted on the right ordinate. In Figure 3-7, the additional information from a high frequency temperature sensor allows for a classification of nondimensional purge flow rates for which the time-varying events impact sealing effectiveness – a relationship illustrated by the out-of-phase $\Psi_{T_{\max}}$ values corresponding to large deviations from the model around the inflection region.
As a corollary, the addition of a high-frequency temperature sensor prevents an incorrect inference that pressure cells cause ingestion from an inflection in steady sealing effectiveness data. In certain cases, such as the vane-only configuration presented by Hualca et al, rotating structures can be present without causing an inflection in the sealing effectiveness. Quantifying $\Psi_{T_{\text{max}}}$ through time-resolved pressure and temperature sensors enables coolant rates at which ingestion occurs and the frequencies driving the ingress to be determined. The additional temperature sensor can also relate the existence of pressure structures to the thermal efficiency of the cavity.

### 3.7 Time Resolved Behavior in Inflection Region

The previous section compared NS, PS$_b$, and FS cases in Figure 3-3 claiming that time-resolved pressure and temperature effects contribute to main gas path ingress in the PS$_b$ region. For this reason, the current section focuses on measurements in the inflection region: PS$_a$, PS$_b$, and PS$_c$. In particular, the relative amplitude of the time-resolved measurements is analyzed. Furthermore, VTE flow is introduced as an additional variable to investigate the impact of the additional coolant on the amplitude of time-varying cavity events.

Discrete Fourier transforms for the three test cases of interest are shown in Figure 3-8(a-f). These DFTs were computed using the same iterative zero-padding routine described for Figure 3-5. However, after all cases were computed, the data were normalized by the absolute maximum amplitude across all test cases. Unlike $N'$, which normalized across a single flow condition, $N'$ is normalized across all test conditions for relative comparison. For each graph, the dashed lines represent cases without VTE flow, and the solid lines represent cases with VTE flow.
The inflection region data presented in Figure 3-8(a-f) exhibited the largest amplitude peaks over all evaluated test conditions. Figure 3-8(a-f) shows that large amplitude values in $C_p^*$ correspond to large amplitudes in $\theta^*$, verifying that these pressure variations are moving pockets of cold and warm air. The experimental data appear as discrete spikes of activity near a normalized frequency of $f/f_D=5$. This finding is similar to Hualca et al. who attributed the various peaks to be a fluid instability switching between integer pressure cells [78]. However, in contrast to Hualca et al., no clear relationship between the low frequency spectral peak locations and particular airfoil counts or hardware were identified. More analysis is needed to determine the cause of the frequencies. The effect of VTE on the time-varying events is also evident comparing the peak amplitudes in Figure 3-8(a-f). Specifically, the VTE flow acts to suppress the time-varying instabilities.

### 3.8 Correlating Time-Resolved Measurements with Cooling Effectiveness

The previous section displayed the entire frequency spectra for selected purge flow rates distributed across the inflection region. This section connects time-varying events to cavity performance indicators by utilizing the maximum amplitude of the frequency spectra.

Figure 3-9 displays the maximum amplitude normalized cavity efficiency and maximum amplitude normalized pressure coefficient as a function of $\Phi_p/\Phi_{ref}$. Additionally, the cooling effectiveness from Figure 3-3 is provided as an overlay on the graphs for comparison. Figure 3-9(a) represents a purge-only configuration without VTE flow, whereas Figure 3-9(b) includes the
addition of VTE flow. Furthermore, Figure 3-9 includes data from three high-frequency sensors (two pressure sensors and one temperature sensor). The independent pressure sensors, which span one fifth of a vane pitch, qualitatively display the same trend and normalized amplitudes illustrating the repeatability and relative insensitivity to circumferential location.

Figure 3-9. Max amplitude of normalized pressure coefficient and cavity temperature efficiency plotted with cooling effectiveness across cooling rates. (a) Purge flow only condition, $\Phi_{\text{VTE}}/\Phi_{\text{ref}} = 0$; (b) Purge flow with the addition of VTE, $\Phi_{\text{VTE}}/\Phi_{\text{ref}} = 0.4$.

At low $\Phi_p/\Phi_{\text{ref}}$ values in Figure 3-9, the maximum amplitude of unsteady features is relatively small. This result is consistent with the phase analysis from Figure 3-6, which showed $\Psi_T$ does not indicate time-varying ingress at the NS condition. However, when approaching the inflection region (PSa and PSb in Figure 3-3), the largest amplitude features are present and have been shown to indicate ingestion. This time-varying ingestion causes the reduction of sealing performance indicative of the inflection point. As $\Phi_p/\Phi_{\text{ref}}$ increases past the inflection point, the time-resolved amplitudes decrease, and the sealing effectiveness returns to a purely increasing behavior. This behavior was also supported by the phase analysis in the FS region (Figure 3-6).

In both Figure 3-9(a) and Figure 3-9(b), the normalized amplitudes reach a peak value coincident with the inflection point, as defined by the $\Phi_p/\Phi_{\text{ref}}$ value corresponding to the lowest slope value between neighboring points. Because the inflection point occurs at different nondimensional purge flow rates for the two cases, this finding directly connects the inflection location to maximum unsteady amplitude, not a particular $\Phi_p/\Phi_{\text{ref}}$ value.

Comparing Figure 3-9(a) to Figure 3-9(b), the presence of VTE flow also reduces the amplitude of the time-resolved pressure and temperature. Quantitatively, the maximum peak between the two cases is reduced by 50% in the temperature and 40% in the pressure. Interestingly, the inflection of the curve is equally pronounced for both cases, with and without VTE flow, despite
the reduction to the unsteady amplitudes. Although this approach of linking a single frequency and amplitude to the sealing effectiveness may be an oversimplification of the complex flow physics present in the rim seal region, it provides a method to connect time-varying events with steady sealing performance. Connections such as these are important to developing real-time turbine health monitoring [94].

3.9 Use of Computational Modeling to Support Experimental Findings

Computational modeling of engine-realistic geometry can provide insight into the flow physics that are captured by limited measurements in experiments. The purpose of this section is to use CFD simulation results to better understand the experimental measurements previously presented. In particular, a comparison between the PSb cases with and without VTE flow will be examined using an Unsteady Reynolds-Average Naiver-Stokes (URANS) simulation. A brief description of the URANS approach is provided here for convenience, and further details are outlined by Robak et al. [89].

The computational domain of the turbine stage model consists of a quarter-wheel circumferential sector with cavity flows and engine-realistic leakage paths such as those presented in Figure 3-1. The leakage flowrates were set using a 1D pressure network to obtain the mass flow. This domain consists of approximately 22 million elements and employs a pressure-based solver with a k-ω SST turbulence model. The geometry represented in the simulation is a direct match for the airfoils and underplatform hardware used in the present study. The simulations were converged to a steady state solution. Then, time-accurate simulations were conducted for five-wheel revolutions before decreasing the time step by a factor of 5 for an additional three-full revolutions. Simulations were considered converged when the pressure amplitude of various tracer points was within 5% of the previous revolution value. Additional details are described in Robak et al. [89].

Although not explicitly presented in this discussion, the time-discrete results confirm low pressure cells rotating around the annulus with number and speed dependent on purge flow rate. When VTE flow is added to the simulation, the strength of these pressure structures decreases, matching the trend in Figure 3-9. More information about these structures is presented in a complementary study [93].

Time-averaged results representing one full-rotor revolution at the end of the converged period are analyzed here. Figure 3-10 shows a cross section of the cavity region at a vane phase consistent with the measurement location of PA as described in Figure 3-1 for the case of PSb with VTE flow. The contour displays the fraction of VTE flow in the cavity. Note the contour scale in Figure 3-10 has been reduced to a range from 0 to 0.1 (10% mass fraction) to highlight the trends. As shown
previously in Figure 3-3, the CFD sealing effectiveness was within 7% of experimental values. The added benefit of the simulation allows the individual contributions from VTE and purge flow to be distinguished.

The ingestion of VTE flow identified in Figure 3-10 further validates the increase in thermal cavity efficiency measured during the experiment and illustrated in Figure 3-3. This ingestion occurs in part to a rise in MGP pressure stemming from the additional VTE flow. Because the VTE flow accounts for only two percent of the MGP flow, a majority of the VTE flow is quickly mixed out by the MGP flow. However, near the endwalls, the VTE flow is allowed to propagate further downstream allowing the VTE flow to enter the cavity in relatively high concentrations.

![Contour of VTE ingestion into cavity for PSb configuration with VTE flow.](image)

The ingestion of VTE flow identified in Figure 3-10 further validates the increase in thermal cavity efficiency measured during the experiment and illustrated in Figure 3-3. This ingestion occurs in part to a rise in MGP pressure stemming from the additional VTE flow. Because the VTE flow accounts for only two percent of the MGP flow, a majority of the VTE flow is quickly mixed out by the MGP flow. However, near the endwalls, the VTE flow is allowed to propagate further downstream allowing the VTE flow to enter the cavity in relatively high concentrations.

The addition of VTE flow into the cavity changes the fluid dynamics of the cavity leading to suppression of the time-resolved pressure and temperature peaks as seen in Figure 3-9. To further
address this observation, Figure 3-11 shows contours of swirl ratio for the rim seal region of the cavity (denoted by black dashed box in Figure 3-10). The swirl ratio is defined as

\[
\beta = \frac{V_\phi}{\Omega_D r}
\]

(3-5)

where \(V_\phi\) is the tangential velocity, \(r\) is the radial location, and \(\Omega_D\) is the angular speed of the disk. The swirl ratio has been previously identified as a useful parameter for locating mismatches in tangential velocity that could lead to formation of instabilities [95]. In the axial space between the vane and blade in the MGP, the swirl ratio is greater than unity. Physically, the air downstream of the vane must be moving with a tangential velocity capable of spinning the rotor. However, in this study where discrete coolant holes inject air axially into the cavity, there is a large discrepancy between the swirl ratio in the MGP and the cavity region.

Previous authors have shown the velocity gradient is a key driver of Kelvin-Helmholtz instabilities [96]. Because the rim seal of the cavity acts as a buffer region of intermediate swirl ratio separating the MGP from the injection plane, it is of critical importance to instability formation. In Figure 3-11, there is an increase in the mean swirl ratio with the addition of VTE flow caused by high-swirl VTE fluid propagating downstream near the hub end wall. Because the cavity velocity profile is bounded by the high swirl ratio of the MGP and the axial purge flow injection (zero swirl), the increased swirl ratio stemming from VTE flow ingestion lowers the velocity gradient throughout the cavity and therefore the instability potential. For this reason, the addition of VTE flow suppresses the instability strength and causes the onset of the VTE flow instability to shift relative to the non-VTE case.
3.10 Conclusions

This study utilized a single-stage turbine operating with true-scale engine hardware at relevant operating conditions to quantify the effect of coolant flows on the performance of the rim seal region. The steady and time-resolved sealing behavior was quantified using high-frequency pressure and temperature sensors. These data were then compared with CO₂ tracer gas cooling effectiveness measurements for identical conditions.

The comparison of time-resolved measurements with traditional tracer gas sealing quantification shows that VTE flow is ingested into the cavity and measured an increase in the cavity thermal efficiency to be 7-10% with expected contributions from conduction and convection.
effects. This finding confirms that VTE provides a thermal benefit to the cavity and should be accounted for in cavity effectiveness modelling to prevent excess use of purge flow.

Correlation of the pressure coefficient and normalized temperature signals indicate ingress occurs when the two signals are out of phase. This method allows separation of time-varying events responsible for ingestion from those less detrimental to cavity performance. For this dataset, it was shown that nondimensional purge flow rates near the cooling effectiveness inflection region create a fluid instability responsible for time-varying ingestion.

A Fourier transform analysis of normalized thermal efficiency and pressure coefficient measurements in this inflection region indicate the presence of low frequency pressure and temperature peaks corresponding to rotating cells in the cavity. The amplitude of these peaks was suppressed by as much as 50% when VTE was present. Using computational simulations, it was shown that the VTE ingestion affects the swirl ratio in the cavity which changes the velocity profile near the rim seal. Because Kelvin-Helmholtz instabilities arise with high velocity gradients, the ingested high-swirl VTE reduces the velocity gradient in the rim seal, suppressing the instability strength. Likewise, swirled coolant could also be used to reduce the velocity gradient.

The maximum normalized amplitude from the spectral analysis was related to the cooling effectiveness where the peak value coincides with the inflection point in the sealing effectiveness curve. This coupling confirms that these unsteady pressure and temperature events are driving time-varying ingress which, in turn, affects the steady sealing performance. Further, at low and high purge flow rates outside the sealing effectiveness inflection region, these time-varying events are partially suppressed. Comparisons with a well-defined ingress model shows that the inflection region deviated from predicted trends in locations where this time-varying behavior is most prevalent.

Overall, the results of this study provide valuable information about how cavity instabilities can drive ingestion under certain conditions and how VTE flow affects cavity performance and the onset of instabilities. This work shows novel application of temperature measurements to provide previously unknown relationships between cavity instabilities and sealing performance. Further, this work elicits research to predict the onset of the instabilities and develop design tools that work to mitigate their effects.
4 APPLICATION OF 3-OMEGA METHOD FOR THIN-FILM HEAT FLUX CALIBRATION

4.1 Abstract

Double-sided thin-film resistance temperature detector (RTD) heat flux gauges (HFGs) are commonly used to characterize heat transfer rates in high-heat flux environments with complex flow features. These gauges comprise two thin-film RTDs on opposing sides of a dielectric. To deduce accurate heat flux, the RTDs must be properly calibrated and the material properties of the dielectric must be characterized. This study presents a complete gauge characterization method for sensors of this type by applying standard calibration procedures with specially-designed RTDs capable of utilizing the 3-omega method. The 3-omega method quantifies the thermal conductivity and thermal product of a material by measuring the response of a specially-designed heater/thermometer deposited on the substrate. This study shows the 3-omega method enables RTD calibrations and thermal property determination over a range of temperatures for individual gauges, reducing the uncertainty in calculated heat flux. Although the method is quite general, this study utilized platinum RTDs with a polyimide dielectric, which is common in turbomachinery applications. The thermal properties obtained through this method agree with previous characterization efforts; however, discrete characterization of seven gauges shows that gauge-to-gauge variation in the dielectric could influence measured heat flux by as much as 30%. This study also builds the framework to characterize the thermal conductivity of the adhesive layer beneath the gauge which is necessary to mount the sensors to the test article. Although often uncharacterized, the adhesive thermal conductivity has a significant impact on matching experimental measurements to simulations. Additionally, this study found that if the thermal conductivity of the dielectric is constant (an assumption that holds for the present study), an in-situ RTD calibration can be performed. In-situ RTD calibration and traditional method RTD calibration agreed to within 0.1%. Overall, this work has practical implications in obtaining high quality measurements from heat flux gauges of this type.

4.2 Introduction

Heat flux measurements aid in understanding the time-resolved thermal performance of a system and are critical to improving thermal designs in a wide range of applications. Although several types of heat flux measurement devices exist, this paper focuses on the calibration of

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double-sided thin-film resistive temperature detector (RTD) heat flux gauges (HFGs). Shown in Figure 4-1, sensors of this type are composed of two RTDs on opposing sides of a dielectric and have been established for high heat flux environments in complex flow fields [13,35].

![Figure 4-1. Schematic of double-sided heat flux gauge.]

Double-sided thin-film RTD HFGs use thermo-electrical calibrations and material properties to deduce a heat flux. The RTDs are supplied by a small excitation current, and the corresponding resistance is related to a temperature through an electrothermal calibration traditionally completed in a stable temperature environment such as a scientific convection oven or oil bath. This calibration allows transformation of the measured quantity (voltage or resistance) of the platinum RTD to temperature. The temperature traces from the top and bottom can then be used to deduce heat flux by solving the unsteady conduction equation with the material properties of the dielectric.

The ambiguity of these sensors is influenced by the RTD calibration accuracy as well as the accuracy of the thermal properties of the substrate. More specifically, highlighted in Figure 4-1, the coefficient of resistance of the RTDs ($\alpha_{R,1}$ and $\alpha_{R,2}$) as well as the thermal product $(\rho c_k)^{0.5}$ and thermal conductivity over thickness ($k_s/d_s$) of the substrate must be known to properly deduce time-resolved heat flux [16]. Although previous researchers have successfully implemented appropriate characterization techniques for these parameters [13,14,43,45], no technique currently characterizes the thermal properties for each individual HFG. Instead, bulk material properties are tested and assumed to represent the sensor dielectric. However, batch-to-batch variations and mismatches in characterization and gauge operation temperature can lead to inaccuracies in the thermal properties of up to 20% which propagates to errors in heat flux measurements [91]. Additionally, no existing method allows for an in-situ calibration of the RTDs which could drift as the metal anneals or degrades in high temperature environments.

The 3-omega method [97,98] provides a new approach to improve upon traditional HFG calibration techniques. The 3-omega method quantifies the thermal conductivity and thermal
product of a material by measuring the response of a specially-designed heater/thermometer deposited on the substrate. Because thin film RTD HFGs require deposition of metal strips, this technique lends itself well for characterizing the thermal properties of the dielectric. Furthermore, since the 3-omega method links the electrothermal response of the RTDs to the substrate thermal properties, an in-situ calibration of the RTDs is possible. Moreover, since RTDs on both sides of the dielectric are used, it is possible to obtain the thermal conductivity of the adhesive layer ($k_a$) as well which plays a key role in matching simulations to experimental results. Having the capability to check and alter calibrations in-situ on a per gauge basis will increase the accuracy of the measurement and save significant time and effort in implementing measurements of this type.

This paper presents a methodology for how the 3-omega technique can be used as an alternative to traditional thermal characterization of the dielectric and to supplement traditional RTD calibrations. First, the background information on the technique is presented. Next, the design and fabrication of sensors is introduced, and data reduction methods are illustrated. This procedure is then applied to seven double-sided thin-film RTD HFGs and thermal property results are obtained across a range of temperatures and compared to traditional method values. Finally, the technique is expanded to demonstrate in-situ calibrations.

4.3 Literature Review

Thin film RTD HFGs have been used for decades in the turbomachinery discipline to characterize the heat transfer in complex flow fields. Typically, these sensors have used polyimide as the dielectric material and a platinum deposition on the order of nanometers for the metal deposition [13,14,28,35,43]. These selections allow the sensors to be flexible enough to wrap around complex airfoil geometries with a time response on the order of kilohertz which is critical to characterize harmonics of blade passing events present in turbomachinery flows [23,28,99–102]. Noteworthy research by a host of institutions over the last several decades has advanced the design and fabrication of these type of sensors [13,14,43,91,99]. For this reason, the current study focuses on platinum RTD elements on both sides of a polyimide dielectric and their application to turbomachinery. However, the general process holds for other materials as well. This paper demonstrates the use of the 3-omega technique for the new purpose of creating a more accurate thin-film RTD HFG. For that reason, the literature review will draw from both 3-omega literature, nanoscale platinum film studies, and previous HFG applications.

A stable calibration is necessary for an RTD to obtain accurate measurements. This calibration depends on the temperature coefficient of resistance, $\alpha_R$ (constant for platinum), as well as the reference resistance of the RTD ($R_{ref}$). The bulk platinum $\alpha_R$ value is 3.9x10^{-3} (°C^{-1}) [53] and
directly characterizes the sensitivity of RTDs. However, residual stresses and electron scattering at grain and film boundaries resulting from the deposition process for thin film RTDs cause the coefficient of resistance of thin film sensors to deviate from bulk values.

Tiggelaar et al. [103] tested the stability of thin platinum RTDs deposited onto silicon wafers showing an annealing process with a ramp of 10°C/min up to 950°C relaxes the residual stresses while increasing the grainsize of the platinum film. Similarly, Chung and Kim [104] showed annealing a thin platinum film at 1000°C for 2 hours resulted in stable physical and electrical structures as well as $\alpha_R$ values near the expected bulk platinum property. Zribi et al. [105] further analyzed the annealing of thin film RTD HFGs using glass as the dielectric. Zribi et al. obtained stable thin platinum $\alpha_R$ values through an annealing process of 250°C, but with values of $\alpha_R$ roughly one-third that of bulk values.

In many cases, including the present study, the annealing process is limited by the maximum allowable temperature for the dielectric. As a consequence, manufacturers of flexible RTD HFGs are constrained to $\alpha_R$ values lower than bulk platinum [91] due to the use of polyimide as the dielectric. However, accurate measurements have been demonstrated from stable and repeatable coefficient of resistance values that deviate from bulk values [91] so long as the $\alpha_R$ value is properly characterized.

Apart from RTD calibrations, the thermal properties of the dielectric, including $(k_s/d_s)$ and $(\rho c k)^{0.5}$, must be known to achieve accurate heat flux measurements. Experimentally, these lumped parameters can be quantified by measuring the thermal response of the thin film RTD HFG to a known heat source. Many types of heat sources have been previously used, but a convective heat source has been found to be the most reliable and closest to experimental conditions [43,45]. Lumped parameter thermal property determination allows reliable data at a particular temperature, but is rarely expanded for a range of temperatures. Alternatively, these parameters can also be independently tested [14,91] through a number of standards [57,106]. Both the lumped parameter and independent methods are valid ways of quantifying these thermal properties, but neglect variations by individual thin-film RTD HFGs.

One experimental method that could be used in place of the traditional standards is the 3-omega method for thermal conductivity. Developed by Cahill et al. [97], this method quantifies the thermal conductivity of a material by measuring the harmonic response to a heater. Through this technique, it is also possible to determine the thermal diffusivity and therefore the thermal product of the material [56]. The 3-omega method relies upon characterizing the relationship between the coefficient of resistance of the heater and the thermal properties of the dielectric, thereby making it particularly useful for thin film RTD HFG calibration.
Below the sensor, an adhesive layer is necessary to bond the thin film RTD HFG to the test article. In addition to the dielectric thermal properties, the thermal properties of the adhesive layer are vital to scaling results to engine conditions as well as matching experiment to models. Ni et al. [107], compared experimental data from thin film RTD HFGs to an engine simulation. When accounting for the presence of an insulating adhesive layer, experimental results better matched numerical models. Through the use of both sides of a double-sided thin-film RTD HFG, it is possible to characterize the material properties of the underlying adhesive.

Although the application of the 3-omega process is novel to thin film RTD HFGs, bidirectional 3-omega sensors have been successfully used in the past to quantify underlying materials. Lubner et al. [108] utilized the principles of the 3-omega method to create bi-direction sensors. In this configuration, sensors first quantify the backing material; then, a sample with an unknown thermal conductivity is placed on the exposed sensor. Through prior quantification of the backing material, the unknown material properties can then be deduced. A comparison can be drawn between the Lubner et al. approach and the application of double-sided thin-film RTD HFGs: first, the top sensor quantifies the thermal properties of the dielectric; then, the bottom sensor is used in conjunction with the thermal properties of the top sensor to quantify the underlying material.

The current study adds to the available literature by combining techniques from various disciplines to create a unique calibration method for double-sided thin-film RTD HFGs. This technique can be utilized across a variety of disciplines to better quantify heat flux, leading to more robust and efficient thermal systems.

### 4.4 Theoretical Framework for 3-omega Method

The theoretical framework for the 3-omega technique specific to HFGs is summarized in this section, and the reader is directed to previous works for further details and derivation of the governing relations [56,97,98]. In general, the electrical and thermal transfer functions of a system can be related by using a combined heater/thermometer to excite the system and measure the response. Following the procedure outlined by Dames and Chen [98], the measured quantities of harmonic voltage drop across the heater \( V_{n0,\text{rms}} \), the coefficient of resistance of the heater \( \alpha_R \), the zero-current resistance of the heater \( R_e \), and the current driving the heater \( I_{1,\text{rms}} \) can be related to the electric transfer function through Equation (4-1):

\[
\frac{V_{n0,\text{rms}}}{\alpha_R R_e I_{1,\text{rms}}} = X_{n0}(\omega_{1,\text{rms}}) + j Y_{n0}(\omega_{1,\text{rms}})
\]  

\[ (4-1) \]
where $X_n\omega(\omega_1\omega)$ is the in-phase component of the transfer function and $Y_n\omega(\omega_1\omega)$ is the out-of-phase component. Both components can be a function of the current-driving frequency, $\omega_1\omega$ and are denoted for a specific harmonic, $n\omega$. The components of the electrical transfer function contain valuable information about the thermal properties of the system.

As presented here, Equation (4-1) can be applied to a variety of geometries and harmonics. The geometry relevant to this study is a line heater above a substrate and will focus on the in-phase third harmonic system response. This formulation is considered the traditional case for the 3-omega method [97] and has been shown to produce robust measurements of thermal conductivity [98].

To apply the 3-omega technique, a properly-designed heater/thermometer must be supplied with a sinusoidal current at frequency $\omega_1$ which introduces Joule heating and a corresponding temperature rise at a harmonic frequency, $\omega_2$. In turn, this process creates a voltage across the heater/thermometer that has a dominating $\omega_1$ component with a small $\omega_3$ component. It has been shown by solving the conduction equation [56] that the third-harmonic electric transfer functions, $X_3$ and $Y_3$ can be related to the thermal properties through Equation (4-2) such that

$$X_{3\omega}(\omega_1\omega) + Y_{3\omega}(\omega_1\omega) = \frac{1}{4\pi k L} \int_{0}^{\infty} \frac{\sin^2(\eta b)}{(\eta b)^2 \sqrt{\eta^2 + \gamma(\omega_1\omega)^2}} d\eta$$

(4-2)

where $k$ is the thermal conductivity, $\eta$ is an integration variable, $\gamma$ is the wave number, $b$ is the heater half-width, and $L$ is the heater length. Equation (4-2) has no known closed-form solutions and must be approximated numerically. Therefore, $X_3$ is commonly simplified in the limiting case that the heater half-width is much smaller than the thermal penetration depth ($\lambda_s$) defined by

$$\lambda_s = \sqrt{\frac{2\alpha_T}{\omega_1\omega}}$$

(4-3)

where $\alpha_T$ is the thermal diffusivity and $\omega_1\omega$ is the heating frequency in radians per second.

The thermal penetration depth defined in Equation (4-3) is an important parameter since it quantifies the penetrating distance into a substance for an oscillating thermal wave. If $b \ll \lambda_s$, Equation (4-2) can be simplified such that $X_{3\omega}$ is directly related to the thermal properties through Equation (4-4)
\[ X_{3\omega}(\omega_{1\omega}) \approx \frac{1}{8\pi L k} \left( \ln(\omega_{1\omega}) + \ln(2) + \ln\left(\frac{b^2}{\alpha_T}\right) - 2\xi \right) \]  

(4-4)

where \( \alpha_T \) is the thermal diffusivity and \( \xi \) is a fitting constant. From Equation (4-4), it is possible to use the measured \( X_{3\omega}(\omega_{1\omega}) \) and the geometry of the heater to solve for the thermal parameters of interest, \( k \) and \( \alpha_T \). The linear relation of \( X_{3\omega} \) and \( \ln(\omega_{1\omega}) \) correlates the slope to the thermal conductivity and subsequently the intercept to thermal diffusivity. Once thermal conductivity and diffusivity are known, it is possible to obtain the thermal product, \( \sqrt{\rho c k} \) through Equation (4-5)

\[
\sqrt{\rho c k} = \sqrt{\frac{k^2}{\alpha_T}}
\]  

(4-5)

4.5 HFG Design for 3-omega Method

Thin film RTD HFGs already utilize nanofabrication processes to create the thin-film RTDs on the dielectric surface. However, optimization of the RTD geometry is required for the 3-omega method to be implemented. Equation (4-4) denotes a simplified relationship which only applies under certain assumptions: an infinitely long heater, an isothermal substrate, the heater as a line source; and the substrate is semi-infinite.

To fulfill these assumptions, the heater/thermometer length and width must be chosen to ensure a sufficient linear region exists. In practice, the robustness of the gauges and the presence of this linear region are negatively correlated. Therefore, the heater/thermometer design of these gauges need to maximize gauge durability without compromising the measurements. Dames et al. outlines several design guidelines to ensure a linear region exists for a range of measurement accuracy [109]. Table 4-1 outlines these common design criteria recommended to apply the 3-omega method with the simplified Equation (4-2).

<table>
<thead>
<tr>
<th>Assumption</th>
<th>Guild line</th>
<th>HFG Design</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Substrate is semi-infinite ( (d_S \to \infty) ) [110]</td>
<td>( d_s/\lambda_s \geq 2 )</td>
<td>1.02</td>
</tr>
<tr>
<td>2. Substrate sees heater as line source ( (b \to 0) ) [111]</td>
<td>( \lambda_s/b \geq 1.6 )</td>
<td>1.6</td>
</tr>
<tr>
<td>3. Heater is infinitely long ( (L \to \infty) ) [110]</td>
<td>( L/\lambda_s \geq 10 )</td>
<td>10</td>
</tr>
</tbody>
</table>

Table 4-1. Criteria for 5% Error in Linear Approximation [109]
In Table 4-1, the geometric properties of the gauge (d, L, and b) must be chosen to ensure that $\lambda_s$ can be modulated through a sinusoidal current frequency sweep to meet the above assumptions. Based on information in Table 4-1, an excitation frequency range exists for which the gauges in this study meet most recommendations except for the semi-infinite approximation. Combining assumption 1 with assumption 2, it can be shown that $d/b > 3.2$ to meet all assumptions. For a specified polyimide substrate material, the thickness $d_s$ is fixed, which leaves $b$ as the varying parameter. However, decreasing values of $b$ correlate directly with increased manufacturing challenges and reduced durability of the gauge. For this study, the value for $b$ was chosen in accordance with previous survivability tests. Since the guild line to meet assumption 1 in Table 4-1 was not achieved for the current design, further validation of the linear post processing is warranted and will be addressed in subsequent sections.

Following the design parameters listed in Table 4-1, Figure 4-2 illustrates the HFG geometry selected for this study with the measured mean fabrication values as well as the range. As illustrated in Figure 4-1 (side view) and Figure 4-2 (top view), a 50 $\mu$m polyimide copper-clad film was etched to create the copper leads, and platinum was subsequently deposited via a vapor deposition process to a targeted thickness of 1500 Å. This deposition resulted in RTD resistance values of 40-135 $\Omega$. The variation in resistance most likely stems from deviations in thickness and microstructure in the deposited film. However, these variations are accounted for in the 3-omega process and therefore did not affect the conclusions of this study. Further details about the gauge fabrication are discussed by Siroka et al. [91].

Figure 4-2. Design and Fabrication of top-side of HFG with ranges for minimum and maximum measurements in tested geometries.

Figure 4-2 shows the design intent as an outline with the measured values obtained after fabrication via a microscope. The dimensional stability of $b$ and $L$ is limited by the mask fabrication
for the specific contact lithography technique used in this study. A range of 10 μm was measured in the length while a range of 8 μm was measured in the width. Since the geometry of the heater is used to deduce the thermal properties, each individual gauge in this study was measured to ensure high accuracy in the results.

4.6 HFG Calibration Procedure

Thin-film RTD HFGs require electrothermal RTD calibrations to accurately transform from measured voltage or resistance to temperature. These gauges also require thermal properties to translate measured temperature values on the top and bottom side of the gauge into heat flux. This section provides a framework for implementing the 3-omega technique to characterize double-sided thin-film RTD HFG thermal properties during a traditional calibration process. Specifically, this section focuses on how the top-side thin-film RTD calibration can be used in conjunction with the 3-omega method to determine thermal properties of the dielectric. However, during this process, the bottom-side HFG is also calibrated and information about the underlying adhesive layer can be gathered which will be covered in a following section.

For the electrothermal RTD calibrations, the gauges were excited by a 1mA constant current while situated in a scientific convection oven with a stability rating of 0.2 K. Once the oven and gauges reached thermal equilibrium, RTD voltages were collected to represent baseline calibration data. The excitation was then switched to a sinusoidal current to utilize the 3-omega method and determine the thermal properties. Then, the temperature was changed to the next setpoint and the process was repeated. For this experiment, the temperature ranged from 50°C to 150°C.

Figure 4-3 illustrates the experimental setup for the calibration procedure. On the right of Figure 4-3, specialized excitation and filtering equipment capable of switching between a constant current operation mode and a sinusoidal excitation is labeled as excitation. For the 3-omega technique, a lock-in amplifier was required to separate the small-amplitude third harmonic voltage from the fundamental first harmonic. The lock-in amplifier was connected to a computer through which the RMS values of both harmonics were recorded in reference to the gauge excitation. The system outlined here and shown in Figure 4-3 allows for individual gauge calibrations to be conducted in a scientific oven. However, the same setup can be utilized in-situ to obtain thermal property measurements. The only difference between the calibration set-up and experimental use is the relocation of the instrumented test article from the oven to an experimental environment.
4.6.1 Electrothermal Calibration

The goal of the electrothermal calibration is to create a calibration curve for the RTD elements on the HFGs. Figure 4-4(a) shows two example calibration curves for the same gauge at different annealing states. The typical annealing procedure sets the oven slightly higher than the maximum calibration temperature until the nominal resistance of the gauge changes by less than 0.05% over the span of an hour. This annealing relaxes the internal stresses in the platinum RTD changing the platinum grain-size and therefore the resistance [103].
Figure 4-4. a) Resistance versus temperature for a HFG before and after the annealing process b) Nondimensionalized results from Figure 4-4(a) showing the effect of annealing on $\alpha_R$.

The curves in Figure 4-4(a) give an understanding of the annealing process. From Figure 4-4(a), the resistance of the RTD decreases as the internal stresses in the platinum relax at elevated temperature. Figure 4-4(b) shows the normalized resistance for the two curves in Figure 4-4(a) as well as a curve relating to the bulk platinum coefficient of resistance. Notice that as the RTD is annealed, the coefficient of resistance increases, but still remains far from bulk values. As stated previously, this discrepancy arises because the polyimide backing constrains the maximum annealing temperature below what is required to reach bulk platinum value. Note that all the data in Figure 4-4 as well as subsequent figures correspond to the same thin-film RTD HFG labeled subsequently as the reference gauge.

As shown in Figure 4-4, the defined annealing process changes the resistance and $\alpha_R$ value for the RTD that results in errors in the absolute temperature measurement. The data in Figure 4-4
confirm that RTDs require annealing. Furthermore, as these gauges are implemented for long-duration experiments, it is critical to have an in-situ calibration process to account for any calibration drift that arises due to RTD annealing or deterioration in the testing environment. As will be illustrated in subsequent section, the 3-omega technique provides a tool for such in-situ calibrations.

4.6.2 3-omega Thermal Property Determination

For each temperature setpoint in Figure 4-4 (a), the 3-omega technique was employed to quantify the thermal properties of the substrate for the annealed condition. A reference signal synced with the lock-in amplifier's internal oscillator was used to modulate the current excitation over a range of frequencies with a logarithmic spacing from 10-10000 Hz. The 1st and 3rd harmonic RMS voltage drop across the heater/thermometer was recorded by the lock-in amplifier with a filter setting and settling time automatically adjusted for each frequency. Each 3-omega sweep was completed in less than 3 minutes without requiring wiring changes.

An example frequency sweep is shown in Figure 4-5 where the real and imaginary components from Equation (4-1) are on the ordinate and the logarithm of the fundamental frequency is on the abscissa. The trace in Figure 4-5 is for a gauge with a resistance of 125 Ω excited with a 10mA amplitude sinusoidal current at 150°C. This 3-omega sinusoidal excitation drives a current with an amplitude that is an order of magnitude larger than the constant current used for typical operating and electrothermal calibration. This relatively large current is necessary to create a detectable third harmonic voltage defined by the noise floor of the chosen electronic systems.

In Figure 4-5, the measured in-phase and out-of-phase electric transfer function are plotted as asterisks and x's, respectively. While operating at relatively low frequencies, a region exists for where the in-phase component is linear and the out-of-phase component is constant. This linear region is illustrated in Figure 4-5 by red circles and is defined as the region in which the assumptions listed in Table 4-1 are best met through the HFG design column. The existence of the linear region builds validation that Equation (4-3) can be used to quantify the thermal properties of the substrate. The red circles were used to calculate the thermal conductivity. It can be seen in Figure 4-5 that including adjacent points into the linear region would have little effect on the calculated slope. This builds validation that the assumptions in Table 4-1 are correct.
Figure 4-5. Measured gauge response for a 3-omega sweep comparing multilayer solution to linear trend.

Figure 4-5 also illustrates the computational approximation to a more accurate version of Equation (4-2) which accounts for multi-layered substrates [112], effectively accounting for layers below the gauge which may affect the system response if the thermal wave penetrates into their domain. The difference between the linear approximation and an algorithmic fit to the Equation (4-2) solution changes the thermal properties by less than 2%. The data in Figure 4-5 show the solution to a gauge mounted on an idealized Pyrex backing material. These tests were repeated with copper and stainless steel as backing material to span a range of potential thermal properties. For each of these alternate setups, the linear solution varied from the complex processing by less than 2%. This advanced post processing with gauges mounted to different materials was necessary to validate assumption 1 in Table 4-1. Since the fitting algorithm for the full solution is computationally intensive and was shown to minimally affect the results, the linear processing scheme was used for this work.

Worth noting, the out-of-phase third harmonic component ($Y_{3\omega}$) as well as the first harmonic components ($X_{1\omega}$, $Y_{1\omega}$) can be linked to the thermal conductivity through similar, but independent processes [98]. For the current study, differences between the methods were 5.6%. The third in-phase harmonic ($X_{3\omega}$, traditional 3-omega method) was chosen due to its established straightforward methodology as well as its insensitivity to phase errors and current stability errors [98].

4.6.3 Thermal Property Error Quantification

There are three main error sources for the 3-omega method: fit uncertainty, precision error, and bias error. The fit uncertainty was estimated at 2% based upon the difference between the linear
fit and the computational solution fit which correspond well to the work by Borca-Tasciuc et al [112]. The precision error was calculated based on a collection of 100 measurements for the same gauge and was found to be 0.42%. The bias uncertainty stems from various measurements that contribute to the final value. This was calculated using a perturbation method outlined by Moffat [65]. Figure 4-6 shows the relative impact of each of these bias-uncertainty contributors. These systematic errors arise from the uncertainty of measured parameters. For example, the error associated from the resistance value accounts for 2-wire configuration of the HFGs in this study because the end application for this study (turbine blade heat transfer) is limited in available space. This error can be greatly reduced if a 4-wire measurement technique is used instead.

Figure 4-6 shows that additional error sources are present in the thermal product quantification that are not present in the thermal conductivity. Specifically, the bias error of the measured voltage (V₃ω) and the heater half width (b) increase the thermal product uncertainty. Therefore, the overall uncertainty of the thermal product is understandably higher than the thermal conductivity. From Figure 4-6, the calculated bias errors for the thermal conductivity and thermal product were 2.0% and 4.4% respectively. When accounting for all error sources (precision, fit, and bias) through a root sum square, the overall uncertainty in the thermal conductivity and thermal product measurement is 2.9% and 4.9% respectively.

4.7 Thermal Property Results

This section presents the thermal property results from all available sensors using the 3-omega technique. First, the dielectric properties are quantified followed by the adhesive materials.
4.7.1 Dielectric Thermal Property Results

Following the procedure outlined above in Figure 4-5 using Equation (4-4), the thermal conductivity and thermal diffusivity for seven individual gauges were characterized. This quantification was performed at each calibration setpoint shown in Figure 4-4. A 10 mA current was supplied for the data presented below. Independent tests were repeated using excitation currents of 8.5 mA and 7 mA. Although not presented here, differences between the thermal property characterization from the excitation level was within the uncertainty for both the thermal product as well as the thermal conductivity.

Figure 4-7 illustrates the measurements for the example gauge used in the previous sections. The highlighted region shows the variation across all the tested gauges. The thermal conductivity quantified at 150°C ranges from 0.22–0.26 [Wm⁻¹K⁻¹]. Although a relatively narrow range, the thermal conductivity of the substrate is proportional to the mean heat flux value of the gauge. Therefore, any error in the thermal conductivity directly propagates to the measured heat flux. The variation in Figure 4-7(a) serves to show that individual heat flux measurements could be affected by as much as 18% by improper gauge characterization from bulk value determination.
Figure 4-7. a) Thermal conductivity results for seven different HFGs over a range of temperatures showing comparison to traditional methods b) Thermal product for seven HFGs over a range of temperatures comparing traditional methods to the current study.

Although thermal conductivity is a parameter of interest on its own, the thermal product of the gauge affects the RMS error in the unsteady heat flux [13,18,50]. Similarly, the range illustrated in Figure 4-7(b) shows that the RMS heat flux value could be affected by as much as 30% if only bulk values are used to reduce the heat flux. As a corollary, the 3-omega technique enables the thermal property determination of each individual gauge greatly reducing the errors from bulk value approximations.

Alongside the 3-omega quantification, Figure 4-7(a) and Figure 4-7(b) show previous bulk-value thermal property measurements [91,106,113,114]. These processes were conducted by the authors for the same substrate material. Measurements compare well with the variation seen in the tested gauges indicating a benefit of the 3-omega method. The 3-omega method allows for thermal property information at each calibration setpoint while obtaining the electrothermal calibration.
The other methods require significantly more effort for similar results that cannot be applied on an individual gauge basis. Figure 4-7 illustrates the importance to quantify these thermal properties over a range of temperatures enveloping all experimental operating conditions showing the variation with temperature for a given gauge over the tested conditions also varied by as much as 25% which is consistent with previous findings [91].

Both lumped parameter and 3-omega quantification schemes provide results that are often more accurate than the manufacturer specification. This is due to the fact that batch-to-batch variations exist and the substrate quantified by the manufacturer could be different than the material received. Therefore, these results suggest independent checks of material properties for individual gauges are necessary to substantially reduce the uncertainty of the measurement.

4.7.2 Adhesive Thermal Property Results

Gauges of this type are commonly adhered to a test article. This adhesive layer acts as a thermal insulator yet is a critical parameter necessary to calculate heat flux from measured data. For this reason, it is essential to characterize the adhesive properties to relate measured heat flux to un-instrumented test articles. This is illustrated by Ni et al. [107] who found that to match models to experiment, the adhesive layer as well as the substrate of the gauge must be considered.

Additional information about the backing adhesive can be acquired through the operation of the bottom-side thin-film RTD. The previous sections have illustrated that the thermal properties of the substrate can be quantified using the top thin film RTD. By means of this known information and exciting the bottom gauge in the same manner as described above for the 3-omega method, it is possible to calculate the thermal conductivity of the adhesive through a boundary mismatch approximation (BMA) [108,115–118].

BMA assumes that the thermal transfer function of the system (in this case the adhesive and the substrate), can be summed in parallel. This summative approach yields Equation (4-6) relating the measured apparent thermal conductivity of the system $k_{sys}$ such that

$$k_{sys} = k_a + k_s$$

where $k_a$ is the adhesive thermal conductivity and $k_s$ is the substrate thermal conductivity, which was previously experimentally quantified. In Equation (4-6), $k_{sys}$ is measured through the backside 3-omega technique using Equation (4-4). Because $k_s$ is quantified through the top-side 3-omega method, it is possible to solve for the thermal conductivity of the adhesive, $k_a$. Note this technique can also be used to quantify the thermal diffusivity of the adhesive material [116,118], but requires
complex curve fitting algorithms. Since the high frequency heat flux components are damped by the gauge on the surface, and only the thermal conductivity affects the mean heat transfer rate, the quantification of thermal conductivity was prioritized instead of thermal diffusivity.

Figure 4-8 shows the results of these measurements. Similar to Figure 4-7, the reference gauge and variation over the seven gauges is illustrated. Figure 4-8 also highlights the manufacturer specification [119]; notice that measured values in the current study are significantly lower than this specification. This discrepancy is attributed to the entrapment of air with the particular adhesive used in this study and its application method. Further, the difference in the specification and measured values illustrates the advantage of quantifying the adhesive layer in-situ.

![Graph showing thermal conductivity as a function of temperature for calibration setpoints with highlighted gauge variation.](image)

Figure 4-8. Measured thermal conductivity as a function of temperature for the calibration setpoints with highlighted gauge variation.

4.8 Using the 3-omega Technique for In-Situ RTD Calibration

As outlined in Equations (4-1)-(4-4) and shown in the previous section, the 3-omega method links the thermal conductivity to the coefficient of resistance. When $\alpha_R$ is known during the calibration procedure, this relationship can be used to obtain the thermal properties of the gauge. However, the inverse of this relationship also offers utility for in-situ calibration checks of gauges. If the thermal properties are constant during operation (an assumption that will be evaluated experimentally in this study), it is therefore possible to back calculate $\alpha_R$ using the 3-omega method.

The following section will use data from the same reference gauge to test the feasibility of this theory for in-situ extraction of coefficient of resistance. To accurately apply this method, an initial assumption requires the thermal properties do not change with time. To test this assumption, the same thermal properties were measured before and after an annealing process of 72 hours at 150°C, as illustrated in Figure 4-9. Since the coefficient of resistance is known to change with
annealing, the $\alpha_R$ value was also calculated before and after the annealing process. These data were previously shown in Figure 4-4(a) and Figure 4-4(b), but will be referenced here again.

![Figure 4-9. Schematic of experimental procedure and how it relates to time and temperature.](image)

Shown in Figure 4-10(a) is the thermal conductivity data collected for the same gauge before and after annealing with the measured coefficient of resistance (each data point is deduced from a 3-omega frequency sweep). On the right ordinate, the percent change in resistance is recorded for the same time interval. Notice that the resistance of the gauge changes more for the initial case than the annealed case. The results in Figure 4-10(a) further illustrate the importance of annealing the gauge to obtain an accurate and repeatable calibration.

![Figure 4-10. Thermal conductivity and resistance measurements for the sample gauge with a) the assumption that $\alpha_{R,\text{top}}$ is constant and b) the measured value of $\alpha_{R,\text{top}}$ using 3-omega.](image)

Figure 4-10(a) evaluates the assumption that the thermal properties remain unchanged over the 72-hour annealing. The thermal conductivity measurements for the initial and annealed case
illustrate that the mean data (in which the gauge was held slightly above 150°C for 72 hours), shows differences of 0.1%. Therefore, when operating these gauges over extended periods, the thermal conductivity is also expected to remain unchanged which allows the coefficient of resistance to be back-calculated. Although expected, this validates the choice of polyimide as a dielectric for gauges of this type for its known property stability.

Unlike the experimental setup illustrated in Figure 4-9, the coefficient of resistance will be unknown in practice. Therefore, it is beneficial to measure the thermal conductivity assuming the initial coefficient of resistance because it utilizes the exact measurement and post processing techniques. Figure 4-10(b) illustrates the calculated thermal conductivity based upon the assumption that the coefficient of resistance is constant. Under this assumption, there was a 4.3% difference in the thermal conductivity which correlates to an identical change in the coefficient of resistance.

Since the coefficient was measured in Figure 4-10(a) and calculated in Figure 4-10(b), it is possible to compare the measured to the calculated value. In this case, the difference between the calculated and measured coefficient of resistance would be identical to the change in thermal conductivity - 0.1% (as illustrated by the mean value difference in Figure 4-10(a)). This discrepancy is on the same order of magnitude as the uncertainty in the measured coefficient of resistance. Therefore, through this method, it is possible to accurately determine the coefficient of resistance.

There are several practical implications that arise from this technique. The most important is the ability to calibrate thin-film RTDs in-situ. As illustrated in Figure 4-4(b), the electrothermal calibration is linear, meaning that to define the calibration, one must know a single point and the slope of the line. The single point can be any single pair of resistance and temperature (R_{ref} and T_{ref}) for a platinum RTD under adiabatic conditions. For example, a nearby stable temperature device could be used to obtain T_{ref} while R_{ref} is the recorded gauge response before an experimental run. To obtain the calibration slope, the 3-omega method with the assumption of constant thermal properties from the calibration can then be used to calculate the coefficient of resistance. Therefore, an in-situ calibration can be performed as long as the T_{ref} value has a corresponding thermal conductivity from the oven calibration with the 3-omega technique. Theoretically, this method can also be extended to the bottom side gauge as long as both the adhesive properties and substrate properties are known following the exact same procedure, but substituting k_{sys} for k_{a}.

As a caution, it is imperative to validate the underlying assumption of constant thermal properties. This assumption is not only necessary for accurate heat flux measurements but is also critical to in-situ calibrations. Because the thermal conductivity is proportional to the coefficient of
resistance, a percentage change in thermal conductivity will manifest as an equal error in the percentage change of the coefficient of resistance.

4.9 Conclusions

This work presents a novel calibration method for double-sided thin-film RTD HFGs. The proposed method allows for calibration of RTDs and thermal property determination in one integrated process, with additional benefits of characterizing thermal properties on a per-sensor basis. These thermal properties are necessary inputs when solving the unsteady conduction equation to deduce heat flux. These parameters are also the largest drivers of measurement uncertainty. Therefore, by designing a double-sided thin-film HFG capable of applying the 3-omega technique for thermal property determination, more accurate heat flux can be calculated.

To this end, this study outlines the design considerations for implementing the calibration procedure, highlighting that if proper care is taken, the thermal conductivity of the substrate and the thermal product can be determined within 3% and 5%, respectively. Quantifying these thermal properties on a per-gauge basis negates the assumption that bulk thermal property values represent the material directly under the RTD. Through the quantification of seven distinct double-sided thin-film RTD HFGs, this bulk material assumption was found to contribute as much as a 30% error in deduced mean and unsteady heat flux.

This study also addresses the need for an in-situ calibration method for double-sided thin-film RTD HFGs. For semi-permanent installations like turbine vanes and blades, this provides substantial value. More frequent calibrations lower the uncertainty in the RTD measurements. Since the RTD temperature values are used as the boundary conditions to solve the unsteady conduction equation and deduce heat flux, the more frequent calibrations enabled by this proposed technique thereby also reduce the uncertainty in heat flux. To apply an in-situ calibration, it must be assumed that the substrate thermal properties are not degrading with time. This study showed that for a runtime of 72 hours at 150°C, the polyimide substrate displayed no measurable property changes, allowing an in-situ method to apply. When applying this technique, deduced $\alpha_R$ values differed from the measured $\alpha_R$ by 0.1%.

Overall, this study demonstrates the importance of proper substrate thermal property determination and recommends a calibration procedure to reduce uncertainty in measured heat flux quantities. The grouping of the 3-omega method and gauges of this type leads to improved gauge accuracy and novel in-situ calibration methods.
5 TWO-LAYER TRANSIENT HEAT TRANSFER USING IMPULSE RESPONSE METHODS 5

5.1 Abstract

Solutions to the inverse heat conduction problem (IHCP) are methods that can be used to quantify surface heat flux in multi-layer materials for components in which there are limited subsurface (internal) temperature measurements, such as coated components. A critical consideration is to capture high frequency fluctuations using a practical heat flux sensor. To that end, this paper highlights key parameters for calculating accurate surface heat transfer. Specifically, this research extends the available solutions to the IHCP for multi-substrate structures through an impulse response methodology. The sensitivity of the impulse method was quantified with respect to practical measurements. When compared to the inverse case, the impulse method resulted in lower errors when calculating surface heat flux over a range of conditions. Overall, this work provides a foundation for deducing heat flux from a subsurface heat flux sensor while maintaining a high-frequency response.

5.2 Introduction

Quantifying heat flux in hot components is critical to achieving desired part life. One common method for measuring heat flux is the use of differential temperature gauges (HFGs) [1]. These sensors comprise two temperature measurements at known locations on opposing sides of a substrate with known thermal and geometric properties. As an extension, transient heat flux is calculated at the surface by solving the unsteady conduction equation using the two temperature measurements and the properties of the inner substrate. However, the location of the temperature in a layered measurement sensor affects the operation and the processing required to solve for the transient heat transfer. For instance, if one of the temperature sensors is on the surface of the component itself, the boundary conditions for the conduction equation are known and the surface heat flux can be calculated directly; this example is considered a direct problem. Direct problems have been solved through a number of techniques [13,18], but one of the most computationally efficient methods is through the use of an impulse response filter. Oldfield [18] employed the impulse technique to deduce surface heat flux from temperature measurements on opposing sides of a substrate and found numerical errors of less than 0.014%.

In contrast to direct problems, indirect problems use multiple subsurface temperatures to calculate surface heat flux. The indirect problem requires a different processing analyses because

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the conduction equation is ill-posed. The ill-posed nature of the conduction equation is because surface conditions are damped as thermal energy propagates through the subsequent layers to the measurement location of a multi-layer part. Because of the damping, internal measurements must be rectified to obtain the values at the surface. Inverse solution methods are a vast arena of mathematics [120,121] with several applications [122,123]. The work presented here specifically focuses on solutions to the inverse heat conduction problem (IHCP).

Several techniques have been used to solve the IHCP for a single-layer system. Broadly, these solutions can be split into entire-domain numerical solutions [124] and filter-based solutions [125,126]. The entire-domain approaches use either analytical solutions to conduction equations [124] or commercial FEA solvers [127] to calculate domain-wide conditions from the internal temperature measurements. This IHCP approach is computationally intensive and cannot be used for real-time processing [126]. On the other hand, filter-based solutions are computationally efficient and can be used for real-time processing. Therefore, this methodology is an ideal choice for applications requiring integrated feedback for controls.

Within the architecture of filter-based solutions, approaches based on Green's Function [125,128] have been commonly used to solve single-layer domains, and machine learning algorithms have also been explored [129]. However, few studies have extended these techniques to composite structures, which are central to many real-world applications, such as coated systems. Najafi et al. [130] provides one such filter-solution to the two-layered IHCP based on a domain splitting technique to formulate a computationally-efficient transformation filter using a sum-of-square error minimization with a Tikhonov regularization (TR) to stabilize the solution. Their solution showed small numerical errors for a variety of cases. The Nafaji et al. solution currently serves as the only filter-based composite solution in open literature. Unfortunately, this solution employs a regularization scheme that requires a priori information about the problem for correct selection of the regularization parameter [131]. This drawback can be bypassed through the use of an impulse response approach, which is described in our paper.

The purpose of this study is to add to the number of solutions for composite IHCP systems by extending the impulse response direct method presented by Oldfield [18] to an indirect two-layer 1D transient system. The present study implements novel solutions to the two-layered conduction equation to provide generalized sensor guidelines as well as innovative processing schemes for multi-layer one-dimensional thermal components. First, this paper provides a detailed outline of design considerations for transient two-layered heat transfer. Next, this work compares the proposed impulse response method with the traditional inverse processing method by Najafi et
Finally, the sensitivity of the impulse response method is characterized in terms of thermal property errors and signal noise.

5.3 Considerations of the Two-Layer Heat Transfer Gauges

The methods presented in this paper are general to any multi-layer one-dimensional, linear, time-invariant component. However, as a new contribution, this paper explores a two-layer application. Figure 5-1 illustrates the domain, which serves as the basis for the rest of the analyses. The two distinct domain regions are labeled as Layer 1 and Layer 2, but are characterized in the context of a coated component. For that reason, domain quantities are denoted with a "c" subscript for the coating layer (Layer 1) and a "s" subscript for the substrate layer (Layer 2). Within Figure 5-1, a few key planes are highlighted and denoted including: surface quantities such as temperature \( T_0 \) and heat flux \( q_0 \) that are denoted with a zero subscript; the interface sensor location that is denoted with a subscript of one \( T_1, q_1 \) at a distance related to the thickness of the coating \( d_c \); the location of the second internal temperature sensor is denoted with a subscript of two \( T_2 \), and its location is defined by the combined thickness of the coating and substrate layers \( d_c + d_s \).

![Figure 5-1. Two-layer (coating and substrate) domain with important locations highlighted.](https://via.placeholder.com/150)

The coating layer is a thermal damper to the underlying measurement planes, which imposes limitations resulting from the dissipation of thermal energy upstream of the measurement location. One method to quantify this damping is to analyze the temperature response to a steady harmonic surface heat flux. Equation (5-1) and Equation (5-2) show the governing equations for both domains where the temperature solution through the coating and substrate are \( T_c(x,t) \) and \( T_s(x,t) \):
\[
\frac{\partial^2 T_c}{\partial x^2} = \frac{1}{\alpha_c} \frac{\partial T_c}{\partial t} \quad 0 \leq x \leq d_c 
\]  
(5-1)

and

\[
\frac{\partial^2 T_s}{\partial x^2} = \frac{1}{\alpha_s} \frac{\partial T_s}{\partial t} \quad d_c \leq x < \infty 
\]  
(5-2)

where \(\alpha_c\) and \(\alpha_s\) are the thermal diffusivity for the coating and substrate.

Table 5-1 shows the boundary conditions used for the analysis. Because this is a steady-state solution, no initial conditions are necessary to solve the governing differential equations. The solutions were obtained following conventional procedures to steady-state harmonic conduction problems [132]. Previously, part of this solution was published [13], but the current work expands the solution to both layers of the domain, which is more applicable to a coated system approach.

**Table 5-1. Boundary Conditions for Harmonic Surface Heat Flux**

<table>
<thead>
<tr>
<th>Boundary Conditions</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. (q_0 = \hat{Q} \cos(\omega t)) at (x = 0)</td>
</tr>
<tr>
<td>2. (T_c = T_s) at (x = d_c)</td>
</tr>
<tr>
<td>3. (k_c \frac{\partial T_c}{\partial x} = k_s \frac{\partial T_s}{\partial x}) at (x = d_c)</td>
</tr>
<tr>
<td>4. (T_s = 0) at (x \to \infty)</td>
</tr>
</tbody>
</table>

Each layer was solved using a corresponding superposition of real and imaginary solutions. The temperature through the coating (\(T_c\)) and through the substrate (\(T_s\)) are given as

\[
T_c(x, t) = \text{Re} \left\{ \left( \frac{\hat{Q}}{k_c Z_c} \right) \frac{(1 + \sigma) \exp(-Z_c(x - d_c)) + (1 - \sigma) \exp(Z_c(x - d_c))}{(1 + \sigma) \exp(Z_c d_c) + (1 - \sigma) \exp(-Z_c d_c)} \exp(i \omega t) \right\} \quad (5-3)
\]

and

\[
T_s(x, t) = \text{Re} \left\{ \left( \frac{2 \hat{Q}}{k_c Z_c} \right) \frac{\exp(-Z_s(x - d_c))}{(1 + \sigma) \exp(Z_c d_c) + (1 - \sigma) \exp(-Z_c d_c)} \exp(i \omega t) \right\} \quad (5-4)
\]

where the ratio of thermal effusivities (\(\sigma\)) is
\[ \sigma = \sqrt{\frac{\rho c k_s}{\rho c k_c}} \] (5-5)

and the imaginary wave numbers (Z) are

\[ Z_c = \sqrt{\frac{i\omega}{\alpha_{T,c}}} \text{ and } Z_s = \sqrt{\frac{i\omega}{\alpha_{T,s}}} \] (5-6)

with the other necessary quantities defined in Figure 5-1.

The solution in Equation (5-3) can be particularly useful to assess the design space and limitations of two-layer HFGs over a range of frequencies. Figure 5-2 serves as an example of the solution to the two-layer harmonic heat flux equation solved using the quantities found in Table 5-2. Figure 5-2(a) shows the harmonic heat flux boundary condition at an example driving frequency of 8000 [Hz] with an amplitude of $1 \times 10^4$ [Wm$^{-2}$] across a range of 2 periods. Figure 5-2(b) illustrates the temperature fluctuations at the previously highlighted locations across the same range of periods.

![Figure 5-2](image_url)

Figure 5-2. a) Harmonic heat flux surface boundary condition and b) solution to the two-layer harmonic heat conduction equation at selected locations.
Table 5-2. Geometric and Thermal Parameters Necessary to Solve the Harmonic Heat Conduction Equation

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \alpha_{T,c} )</td>
<td>8.67E-8</td>
<td>([\text{m}^2 \text{s}^{-1}])</td>
</tr>
<tr>
<td>( k_s )</td>
<td>0.12</td>
<td>([\text{W m}^{-1} \text{K}^{-1}])</td>
</tr>
<tr>
<td>( d_c )</td>
<td>1E-6</td>
<td>([\text{m}])</td>
</tr>
<tr>
<td>( \alpha_{T,s} )</td>
<td>1.44E-7</td>
<td>([\text{m}^2 \text{s}^{-1}])</td>
</tr>
<tr>
<td>( \sigma )</td>
<td>1.23</td>
<td>([-\text{]})</td>
</tr>
<tr>
<td>( d_s )</td>
<td>5E-5</td>
<td>([\text{m}])</td>
</tr>
<tr>
<td>( \omega )</td>
<td>16000(\pi)</td>
<td>([\text{rad s}^{-1}])</td>
</tr>
<tr>
<td>( \hat{Q} )</td>
<td>1E4</td>
<td>([\text{Wm}^{-2}])</td>
</tr>
<tr>
<td>( \bar{Q} )</td>
<td>0</td>
<td>([\text{Wm}^{-2}])</td>
</tr>
</tbody>
</table>

Several important characteristics in Figure 5-2 illustrate the necessary processing corrections to obtain surface conditions. First, as shown in Figure 5-2(b), the peak amplitude of the temperature (\( \hat{T} \)) decreases significantly from the surface through the coating and substrate. Compared to the \( T_0 \) amplitude, the \( T_1 \) amplitude in Figure 5-2(b) is attenuated. In fact, the surface amplitude has become completely damped in \( T_2 \) illustrating the loss of information as the temperature wave propagates through the layers. Second, there is a notable phase shift between the \( T_0 \) and \( T_1 \) case (highlighted in Figure 5-2 as \( \phi \)) as well as a shift from the \( T_0 \) and \( q_0 \) case. This shift illustrates that the selected processing scheme must be able to rectify both the shifted phase and damped amplitude of the internal temperature traces to determine true surface conditions.

The identified damping effect has practical implications for the design of a heat flux sensor because the temporal heat flux quantification depends upon a measurable temperature oscillation at \( T_1 \). One way to understand the design space of the proposed multi-layer sensor is to quantify the measurement plane amplitude (\( \hat{T}_1 \)) relative to the surface amplitude (\( \hat{T}_0 \)). Using the solution to Equation (5-3) at the surface and the interface, it can be shown that

\[
\frac{\hat{T}_1}{\hat{T}_0} = \frac{2 \exp\left(-\frac{d}{\lambda}\right)}{\sigma+1+\exp\left(-\frac{2d}{\lambda}\right)(1-\sigma)}
\]

(5-7)

where the thermal coating wavelength is defined as
\[ \lambda_c = \sqrt{\frac{2\alpha_c}{\omega}} \]  

and where \( \omega \) is defined as the harmonic heating frequency at the surface.

Equation (5-7) illustrates the parameters that are critical to the temperature attenuation at the interface of the layers: the top layer thickness divided by thermal wavelength for the top layer (\( d_c/\lambda_c \)) and the ratio of thermal effusivities for the two layers (\( \sigma \)). Based on these identified dependencies in Equation (5-7), the following sections are presented in terms of \( d_c/\lambda_c \) and \( \sigma \) to deduce their physical significance to selected processing schemes.

Figure 5-3 plots Equation (5-4) for three different \( \sigma \) values across a range of \( d_c/\lambda_c \) conditions displaying the clear design tradeoffs associated with this type of measurement. Figure 5-3 provides two key benefits for the ongoing analysis: (i) it graphically shows whether surface conditions can be obtained from the internal temperature points, and (ii) it serves as a guide for the processing steps required to obtain those surface quantities.

---

**Figure 5-3.** a) Ratio of surface temperature amplitude to measurement plane temperature amplitude across a range of non-dimensional coating thicknesses b) sensitivity of the relative temperature amplitude to the ratio of thermal effusivities.

---

Figure 5-3(a) can be split into three distinct regions. The first region (I) denotes where the coating can be treated as thermally transparent. Physically, this region represents a coating (Layer 1) with a high thermal diffusivity, a low coating thickness, and a low frequency heat flux. The combination of those characteristics creates a thermally transparent top layer.

Figure 5-3(b) illustrates the impact the ratio of thermal effusivities has on this region. The ratio of thermal effusivities (\( \sigma \)) in part dictates how the energy is dissipated between the two
domains. Therefore, when the ratio of thermal effusivities is small, more energy will be dissipated in the lower layer, causing the top layer to appear more thermally transparent. It is highly advantageous to develop sensors in this region because the required processing can be simplified to a direct problem. However, it is not always feasible to operate in this region based upon the engineering durability requirements that may be related to the system or the frequency of the heat transfer phenomena of interest.

Region II is defined as the region where the measured temperature amplitude ($\overline{T_1}$) is substantially attenuated by the coating layer (Layer 1), but not completely damped as illustrated by Figure 5-3(a). This region requires indirect processing to rectify the measured temperature to surface conditions. The value of $d_c/\lambda_c$ that bounds Region II at the lower end depends upon the required accuracy for the application as well as the ratio of thermal effusivities, which is illustrated in Figure 5-3(b). However, the upper limit of this region is relatively independent of the ratio of thermal effusivities and can be approximated by a cutoff value of $d_c/\lambda_c = 3$. At that point, the measured signal ($T_1$) is approximately 5% of the surface temperature and requires extreme amplification to recover the surface conditions for reasonable $\sigma$ values. High amplification can negatively impact the accuracy of the deduced surface conditions as the signal amplitude approaches the noise floor.

Region III denotes the region where the damping of temperature amplitude through the coating is greater than 95%, meaning there is insufficient information in the measured signal to accurately reproduce the surface conditions. Region III shows the limitations of a two-layer system by defining a strict limit on the phenomena that can be captured. This region also represents the limitations of the indirect methods that will be characterized in subsequent sections, a limit which does not traditionally exist in uncoated systems.

Overall, Figure 5-3 illustrates the tradeoffs between coating thermal properties, thickness, and heat flux frequency, while also connecting design decision to the necessary processing procedures. In general, sensors of this type benefit from low $\sigma$ values up to the point where $d_c/\lambda_c = 3$. Furthermore, Figure 5-3 provides guidance on whether existing two-layer systems can feasibly use the IHCP solution methods to deduce surface conditions and, if so, where the temperature measurements should be located relative to the surface of interest.

5.4 Indirect Methods to Obtain Surface Conditions

The previous section provided guidelines of three different regions in Figure 5-3(a): (i) the coating is thermally transparent, (ii) the coating must be accounted for through processing, and (iii) the coating is thermally opaque. To maximize the frequency response or durability of the sensor, it
is advantageous to operate in the second region even though it requires more complex (IHCP) processing. The remainder of this paper will focus on solutions for Region 2 while detailing the necessary processing to reconcile damping effects from the coating.

Two different indirect methods were investigated: an inverse method (employing the minimization of the sum-of-the-squared errors between the computed and known values and using TR for stabilizing the solution) and an impulse response method. Both methods assume that the time history from two internal temperature measurements are known and the surface conditions are the desired parameters. Although there are many ways to deduce the surface conditions, these two methods were chosen for their computational efficiency since both utilize a transformation filter, which makes the approach appropriate for on-stand testing.

There are many similarities between the inverse TR method and impulse method. In particular, both approaches utilize filter form solutions to transform the internal measurements into surface conditions. However, while the inverse TR method uses a single filter, the impulse response method uses four independent filters in a cascading manner. Both approaches can be applied to a composite system with any number of layers. However, the subsequent formulations presented through this paper focuses on a two-layer system.

5.4.1 Inverse TR Method

The present study replicates the work of Najafi et al. [130] and compares this processing to an impulse response filter. This section provides some necessary details to understand the underlying mathematics behind the solution. However, readers are directed to the original study for a full description of the implementation process.

Najafi et al. [130] present a solution to the IHCP for a two-layer medium for which temperatures are known at two internal points. The approach is based on subdividing the domain into two regions and subsequently solving the IHCP for the inner layer through the use of single-layer analytical solutions based on Green's functions. This solution is coupled to the second region by utilizing the results from the inner (substrate) layer as the interface boundary condition for the outer (coating) layer. Finally, the surface conditions are solved using a separate analytical solution for the outer layer.

The inverse TR method presented by Najafi et al. [130] has several advantages. Mainly, this formulation can account for known contact resistances between the layers. Another advantage is that the process minimizes errors based upon a sum-of-squares approach comparing the error between the computed and known temperature values using a TR. However, this minimization necessitates that the knowledge of the end application is known beforehand, which is not always the case. For the present analysis, a single TR parameter was used based on a step change in heat
flux at the surface layer. This TR parameter metric provides a representation of how the formulation would be used if the end application was not known or not well understood.

5.4.2 Impulse Response Method

The impulse response method for conduction problems was first described by Oldfield [18]. The basis of the technique uses discrete deconvolution to derive filter impulse responses of the same length as the data. Although a description of the filter formulation is briefly presented in this paper, implementation strategies are presented by Oldfield. Similar to Najafi et al. [130], the impulse response technique is general to any linear time invariant system. Contrary to the inverse TR approach, this process does not require any regularization parameters, which simplifies the application of the impulse response filter for a wide range of applications. However, because it is not regularized, the filter is also more prone to instabilities – a potential issue that will be addressed later. Previously, this impulse response method was limited to direct conduction problems [18] or single-layer IHCP solution [133]. The present study uniquely extends its application to multi-layer materials.

Similar to the inverse TR method, the impulse approach splits the problem into two discrete domains. The workflow of the problem is illustrated in Figure 5-4. In Figure 5-4, the entire gauge is subdivided into a substrate and a coating solution. The substrate solution has been detailed previously by Oldfield [18]; it is solved using an elegant superposition of differential and common mode gauges, which bypasses the need to know details of the backing material. These solutions to the differential and common mode gauges are then used to create two filters that correspond to the AC components of $T_1$ and $T_2$ respectively. The impulse responses ($h_1$ and $h_2$) of those filters are shown in Figure 5-4. Applying the filters to the measured temperature traces transforms the data into the heat flux at the interface ($q_1$).
The interface conditions can then be used with the coating solution to obtain the surface conditions, $T_0$. This second solution step extends the capacities from Oldfield's direct method to an IHCP solution. To create an impulse response filter for the coating solution, an analytical relationship between $q_0$ and $q_1$ for a step change in $q_1$ must be known such that

$$ q_0[n] = h[n] \ast q_1[n] $$

$$ = \sum_{i=0}^{N} h_i q_1[n - i] = h_0 q_1[n] + h_1 q_1[n - 1] \ldots h_N q_1[n - N] $$

Equation (5-9) shows that the impulse response, $h$ can be obtained if a discrete relation between $q_0$ and $q_1$ is known. The analytical relationship between $q_0$ and $q_1$ was determined using a Laplace transform of Equation (5-1) and Equation (5-2) with the boundary conditions listed in Table 5-3.
Table 5-3. Boundary Conditions to a Surface Step Change in Heat Transfer for a Two-layer System

<table>
<thead>
<tr>
<th>Boundary Conditions</th>
<th>at x = 0, t &gt; 0</th>
<th>at x = d_c</th>
<th>at x = d_c</th>
<th>at x → ∞</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. ( q_0 = -k_1 \frac{dT_1}{dx} = Q )</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>2. ( T_c = T_s )</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>3. ( k_c \frac{dT_c}{dx} = k_s \frac{dT_s}{dx} )</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>4. ( T_s = 0 )</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Equation (5-10) displays the response of \( q_1 \) for a step change in surface heat flux \( (q_0 = Q) \) such that

\[
\frac{q_1}{q_0} = \frac{q_1}{Q} = \left[ \text{erfc} \left( t^{*1} \right) - \sum_{n=1}^{\infty} A^n \left( \text{erfc} \left( \frac{2n-1}{t^{*}} \right) - \text{erfc} \left( \frac{2n+1}{t^{*}} \right) \right) \right]
\]

(5-10)

where

\[
A = \frac{1 - \sigma}{1 + \sigma}
\]

(5-11)

and the nondimensional temperature \( (t^*) \) is

\[
t^{*} = \frac{2}{\alpha_{T_c} d_c}
\]

(5-12)

Figure 5-5(a) plots the ratio of the output to the input in Equation (5-10) for \( \sigma = 1 \) across a range of \( t^* \) values from 0 to 3 with the normalized input plotted as a dashed line. In Figure 5-5(a), there exists a region \( t^* \leq 0.4 \) for which insufficient nondimensional time is available for the surface step change in heat flux to propagate to the interfacial plane (see Figure 5-1). In this region (denoted by the shift), the impulse response of the filter will be infinite because \( q_1 = 0 \) while \( q_0 = 1 \). Therefore, to correct the output, the \( q_1 \) sequence is shifted and accounted for after the creation of the filter. Figure 5-5(b) displays three discrete \( \sigma \) values ranging from 0.1-10, illustrating relative insensitivity of the nondimensional shift to this parameter. Although a two-layer system is used in this analysis, this process can be repeated for additional layers that are present, making a more general n-layer solution.
Figure 5-5. Solution to the ratio of interfacial heat flux to surface heat flux for a) $\sigma=1$, note the shift required to obtain a finite impulse response and b) three distinct $\sigma$ values.

After the “coating solution” heat flux filter is created, a separate “coating solution” temperature filter is used to transform the surface heat flux to the surface temperature. This step is accomplished using a solution presented in Doorly et. al [47] for a two-layer system following the same procedure that was previously described above.

Figure 5-4 also illustrates that the AC and DC path are separated for the proposed analysis. The DC heat flux value depends only on the thickness of the substrate layer ($d_s$) and the thermal conductivity of the same layer ($k_s$). A simple 1D conduction network can be used to solve for those parameters. On the other hand, the AC components require the most intensive processing. Separating the time-resolved components from the mean avoids settling times [91] from the impulse response method which implicitly assumes the solution starts at a zero condition. This separation also avoids propagation of mean-value errors and associated concerns with the time-resolved processing.

For large datasets, the impulse and inverse filter require significant time (on the order of hours) to create the necessary filters. However, after the initial computational investment to create the filters, they can be applied relatively quickly. For example, the impulse filters were able to process 1.5 million temperature points in ~1.5 seconds on a standard desktop computer. In comparison, the inverse TR method was able to process the same data set in ~0.5 seconds. For this example, the computational time required is negligible; however, this consideration should be taken into account when choosing the appropriate processing scheme for a specific application.
5.5 Comparison of Inverse Methods

Once the filters for both of these methods were developed, the harmonic solution to the two-layer unsteady conduction equation – Equations (5-3) and (5-4) – was used as a test case for the processing. These solutions allow idealized analytical temperature traces for $T_1$ and $T_2$ to be processed through both the inverse TR method as well as the impulse method. The harmonic equation was chosen as the test case because it allows the heating frequency to be varied which can be used to quantify the accuracy of the processing schemes across a broad spectral range. This characterization is important to applications that have numerous important frequencies present simultaneously, such as gas turbine engines [9].

Figure 5-6 serves as an example of this comparative processing assessment. Figure 5-6(a) uses Equations (5-3) and (5-4) to create the test traces, $T_1$ and $T_2$, respectively. For each case, 50 cycles were simulated with a time step that was sufficiently low to avoid attenuation of the signal. With this definition, the cycles from 10-20 were processed using both previously described methods. Figure 5-6(b) shows an example of the processed surface temperature and heat flux compared to the analytical solution. In Figure 5-6(b), the analytical solution is shown as a solid line, the inverse method is plotted as circles, and the impulse method is plotted as crosses.

These two processing schemes were then characterized in terms of an amplitude error, $\epsilon_{\text{amp}}$, given in Equation (5-11) as the relative difference between the computational processing and analytical solution such that

$$\epsilon_{\text{amp}} = \left| \frac{\hat{N}_{\text{com}} - \hat{N}_{\text{anl}}}{\hat{N}_{\text{anl}}} \right|$$

(5-13)
where N is a quantity of interest such as surface temperature or heat flux. Following this convention, a phase error, $\epsilon_{\text{phase}}$, is also defined as the phase shift between the analytical and computational traces. A discrete Fourier transform was utilized to quantify the $\epsilon_{\text{amp}}$, and $\epsilon_{\text{phase}}$ for each processing scheme [134].

Finally, the results were plotted across all test cases as shown in Figure 5-7 where $d_c/\lambda_c$ was varied from $1 \times 10^{-3}$ to 10 while $\sigma$ was held constant at 1.23. This specific $\sigma$ value corresponds to sensors developed for testing through the current study with a polyimide substrate and Parylene coating. From the impulse response method, several quantities are plotted including the amplitude error ($\epsilon_{\text{amp}}$) with respect the analytical solution in $q_1$, $q_0$, and $T_0$ plotted in solid lines. Additionally, the $q_0$ amplitude error for the inverse TR method is plotted with respect the analytical solution in dashed lines.

![Figure 5-7](image)

**Figure 5-7.** a) Amplitude error quantification for surface and internal quantities through both impulse and inverse methods and, b) grey boxed region in Figure 5-7(a) surface quantities amplitude error for the impulse method where $\sigma = 1.23$.

Several important processing characteristics are quantified through Figure 5-7(a). First, the $q_1$ amplitude error illustrates the effective errors if the coating is not accounted for in the analysis. This quantifies the interface heat flux error with respect to the analytical surface conditions. These errors correspond to the defined regions in Figure 5-3. When $d_c/\lambda_c < 10^{-2}$, the coating is thermally transparent and negligible errors are present in the calculated amplitude. Amplitude errors then increase with $d_c/\lambda_c$ as the coating increasingly damps the signal, solidifying the need to have a processing scheme capable of capturing the physics of a multi-layer system. When the signal is completely damped, the error in the amplitude reaches a value of unity meaning that insufficient information is available from the damped signal.
The processing necessary to obtain the $q_0$ and $T_0$ impulse methods accounts for the coating on the top of the gauge surface. Accordingly, $q_0$ and $T_0$ impulse methods decrease the error when $d_c/\lambda_c < 3$ compared to $q_1$. This lower error validates that indirect processing solutions are necessary to avoid excessive errors in that region. However, when $d_c/\lambda_c > 3$, the internal temperature traces are mostly damped and therefore cannot be used to deduce surface quantities. In this region, the impulse response method tends to become unstable, setting a hard cutoff for the usefulness of the processing scheme that corresponds to the physics outlined in previous sections. Figure 5-7(b) is a subset of Figure 5-7(a) showing errors from $d_c/\lambda_c = 10^{-3}$ to $d_c/\lambda_c = 3$ for the surface quantities using the impulse response method. In this range, the errors were at most 3.2% and were typically below 1%.

The inverse TR method shows a region of increased amplitude error when comparing the $q_0$ results to the inverse TR method where $10^{-2} \leq d_c/\lambda_c \leq 0.2$. This increase in amplitude error is due to the regularization scheme used to create the inverse TR method which was the least-error-square-fit to a step change in heat flux, not a harmonic solution. However, the identified discrepancy solidifies the conclusion that if experimental conditions are unknown, selection of a regularization scheme is challenging, and the impulse response method is therefore a better processing choice.

In addition to the amplitude errors evaluated in Figure 5-7, phase errors can also arise from these processing techniques. Phase errors can lead to erroneous interpretation of time resolved-data, especially when synchronizing data across multiple sensors or data acquisition systems. Therefore, it is imperative to quantify the phase error associated with the processing methods. To this end, Figure 5-8 quantifies the associated phase errors following the same line styles as outlined in Figure 5-7.

The inverse TR method shows similar phase errors in Figure 5-8 compared to the impulse method when comparing the $q_0$ values from $d_c/\lambda_c = 10^{-2}$ to $d_c/\lambda_c = 3$. At $d_c/\lambda_c = 10^{-3}$, there is a slight advantage to the inverse method over the impulse method (on the order of 5°). This discrepancy at lower coating thicknesses is due to the association of phase error with the impulse method is dependent upon the discretization of the shift in Figure 5-5. To accurately capture this shift, relatively high sampling frequencies are necessary. Because the sample rate in this procedure is set by the harmonic heat flux boundary condition, at low $d_c/\lambda_c$ values, the shift is not adequately captured leading to increased phase errors. This identified error can be mitigated in practice by ensuring the rate of acquisition is sufficiently high to capture the shift highlighted in Figure 5-5(a) meeting the criterion in Equation (5-12) such that the sample rate, $f_s$
\[
{f_s} \geq \frac{4\alpha_{\tau_c}}{\tau_c^{*2}d_c^{*2}} 
\]

where \(\tau_c^{*}\) is the critical nondimensional time usually equal to 0.4 with a weak dependency on the \(\sigma\) values.

Overall, both the inverse and impulse show merit in different ways. The inverse TR approach provides a method by which to correctly capture phase at the expense of amplitude errors, whereas the impulse response method does not require a user-selected regularization parameter which is optimal for cases where no prior heat transfer information is known. Because the inverse TR method is well-characterized through other literature, the remainder of this analysis will focus on the impulse method.

### 5.6 Impulse Response Processing Scheme Sensitivities

The previously established processing techniques depend on the geometric and thermal parameters of the system as well as the signal integrity from the temperature measurement devices. Up to this point in the current study, the system has been represented as ideal signals with a perfect knowledge of thermal and geometric parameters. However, in application, signal noise and uncertainty in geometric conditions can both contribute to additional errors. Therefore, this section outlines the sensitivity to those practical factors.
5.6.1 Sensitivity to Signal Noise

One of the drivers of heat flux uncertainty is amplification of random noise in the temperature signals propagating erroneously as temperature fluctuations through the processing of \( T_1 \) and \( T_2 \) surface quantities [120]. Traditionally, this phenomenon has been most prevalent at high frequencies where larger amplifications are necessary to deduce heat flux.

In a composite structure, the coating layer acts as an analog filter to the temperature measurement devices below the surface. Knowledge of the configuration can be used to create lowpass digital filters that utilize the physical characteristics of the coating layer(s) to define a cutoff for the possible thermal frequencies (avoiding unwanted electrical noise). To demonstrate this approach, Figure 5-9 presents the internal temperature amplitudes normalized by the surface temperature amplitude. For this exercise, a value of \( \sigma = 1.23 \) was selected for consistency with previous analyses, and associated thermal and geometric properties are based on Table 5-1. Figure 5-9 also displays a horizontal line representing where the amplitude reaches 5% of the surface level values. This cutoff was chosen as the amplitude attenuation where the processing is no longer able to correct the internal temperature traces to surface conditions. The intersection point between the temperature traces and the 5% line defines the cutoff frequency for the lowpass filters.

![Figure 5-9. Internal temperature sensors amplitude with respect to surface temperature amplitude for \( \sigma = 1.23 \) showing the determination of filter cutoff locations.](image)

Digital lowpass filters were created using the cutoff values defined graphically in Figure 5-9, and those filters were subsequently implemented using a zero-phase filtering procedure to avoid introducing additional phase errors. These signal filters were tested by adding Gaussian noise at 10% of the maximum amplitude \( T_1 \) measurement for a given solution. After adding noise, the
signals were filtered and processed, as outlined in the previous sections. The calculated amplitude errors are displayed in Figure 5-10.

In Figure 5-10, three different \( q_0 \) cases were characterized: the ideal signal (characterized previously in Figure 5-7), the raw signal with noise, and the filtered signal with noise. The ideal signal demonstrates the best stability, with minimal errors up to a value of approximately \( d_c/\lambda_c = 3 \). The addition of noise increased errors at very low \( d_c/\lambda_c \) values—an observation that is expected due to the poor signal to noise ratios. Interestingly, the addition of noise to the signal resulted in minimal error increase for intermediate \( d_c/\lambda_c \) values. This result is due to the fact that the amplitude is being calculated using a discrete Fourier transform which focuses on one specific frequency which is not always affected by gaussian noise. The addition of noise also causes the solutions to become unstable at a \( d_c/\lambda_c > 1 \), limiting the region of the gauge use.

Figure 5-10 shows benefits from the addition of lowpass filters. First, the filter extended the stability of the processing region to \( d_c/\lambda_c = 3 \), which coincides with the \( T_1 \) cutoff set in Figure 5-9. Second, the quantified amplitude errors were the same as the ideal case with the exception of low \( d_c/\lambda_c \) values because this region is below the cutoffs defined in Figure 5-10. Essentially, there is no way to reduce the noise in this region because it cannot be discerned from physical temperature variations.

![Figure 5-10. Amplitude error for impulse response processing under three different noise considerations.](image)

5.6.2 Sensitivity to Geometric and Thermal Properties

Because thermal and geometric properties are often not perfectly known, it is imperative to understand the sensitivity of those parameters to selected processing schemes. Although these
properties are sometimes deduced as lumped parameters (such as k/d) [13,43], the following analysis lists each input property and geometric input separately to show the individual impact of each if measured independently.

The analysis was conducted through a perturbation method [65] where each parameter was perturbed by 1% of its original value. To calculate the error, the relative change in the amplitude from the perturbed solution was calculated with respect to the normal solution and presented as an absolute value. Figure 5-11 shows the results of this analysis for three discrete $d_c/\lambda_c$ values: $1 \times 10^{-3}$, 1, and 3. As with prior sections of the present study, this perturbation analysis was conducted for a nominal case represented by $\sigma = 1.23$.

![Graph showing sensitivity of $q_0$ amplitude error to thermal and geometric properties](image)

**Figure 5-11. Sensitivity of $q_0$ amplitude error to the thermal and geometric properties of the system.**

The results in Figure 5-11 are split into three distinct $d_c/\lambda_c$ values representing the regions outlined in Figure 5-3. At low $d_c/\lambda_c$ values, the system is largely independent of the coating properties itself. This low $d_c/\lambda_c$ value is similar to a constant heat flux condition where only the thickness ($d_s$) and the thermal conductivity ($k_s$) of the substrate are necessary to deduce the heat flux. This is shown in Figure 5-11 since the only sources of error at the low $d_c/\lambda_c$ value were from those parameters.

As the $d_c/\lambda_c$ values increase, the AC amplitude error from the substrate thickness goes to zero. However, the other substrate and coating parameters begin to affect the results. The substrate parameters ($k_s$, $c_s$, and $\rho_s$) come to discrete, constant values. Previous studies have found that the RMS error in single layer direct problems [13] was related to the thermal effusivity $(\sqrt{\rho_s c_s k_s})$, which explains the error dependence. The coating property sensitivity is less straightforward than the substrate properties. The sensitivity to these parameters changes with the intermediate and high
$d_c/\lambda_c$ values. The most evident example of this increased error is in the $d_c$ value where a 1% error in the thickness could propagate to a 1% error in $q_0$ at $d_c/\lambda_c=1$ and a 3% error at $d_c/\lambda_c=3$.

The sensitivity of the surface heat flux to the coating properties at intermediate and high $d_c/\lambda_c$ values is still dependent upon the lumped parameters of $k_c/d_c$ and $\sqrt{\rho_c c_c k_c}$, as shown in the previous section. However, the coating material properties only affect the AC component of the surface quantities. An increase in $k_c/d_c$ erroneously increases the amplitude heat flux while an increase in $\sqrt{\rho_c c_c k_c}$ erroneously decreases the amplitude. These competing effects lower the sensitivity to $k_c$ measurements as seen in Figure 5-11.

Caution must be taken when using a system of this type to ensure that both the substrate and coating are properly characterized. As shown in this section, even a 1% error in coating thickness could lead to significant errors. To ensure the linearity of the perturbation analysis, a 5% perturbation test was also conducted. The results were five times the values presented in Figure 5-10, building confidence in the linearity of the sensitivity.

5.7 Conclusions

The study outlines the design considerations for two-layer heat transfer gauges using the solution to the corresponding unsteady conduction equation with harmonic surface heat flux. This analysis uncovers the ratio of coating thickness to thermal wavelength ($d_c/\lambda_c$) and the ratio of thermal effusivities ($\sigma$) as the driving parameters of the design. These parameters were found to dictate the feasibility of the design by showing that: 1) a system operating at low $d_c/\lambda_c$ can be treated as a direct problem; 2) a system operating at intermediate values of $d_c/\lambda_c$ requires an IHCP solution; and 3) a system operating at $d_c/\lambda_c > 3$ exhibits excessive thermal damping making reconstruction of surface conditions infeasible.

This study then addresses the need for novel solutions to the IHCP by proposing an alternative to traditional inverse methods through the impulse method. The impulse method does not require regularization, and therefore is well-equipped to handle end applications for which the form of the heat flux may not be characterized. When comparing the two approaches, the impulse method showed lower amplitude errors across all tested values of $d_c/\lambda_c$ at the potential cost of increased phase errors.

When considering real-world factors, such as signal noise, the impulse processing yielded unstable outputs above certain $d_c/\lambda_c$ values. However, the addition of a digital filter as part of the processing scheme improved stability of the solutions. Furthermore, the impulse processing approach was found to be relatively insensitive to errors stemming from uncertainty of thermal and geometric properties. As an exception, the coating properties (particularly the coating thickness)
can cause significant errors at large $d_0/\lambda_c$ values. Therefore, it is important to properly characterize these parameters when employing a gauge of this type.

Overall, this study provides a fundamental framework for the design and processing of multi-layer heat transfer gauges with internal temperature measurements. The design framework will aid in the implementation of surface temperature and heat flux quantification in both new and existing systems in various research and industrial applications. Finally, the impulse processing method offers a new option for this class of inverse problems, enabling user-oriented choices based upon application need.
6 DEVELOPMENT OF COATED HEAT FLUX GAUGES FOR FAST RESPONDING MEASUREMENTS

6.1 Abstract

Thermal systems often exhibit transient behaviors that have important implications for the operation of the system and can be difficult to predict. For these reasons, experimental testing is often required to ensure system durability requirements are achieved. One important parameter governing the survivability of components in hot, high-stress environments is the heat flux into the part that dictates the temperature distribution for the component. However, sensors required to experimentally characterize heat fluxes in extreme environments must also be resilient. This study presents the development of coated heat transfer gauges capable of robust, high-frequency measurements in turbine research facilities. The addition of a protective coating increases the durability of the gauge, but inherent of that coating is the attenuation of high-frequency temperature penetrations. As a result, this study first outlines the use of analytical solutions to define a gauge design for a specific frequency range and heat transfer, ensuring that subsurface signals can be rectified to surface conditions through inverse methods. Then, the fabrication of polyimide substrate sensors with a parylene-F coating is described. Micro surface heaters added to the custom sensors were used to determine important geometric and thermal properties necessary to calculate accurate surface heat flux. Ultimately, this work shows increased sensor robustness in a turbine test bed and experimentally validates that the frequency response of the fabricated sensors meet the design intent.

6.2 Introduction

Cyclic variations in thermal systems are often difficult to predict. One example of such a system is a gas turbine engine. Because multiple stationary and rotating components with unique airfoil counts operate together within the engine, harmonic mechanical and heat transfer mechanisms (often in excess of 35kHz [4]) can fatigue hot, highly-stressed turbine blades that operate on a tight thermal margin [9,135] and, for that reason, accurate time-resolved models are needed. However, such modeling is computationally intensive and often neglects upstream and downstream component interaction, which causes additional undesired harmonic behaviors [3,9]. Accordingly, extensive experimental testing of the cooling system is paramount to quantify the durability of the actively-cooled turbine blades [3]. To this end, a class of high-frequency

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measurement devices have been developed and used extensively within the gas turbine community [9].

Double-sided thin-film resistive temperature detector (RTD) heat flux gauges (HFGs) are uniquely suited for time-resolved temperature and heat flux measurements of turbomachinery components for three reasons: (i) they have a small spatial footprint that is necessary for capturing accurate heat transfer measurements in parts with substantial thermal gradients, (ii) their frequency response is on the order of 100kHz enabling characterization of high-frequency components, and (iii) they are minimally intrusive to the flowfield because a flexible substrate is typically used as the sensor base, which allows them to be wrapped around complex airfoil shapes [16]. HFGs are typically composed of two resistive temperature devices deposited on opposing sides of a flexible substrate. The heat flux is calculated by solving the unsteady conduction equation with RTDs providing the temperature boundary conditions and the thermal properties of the substrate defining necessary inputs.

Sensors of this type have been used for decades in turbomachinery applications. Traditionally, HFGs have been used in short-duration facilities with typical test durations on the order of seconds [13,14,23,136]. As these sensors transition from short-duration environments to more engine-representative testbeds, additional protections must be deployed to ensure the survivability of the gauges [91]. One such protection is the inclusion of a coating on the surface of the traditional sensor.

The inclusion of a protective coating complicates the fabrication, calibration, and processing of double-sided thin-film RTD HFGs. The coating acts as a thermal damper for high-frequency thermal waves at the surface. Therefore, the coating material and thickness must be chosen such that durability is increased while still capturing the desired flow phenomena. Moreover, attenuated subsurface temperature signals must be rectified to surface conditions through the use of indirect heat conduction problem (IHCP) solutions. Previous work [137] provides guidelines on the design of coated sensors systems capable of utilizing IHCP solutions.

The current study first applies the previous design framework and processing foundation [137] to guide fabrication of coated HFGs specifically designed for use in steady experimental turbine facilities. Then, this work presents calibration procedures with experimental validation of the processing methods. Finally, the durability of the selected coating is validated in the Steady Thermal Aero Research Turbine (START) facility at Penn State University.
6.3 Design and Fabrication of Coated Sensors

This section serves as a guide to the design and fabrication of coated heat transfer gauges. The steps presented in this section are generalizable to the creation of heat transfer sensors for a variety of high-frequency applications. However, the development of coated heat transfer gauges for specific use in the Penn State START facility will serve as a representative example of the guidelines throughout the entirety of the paper.

The START facility was designed to investigate airfoil heat transfer [19]. This single-stage turbine is capable of engine-relevant operating conditions with true-scale engine hardware. The facility is powered by two 1.1 MW (1500 hp) compressors capable of providing continuous mass flow to the test section. An in-line 3.5 MW (4700 hp) natural gas heater raises and maintains a main gas path temperature between 395 K to 672 K (250°F to 750°F) while a shell-and-tube heat exchanger cools the secondary air supply to as low as 273 K (32°F). Important to this study, the temperature difference between the main gas path and secondary air sets the magnitude of the heat transfer in the facility for a given aerodynamic condition.

6.3.1 Gauge Design

High frequency heat transfer gauges are required to meet specified criteria critical to the end application. Often, these criteria are linked to the mean heat transfer rate and maximum frequency and magnitude of the time-varying components. One example of a frequency criteria in turbomachinery is the blade passing frequency (BPF), which can drive heat transfer at well-defined regularities. For that reason, the BPF will serve as the minimum frequency response needed for turbomachinery applications.

Table 6-1 outlines the criteria for the frequency and magnitude used in this particular study. The values in Table 6-1 are typical of turbomachinery applications [13] with exact values determined through the use of computational models at specific testing conditions within the START testbed. Table 6-1 also details the material choices and thermal properties for the study. The thermal properties for the substrate come from previous tests conducted for the same material [134], while the manufacturer specifications were used for the parylene-F coating properties [138]. These materials and thicknesses were selected for design purposes and will be compared to measured values in subsequent sections.
The selection of polyimide as a substrate and parylene-F as a dielectric coating were constrained foremost by their mechanical flexibility because the application of these sensors requires adhesion around complex airfoils. Additionally, each material must maintain adequate thermal and mechanical properties up to 505 K (450 °F). Furthermore, these materials must be conducive to nanofabrication processes to ensure proper adhesion between layers.

After establishing the end application criteria and coating choices, several design parameters are also required: (i) substrate thickness, (ii) coating thickness, (iii) RTD shape, and (iv) RTD material. In practice, the RTD material is constrained by the electro-thermal properties of known materials and is usually nickel [13,99] or platinum (used for current study) [28,46,91]. Moreover, the shape of the RTD for the application of HFGs is constrained in this study by the pursuit of in-situ calibration techniques [139], which requires a rectangular shape with large length-to-width ratios (L/2b = 10 for current study). Therefore, this section will focus on the thickness choices for the substrate and coating.

Figure 6-1 shows the general layout of the coated sensor. This design uses platinum thin-film RTDs with copper leads deposited on opposing sides of a flexible polyimide backing and a parylene-F coating deposited on top. Figure 6-1 also illustrates the thermal properties necessary to deduce the time-resolved surface heat flux (q₀) from the internal temperature measurements (T₁ and T₂).
It is useful to think of these measured ($T_1$ and $T_2$) and calculated ($q_0$ and $q_1$) quantities in terms of a mean and fluctuating component such that the time-resolved arbitrary variable $N$ is

$$N = \overline{N} + N'$$  \hspace{1cm} (6-1) 

where $\overline{N}$ is the mean component and $N'$ is the fluctuating component. The mean heat flux measurements are dependent on the thermal resistance of the substrate layer, but independent of the coating. Consequently, the substrate thickness was chosen based upon the mean heat transfer rate and allowable uncertainty in the temperature. This relationship is expressed through Equation (6-2):

$$\overline{q_0} = \frac{k_s \Delta T}{d_s}$$  \hspace{1cm} (6-2) 

where $k_s$ is the substrate thermal conductivity, $d_s$ is the substrate thickness, and $\Delta T$ is the temperature difference between $T_1$ and $T_2$. To minimize uncertainty in mean heat flux, it is advantageous to maximize $\Delta T$ across the substrate. As outlined in Equation (6-2), this can be accomplished by increasing the thickness or choosing a substrate material with lower thermal conductivity. Because the thermal conductivity was predetermined in this study based on the substrate material selection, the thermal resistance of the sensor is controlled through the substrate thickness. There exists a trade-off between increasing the measured temperature difference and changing the mean heat transfer in the experiment. Therefore, appropriate care must be taken to ensure the measurement device itself does not affect the thermal results. If the thermal resistance of the sensor gets too large, it will act as an insulation layer causing the experimental setup to...
deviate from design intent. Particular to this study, a \( d_c \) of 50 microns was used which has been previously shown to have negligible effects in similar flow conditions [49].

In addition to considerations of maximizing temperature difference across the substrate, the coating thickness must be chosen to ensure that frequencies of interest are not entirely damped and can be corrected to surface conditions. One available design tool for choosing this parameter is the two-layer analytical solution to the unsteady conduction equation under harmonic heating. The solution to the two-layer harmonic problem outlined by Siroka et al. [137] develops the relationship between the temperature amplitude at the defined sensor locations (\( \hat{T}_1 \) and \( \hat{T}_2 \)) relative to the surface temperature (\( \hat{T}_0 \)):

\[
\frac{\hat{T}_1}{\hat{T}_0} = \frac{2 \exp \left( \frac{d_c}{\lambda_c} \right)}{\sigma + 1 + \exp \left( -\frac{2d_c}{\lambda_c} \right) (1-\sigma)}
\] (6-3)

and

\[
\frac{\hat{T}_2}{\hat{T}_0} = \frac{2 \exp \left( -\frac{d_c}{\lambda_c} \right) \sigma \exp \left[ \left( \frac{d_c}{\lambda_c} \right) \left( \frac{k_c}{d_c} \right) \left( \frac{k_s}{d_s} \right) (\sigma) \right]}{\sigma + 1 + \exp \left( -\frac{2d_c}{\lambda_c} \right) (1-\sigma)}
\] (6-4)

where the thermal coating wavelength is defined as

\[
\lambda_c = \sqrt{\frac{2\alpha_c}{\omega}}
\] (6-5)

and where the ratio of thermal effusivities (\( \sigma \)) is

\[
\sigma = \sqrt{\frac{\rho_c k_c}{\rho_s c_k}}
\] (6-6)

Figure 6-2 plots these solutions (using values from Table 6-1) and shows the ratio of the measurement-plane temperature amplitudes to surface temperature amplitude across a range of nondimensional coating thicknesses (\( d_c/\lambda_c \)). The nondimensional coating thickness is an important
parameter because it represents the factors affecting the attenuation of the measured signal. The design intent plotted in Figure 6-2 is based on the nondimensional coating thickness at a periodic heating frequency, $\omega_{BPF}$, for a coating thickness of 1µm as outlined in Table 6-1, which will be justified subsequently.

![Graph showing ratio of surface temperature amplitude to measurement plane temperature amplitudes across a range of non-dimensional coating thicknesses with the selected HFG design intent.](image)

**Figure 6-2. Ratio of surface temperature amplitude to measurement plane temperature amplitudes across a range of non-dimensional coating thicknesses with the selected HFG design intent.**

Figure 6-2 presents several key aspects about the sensor operation. First, at $d_c/\lambda_c = 3$, both $T_1^\wedge$ and $T_2^\wedge$ are almost entirely damped relative to the surface temperature fluctuations. This damping represents the loss of information as heat dissipates through the coating and substrate layers. For a given coating thickness value and material, this limit sets the maximum frequency response of the sensor. Second, at design intent ($d_c/\lambda_c \approx 0.5$), the $T_2^\wedge$ is completely damped from the upstream layers. Consequently, the gauge at the design intent will operate in a semi-infinite mode meaning the fluctuating heat transfer at the frequency of interest depends only on the temperature measurement at the outward-facing coated surface, $T_1^\wedge$. Third, at the design intent, the $T_1^\wedge$ measurement is attenuated from surface conditions due to the coating, by a magnitude of nearly 50%. For this reason, ICHP solutions using inverse methods will be applied to rectify the measured temperature to the true surface temperature.

In practice, the coating thickness must be chosen to quantify a specific heat flux level relative to the noise floor of the system. Equations (6-3)-(6-4) presents the ratio of measurement to surface temperatures, which is independent of the surface heating value. In contrast, Figure 6-3

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7 It should be noted: to reconstruct the entire frequency spectrum, $T_2$ is still a necessary input to sufficiently resolve the low frequencies.
shows the temperature amplitude of the top sensor ($\tilde{T}_1$) with a periodic heating at the defined frequency, $\omega_{BPF}$, over a range of coating thickness values for three different heating levels. The formulation of this solution was presented by Siroka et. al [137] and uses the material properties outlined in Table 6-1. Figure 6-3 also identifies a representative noise floor (0.15 °C) at the selected BPF for a demonstrative data acquisition system defined here as the combination of HFG signal excitation, filtering, and acquisition. The intersection of the noise floor with the heating curves initially prescribes the limit on the measurable amplitude of instantaneous heat flux for a given coating thickness. However, as will be explored in subsequent sections, signal filtering and averaging can be used to resolve smaller values of heat flux amidst the noise.

![Figure 6-3. Amplitude of the top temperature sensor at the representative BPF across a range of thicknesses for three different heat flux levels.](image)

6.3.2 Gauge Fabrication

The HFGs used in this study were fabricated at the Penn State University Nanofabrication Laboratory using a custom procedure detailed by Siroka et al. [91]. The fabrication process involves both additive and subtractive processes starting with a commercially-available Pyralux, which is a 50 µm thick polyimide cladded on both sides with 9µm of copper. In the first part of the process, copper is removed through an etching process to create the HFG leads. Next, a 1500 Å platinum layer is selectively added through an evaporative deposition process to create the RTD elements. Double-sided sensors can be created by repeating a similar process on the opposite side of the substrate.

The addition of a parylene-F protective coating over the sensors is a new contribution for this study. Parylene-F is inert and chemically resistant, which provides protection against moisture
and corrosion [138]. Before applying the coating, an adhesion promotion process was performed using Silane 147A. The parylene-F was then deposited through a thermal evaporation process that creates a complete-coverage conformal film to encapsulate the copper and platinum layers. Following the deposition, a profilometer was used to quantify the corresponding thickness, which was measured to be $1.00 \pm 0.02 \, \mu m$, illustrating the precise thickness control of the parylene-F coating process.

Figure 6-4(a) and Figure 6-4(b) show the completed uncoated and coated gauges, respectively. It is also noted that Figure 6-4 characterizes two distinct sensor build outcomes, not the same sensor after different processes. These different builds were intended to quantify the durability of the coated sensor to its uncoated counterpart, an effect that will be assessed in more detail in a subsequent section. The design intent for the RTD was a full-width (2b) of 50µm and a length (L) of 500µm. Comparing Figure 6-4(a) to Figure 6-4(b), there is a notable decrease in the platinum-to-polyimide contrast caused by the addition of a coating. To better differentiate platinum from polyimide, a high contrast Koehler illumination is shown in Figure 6-4(c) and Figure 6-4(d) corresponding to the standard illumination shown in Figure 6-4(a) and Figure 6-4(b). Evaluation of Figure 6-4(c) and Figure 6-4(d) shows that the coating did not cause discontinuities within the platinum. Although only a small portion of sensors were used to generate the data for this study, over 200 coated and 200 uncoated sensors were fabricated during this process. No notable differences were present in the yield of the devices.
6.4 Calibration of Coated Gauges

To deduce accurate heat flux from the coated gauges, an electrothermal calibration and a material characterization is necessary. The electrothermal calibration transforms the measured voltage across the RTDs to a temperature. These temperatures must then be processed with correct material properties to accurately calculate the thermal energy entering the surface of the HFG. This section outlines the RTD calibration procedure followed by the process used to determine the gauge thermal properties and frequency response for a coated HFG sensor.

6.4.1 Electrothermal Calibration

An accurate and repeatable thermal calibration is necessary for HFGs to operate as intended. To ensure the repeatability of the RTDs, the sensor was annealed under vacuum at 250°C after fabrication until the nominal resistance change in the RTDs was less than 0.05% over the span of an hour. This annealing process has been explored in prior studies [139] and has been shown to improve the long-term operation of the devices.

After the annealing process, the coated HFG was placed into a scientific convection oven with a stability rating of 0.2 °C. A precise excitation current of 1.0mA excites the RTDs as outlined in Figure 6-1, and the measured voltage from the RTD was amplified to improve the signal-to-
noise ratio. When thermal equilibrium was achieved, RTD voltages and oven temperature were collected. This procedure was repeated for several temperatures to create the calibration curves shown in Figure 6-5 which are expected to be linear due to the thermoelectric properties of platinum. The calibration curves presented in Figure 6-5 provide a transformation from the measured voltage to the temperature of the RTDs. More information about the annealing and calibration processes can be found in previous works [91,139].

![Figure 6-5. RTD calibration curve for the two sensors comprising the coated HFG.](image)

6.4.2 Thermal Property and Frequency Response Determination

Thermal properties of differential heat flux measurement devices must be properly characterized because they are often the largest contributor to measurement uncertainty. To determine the thermal properties of these particular devices, a heater was deposited on the top of the coating layer, as shown in Figure 6-6. The heater element was fabricated from platinum using the same techniques detailed previously. A small portion of the sensor yield lot were subjected this additional deposition process under the assumption that the material properties of the gauge below the surface heater are representative of the entire build of sensors.
The addition of a surface heater allows for a known heat flux at the surface, which can then be measured using the HFG. Figure 6-7 illustrates the experimental excitation of this surface heater. The heater current, $I_0$, was set though the use of a custom-designed precision current supply capable of modulating the current at frequencies from 0-50kHz within 0.05% uncertainty of the current amplitude. This excitation current is represented in Figure 6-7(a) over the course of two cycles. The current through the surface heater creates voltage modulations at the same frequency as illustrated in Figure 6-7(b). The heat flux dissipated on the surface ($q_0$) can then be calculated though Equation (6-7):

$$q_0 = \frac{V_0 I_0}{A_h} \quad (6-7)$$

where $I_0$ is the set current, $V_0$ is the voltage drop across the heater that is in-phase to the current, and $A_h$ is the cross-section area of the heater. As shown in Figure 6-7(c), this creates a heat flux at twice the frequency of the current and the voltage. The corresponding heating frequency ($f_h$) will be used throughout the remainder of this study to quantify the response of the device across different spectral ranges. Figure 6-7 serves as an example of one such $f_h$ value.
Figure 6-7. Example traces of a) set cyclic variation in heater current; b) measured voltage drop across the heater; and c) calculated surface heat flux across 4 heating cycles (2 current/voltage cycles).

Figure 6-7(c) illustrates that the surface heat flux ($q_0$) has a mean and fluctuating component. Importantly, the mean component through the device is not always equal to the fluctuating component. In a 1D analysis, the mean component will encounter an entire resistive network based upon the convection above the device and all the lower layers. However, the fluctuating component will be constrained to the resistance within the thermal penetration depth of the frequency. For that reason, the fluctuating components are much less sensitive to convection and radiation effects [55,109]. Therefore, the fluctuating components will be used to determine the thermal properties.

To determine the thermal properties, both the boundary condition ($q_0$) and the temperature measurements ($T_1$ and $T_2$) are necessary. The previously described surface heating was damped through the coating and substrate layers causing attenuated temperature readings from the subsurface RTD measurements. Figure 6-8 displays the $T_1$ and $T_2$ measurements at different frequencies for the same heating power over 4 heating cycles (representative of Figure 6-7(c)). The signals in Figure 6-8 were acquired at 500kHz for 3 seconds. Displayed in Figure 6-8 is the filtered signal with the raw (translucent silhouette) signal behind it. The signal was filtered following the
solution framework outlined by Siroka et al. [137] using a zero-phase moving average filter constructed for a given heating frequency.

Figure 6-8. Temperature traces from the subsurface RTDs in response to cyclic surface heating at a) 3 Hz, b) 300 Hz, and c) 8000 Hz.

Figure 6-8 confirms several characteristics analytically predicted by Figure 6-2. As the frequency increases, both $\bar{T}_1$ and $\bar{T}_2$ diminish. At the intermediate frequency of $f_h = 300$ Hz (Figure 6-8(b)), $\bar{T}_2$ is damped to a nearly DC output while $\bar{T}_1$ still shows a distinct frequency response. At the design intent value, $f_h = 8000$, both the $\bar{T}_1$ and $\bar{T}_2$ signal appear to be within the noise in the instantaneous view. This may seem contradictory to Figure 6-3, but stems from lower heating values than the design intent, which were limited by the allowable current through the surface heater.

To significantly reduce the noise floor of the experimental system and more accurately obtain the frequency cutoff and material properties, a lock-in amplifier was utilized. Lock-in amplifiers are ideal devices for detecting signals at prescribed frequencies in noisy environments [140]. For this experimental setup, the in-phase and out-of-phase voltage signals were measured with respect to the supplied current, which was used as a reference signal. Signal resolution was increased 100x through the lock-in amplifier when compared to conventional amplification. The left ordinate in Figure 6-9 shows $\bar{T}_1$ (plotted in symbols), while the right ordinate displays $\bar{q}_0$ (plotted in lines). The logarithmic abscissa represents the heating frequency. For this experiment, the heating frequency was swept between 2-30,000 Hz at 100 logarithmically spaced points. Every fifth point is plotted in Figure 6-9 for ease of interpretation.
Three discrete current values, $I_0$, were chosen to create varying levels of heating fluctuations, as identified in Figure 6-9. These three $I_0$ values were on the same magnitude as the excitation current for the RTDs (1.0 mA for both $I_1$ and $I_2$). However, the heater resistance was much greater than the RTDs, making the heat flux through the heater much higher than the RTDs. To ensure the excitation currents has a negligible effect on the results, this procedure was repeated with an order of magnitude lower excitation RTD current. Although not shown here for brevity, Figure 6-9 was reproduced using the lower excitation current and no notable differences were seen in the measured temperatures, building confidence that the RTD current was sufficiently small compared to the supplied heat flux from the surface heater.

Figure 6-9 generally follows anticipated trends – in particular, the temperature rise is increased at high heat fluxes. Figure 6-9 also builds a better understanding of the frequency response of this device. For frequencies up to approximately 15kHz, there is a measurable difference in the $T_1$ values between the different heating levels. This finding builds confidence that the frequency response is indeed within the design intent. Furthermore, at low frequencies, the measured heat dissipated through the heater was significantly lower than at higher heating frequencies due to capacitance effects in the heater. To account for these effects, only the in-phase voltage component to the current was used to calculate the heat flux.

Although the absolute value of the measured $T_1$ depends upon $q_0$, the thermal properties of the gauge should be independent of the selected heating values. Therefore, it is possible to collapse the curves in Figure 6-9 by normalizing the temperature amplitude by the heat flux amplitude. Figure 6-9 illustrates the data presented in Figure 6-9 though this form as open symbols, and the same relationship is added showing $T_2/q_0$ with filled symbols. Comparing the two
measurement locations in Figure 6-10, the lower amplitude of the $T_2$ measurement plane highlights the expected damping effect relative to the $T_1$ measurement plane. To quantify this effect, Figure 6-10 identifies the respective cutoff frequency (defined as 0.05% of the initial value) for the $T_1$ and $T_2$ plane. Following this convention, the cutoff frequency for $T_2$ ($f_{c,2}$) was determined to be 233 Hz, while the cutoff frequency for $T_1$ ($f_{c,1}$) was determined to be 13,800 Hz.

![Figure 6-10. $T_1/q_0$ and $T_2/q_0$ across a range of heating frequencies with a 2-layer model plotted based on determined thermal properties. The maximum frequency response of each individual RTD is characterized by dashed lines.](image)

As previously addressed, thermal properties are necessary to accurately convert the measured temperature to the surface heat flux. The thermal properties of this device were obtained following an iterative approach. Impulse response filters for the coated gauge [18,137] were constructed using the nominal design parameters in Table 6-1 as inputs including $k_c$, $(\rho_c c_v k_c)^{0.5}$, $k_c/d_s$, and $(\rho_c c_v k_c)^{0.5}$. These filters utilize inverse methods to transform the measured temperature to surface heat flux and rely on accurate thermal properties of the coating and substrate. The created filters were used with the data in Figure 6-10 across the entire frequency range and the root-mean-square errors were calculated. A Nelder-Mead Simplex Method minimization scheme [141] was employed to perturb the thermal property filter inputs and minimize the root-mean-square errors between the input heat flux and the calculated heat flux. Table 6-2 displays the thermal properties deduced from this process and compares them to the nominal design values. The values in Table 6-2 were between 2.6-7.7% different from the design values in Table 6-1 with the exception of $d_c$ which was measured with a profilometer and therefore held as a constant input.
Table 6-2. Comparison of Design Thermal Properties to Measured Values

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Design</th>
<th>Measured</th>
<th>Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>((\rho_s c_s k_s)^{0.5})</td>
<td>501</td>
<td>532</td>
<td>5.6%</td>
</tr>
<tr>
<td>(k_s/d_s)</td>
<td>3800</td>
<td>3842</td>
<td>1.1%</td>
</tr>
<tr>
<td>((\rho_c c_c k_c)^{0.5})</td>
<td>407</td>
<td>441</td>
<td>7.7%</td>
</tr>
<tr>
<td>(k_c)</td>
<td>0.12</td>
<td>0.117</td>
<td>2.6%</td>
</tr>
<tr>
<td>(d_c)</td>
<td>1E-6</td>
<td>1E-6</td>
<td>0%</td>
</tr>
</tbody>
</table>

Determining the thermal parameters across an entire frequency range helps to ensure proper sensitivity to appropriate parameters. For example, at low frequencies, the impulse filters are more sensitive to direct mode heat transfer parameters such as \(k_s/d_s\). On the other hand, coating properties and the thermal effusivity of the substrate become increasingly important at high frequencies [137]. As shown in Figure 6-10, these parameters can then be used with the simplified 2-layer model to design devices according to desired specifications.

Comparing the measured data (symbols) and the analytical model (lines) in Figure 6-10, the identified discrepancy is partially attributed to the fact that the model assumes a semi-infinite substrate whereas the impulse filters do not. However, the simplified model accurately captures the shape of the curve and becomes more accurate with increasing frequencies, making it a useful design tool for the first-order prediction of the frequency response and coating thickness determination.

6.5 Experimental Verification

This section verifies that the material properties determined through the use of a lock-in amplifier and presented in the previous section will work with traditional amplification in the presence of signal noise, which can typically introduce processing challenges. First, the linearity of the mean heat flux will be verified. Then, the time-resolved heat flux deduction capacities will be quantified for two test cases.

6.5.1 Mean Heat Flux Verification

Based upon Equation (6-2), the mean heat flux, \(\overline{q_0}\), should be a linear function of the change in temperature between the RTDs with a slope equal to \(k_s/d_s\). This relationship is illustrated in Figure 6-11, which plots \(\overline{q_0}\) against \(\Delta T\) across the HFG RTDs for a set of chosen frequencies. Figure 6-11 also displays the linear line with a \(k_s/d_s\) = 3842 [Wm\(^{2}\)K\(^{-1}\)], as determined in the previous section. The values of \(\overline{q_0}\) plotted on the ordinate represent the calculated heat flux based upon Equation (6-7) with corrections for mean heat flux lost to the surrounding environment. The
uncertainty bounds plotted in Figure 6-11 account for the uncertainty of the temperature calibrations, heater currents, and voltage acquisition, leading to an estimated overall uncertainty of 8% for mean heat flux. Through this approach, the linearity shown in Figure 6-10 builds confidence in the ability to accurately measure mean heat flux independent of frequency. This relationship relates to the temperature traces illustrated in Figure 6-8 through the insensitivity of the mean temperature difference across a range of frequencies.

![Graph showing mean heat flux vs temperature difference for different heat frequencies](image)

**Figure 6-11.** Measured mean heat flux plotted against the measured temperature difference for different heat frequencies under normal amplification.

### 6.5.2 Fluctuating Heat Flux Verification

To verify the thermal properties important to the fluctuating heat flux in the previous section, the filtered temperature traces in Figure 6-8(a) and Figure 6-8(b) were processed using impulse response filters and are shown in Figure 6-12(a) and Figure 6-12(b) respectively. In Figure 6-12, the values calculated using an impulse response approach are shown as solid lines with the measured $q_0$ values as dashed lines. Note that at 3 Hz, the component of the voltage in phase with the current was smaller than at 300 Hz as evidenced in Figure 6-9, explaining the lower amplitude. These two cases are representative of the two operating regions of the HFG. At 3 Hz, both the $T_1$ and $T_2$ are critical to reconstruct the surface heating. At 300 Hz, only the $T_1$ trace is necessary for the fluctuating component, however, $T_2$ is still needed to quantify the mean value.
Figure 6-12. Instantaneous $q_0$ over four surface heating cycles at a) 3 Hz and b) 300 Hz.

Based on the two test cases in Figure 6-12, the relative amplitude difference between the calculated impulse response value and the measured heat flux is approximately 30% at $f_h = 3$ Hz and 10% at $f_h = 300$. This identified discrepancy at the lower heating frequency is likely due to the uncertainty in measured heat flux when a large phase lag between the voltage and current is present. Further investigation is warranted to fully understand this issue.

6.6 Durability Improvements

Until this point, the present study has detailed the design, fabrication, and complex processing necessary to account for the coating in high-frequency heat flux measurements. However, the primary function of the proposed coating is to add durability to the sensors. To test the durability of the chosen parylene-F coating compared to its uncoated counterpart, both types of sensors were installed in the START facility at Penn State University upstream of the test section. The sensors were adhered to a 0.5" diameter probe along with a reference thermocouple (TC). Every uncoated sensor failed during the probe fabrication process, showcasing the poor durability from the outset. However, all of the coated gauges subject to the same processes at the same time remained functional. This initial qualitative outcome builds confidence in the robustness of the gauges.

After installation, six coated sensors were monitored over four consecutive days of a turbine test program to quantify the linearity of the sensors with respect to the TC. Figure 6-13 illustrates the voltage of a single RTD against the change in temperature of the nearby TC. A photo of the probe can be found in the bottom right of the plot. Because Figure 6-13 represents a durability test, there was no specified heat flux through the part. Therefore, only $V_1$ (a proxy for $T_1$) values were obtained.
The different markers within Figure 6-13 correspond to different days of testing. The agreement of the voltage measurements over this period illustrates the repeatability of the coated sensor design over repetitive test days and conditions. If calibration drift or resistance (voltage) changes due to surface erosion or oxidation were present over the course of the test, the linearity of the curve would have changed in the course of testing. However, a linear fit of the data in Figure 6-13 results in an $R^2$ value of 0.998 while the mean deviation between the fit and the experimental data is $0.15^\circ\text{C}$ across all test days. This result confirms that coating is providing the necessary protection from the experimental environment.

To add perspective, Figure 6-13 showcases that this sensor was functioning through 18 hours of operation in a heated open loop facility just downstream of a combustion chamber with the remainder of the time representing cooldown periods between days (the facility does not operate 24 hours a day). These 18 hours of testing equate to roughly 20 years of testing runs in a short-duration facility [91]. Importantly, these devices were still functional upon removal.

6.7 Conclusions

Sensor survivability is one of the most important parameters for successful instrumentation deployment; the design, calibration, and processing schemes are irrelevant if the sensor is inoperable at installation. To that end, this study details the development of advanced heat flux gauges capable of robust high-frequency measurements. First, using analytical solutions to the unsteady conduction equation, this work outlines the governing nondimensional parameters critical to the design of coated heat transfer gauges. Specifically, the nondimensional coating thickness ($d_c/\lambda_c$) was shown to be important to the attenuation of subsurface temperature signals. Given a set
of end user heat transfer specifications, these processes were implemented in a practical manner to
design a coated sensor applicable to turbine research facilities.

The sensors were manufactured using nanofabrication processes at Penn State University. The additional processing steps required to create a coated gauge were found to have no effect on device yield. Furthermore, a subset of the coated gauges were fabricated with an added surface heater to characterize the frequency response and thermal properties of the build. By modulating and measuring the surface heat flux at known frequencies, the frequency response of a coated heat flux gauge was characterized to be 13.8 kHz.

The thermal properties of the gauge were obtained through iterations of impulse response filters minimizing the root-mean-square error between the input heat flux and the heat flux calculated based upon the temperature signals. The gauge design and characterization outlined herein represents a unique approach for validating sensor design. Although there is an increased fabrication complexity from the addition of a surface heater, calibrating and verifying in this manner provides a complete system check that accounts for the calibration of the RTDs as well as the thermal properties of the system. Using the impulse response filters with correct thermal properties, the mean and time-varying heat flux error was quantified with respect to the measured surface heat flux. Mean heat flux errors of 8% and fluctuating heat flux errors of 10-30% were noted.

The current work showcases the successful implementation and durability enhancement from the addition of a coating in a turbine test facility. Initial results show promising durability improvements for experimental testbeds of this type. This study has also addressed several challenges with the design and implementation of coated sensors. Ultimately, methods presented throughout this document can be extended to a variety of applications in the thermal-fluidic sciences and serves as a guide for the design and validation of coated thermal sensors.
7 CONCLUSIONS

The research conducted and reported in this dissertation advanced the development of thin-film RTD HFGs for use in steady flow turbine test facilities. Several adaptations from traditional HFG usage were investigated to enhance sensor robustness while adapting from short duration (transient) facilities to harsh steady testbeds. The previous chapters have detailed the need, design, fabrication, calibration, and application of such devices. This section reflects on those findings and offers key takeaways from the knowledge gained through: i) the application of these sensors to study turbine rim seal flows, ii) the development of novel calibration methods for HFGs, and iii) an assessment of coated sensors. Finally, recommendations for the future work regarding this technology maturation is presented.

7.1 Time-resolved Flow Measurements in Turbine Rim Seals

The physics driving turbine heat transfer and rim flows, in particular, are inherently unsteady. Multifaceted mechanisms such as annulus pressure distortions from vane wakes and rotor pumping create time-varying ingestion. Further complications can arise in the presence of flow instabilities driven by a velocity mismatch between the main gas path and cavity region. Therefore, high-frequency temperature and pressure sensors were utilized at engine relevant operating conditions on true-scale hardware to characterize cavity instabilities and relate them to the performance of the seal. This study, fully detailed in Chapter 3, showcased the benefit of high-frequency thermal sensors when investigating time-varying flows in turbomachinery applications.

This dissertation pioneered the use of high-frequency temperature sensors to study rim seal performance. Several key takeaways from this study arise from the addition of this sensor type. First, the utilization of the high-frequency thermal sensors to characterize the thermal efficiency of the cavity created a more direct relationship between ingestion and component durability. Second, the inclusion of this additional sensor with high-frequency pressure measurements enabled time-varying pressure signals tied to ingestion to be distinguished from those less significant to seal performance.

7.2 Calibration processes for HFGs

This dissertation improved the calibration procedure for HFGs through the use of the 3-omega technique which created a twofold benefit to HFG calibration: in-situ calibrations and thermal property determination on a per gauge basis. This technique, originally developed to characterize thermal properties, was incorporated with HFGs seamlessly through the purposeful design of the sensors excited with an alternating current instead of a traditional constant supply.
The overall uncertainty in the thermal conductivity and thermal product measurement was found to be 2.9% and 4.9% respectively.

Thermal property determination based upon bulk substrate samples were previously assumed to be representative of the substrate thermal properties directly below the RTDs used for HFGs. However, this technique illustrated that HFGs from the same fabrication processing could have thermal conductivity values that differ by as much as 20% for a given temperature. This novel HFG calibration procedure therefore increases the accuracy of the device through proper thermal property determination that avoids the bulk assumption.

Additionally, this process provides in-situ calibrations for the coefficient of resistance for a sensor. When applying this technique, deduced $\alpha_R$ values differed from the measured $\alpha_R$ by 0.1%. This capability is essential for instrumentation that may not be easily removed and recalibrated, such as HFGs inside of a turbine test facility.

### 7.3 Assessment Coated Sensors

Sensor coatings increase the durability of the device at the loss of frequency response. This dissertation built a framework for the design and processing of coated sensors. Using the unsteady conduction equation with periodic heating as a starting point, this work theoretically detailed and experimentally determined that the nondimensional coating thickness was a key parameter to the design of coated gauges. From this foundation, coated gauges were designed, fabricated, and validated using a novel impulse response inverse method to calculate surface heat flux from subsurface temperature sensors.

As stated in Chapter 6, gauge survivability is the most important parameter to instrumentation of this type. The innovative design, calibration schemes, and processing are irrelevant if the sensor is inoperative at installation. Within this document, both uncoated (Chapters 2-4) and coated (Chapters 5-6) HFGs were characterized. The coated gauges, although requiring more complex fabrication, calibration, and processing, have shown substantial durability improvements over their uncoated counterpart. For that reason, coated gauges are recommended for the majority of applications.

However, the implementation of coated gauges does not come without cost. In Chapter 1 of this document, it was estimated that uncoated sensors have a 10% uncertainty where a large driving factor was the uncertainty in the thermal properties of the device. In Chapters 5 and 6 of this document, it was illustrated coated sensors have additional uncertainty compared to uncoated sensors that arise from the additional material property determination and inverse processing necessary to correct for surface conditions. The estimated uncertainty for the coated sensors is 10%
for the mean heat flux (only depended upon the same parameters as an uncoated sensors), and between 10-30% for time-resolved components. This uncertainty stems from the material property characterization and should be improved upon through future research endeavors.

Additionally, uncoated gauges exhibit a substantially higher frequency response. If a suitable nondimensional coating thickness cannot be chosen to capture necessary flow phenomena, uncoated sensors may be required. Moreover, the \textit{in-situ} calibration techniques outlined in Chapter 4 were only tested on uncoated sensors. Further investigation is warranted to apply the same procedure to coated HFGs, which has the potential to drastically reduce the uncertainty of coated gauge heat transfer calculations.

### 7.4 Recommendations for Future Work

The work presented within this dissertation provides an intermediate step to the maturation of this sensing technology into a myriad of applications. However, there are still high technical barriers in both fabrication and validation that must be solved for this sensing technology to reach its full potential. The ensuing discussion formulates a roadmap for the future of this technology in three different areas: i) sensing in experimental turbine testbeds, ii) development sensors for engine testing, and iii) integration of sensors to characterize thermal properties.

These sensors are positioned to have an immediate impact to the validation and improvement of the thermal management in gas turbines. The START facility is the only steady turbine testing facility in the world with this advanced sensing capability. Future START programs should implement the capabilities developed within this dissertation to investigate innovative cooling schemes within turbine engines. Coupling this technology with advanced IR capabilities could accelerate pioneering cooling strategies at relevant engine operation, leading to more efficient engines. A byproduct of this testing would be high-frequency experimental data that could be used to validate time-accurate simulations and physics-based life predictions of turbines.

Concurrent to utilizing the technology developed in this dissertation, it is important to innovate fabrication methods and material choices for improved coated and uncoated sensors. Robustness increases without the addition of coatings should be explored. In particular, the use of a chromium layer could increase survivability of the sensors by enhancing the adhesion of the RTDs to the polyimide backing. For coated gauges, it is imperative to explore alternative coating materials with enhanced properties compared to the polymers used in this work. Specifically, the material class of metallic oxides should be explored for the characteristic protective hardness with relatively high thermal diffusivity compared to polymer coatings.
Beyond the laboratory setting, the processing methods presented here are of critical importance to the advancement of high-frequency heat transfer gauges to engine testing. With the advent of additive manufacturing and direct deposition sensors, it is feasible that high-frequency heat transfer gauges could be directly built into components such as turbine blades for advanced testing. One critical challenge to overcome is the creation/application of a dielectric coating material before the deposition process. Future efforts should be spent in the trial of different dielectric coatings that would enable direct deposition of the platinum RTDs to the airfoil. A thermal barrier coating, commonly used in production turbine blades, could be used as the substrate before a topside sensor and protective coating is placed.

Finally, this sensor technology can be leveraged to provide thermal properties for many applications both within the turbomachinery discipline and beyond. Many times, extensive testing is necessary to quantify all thermal parameters of interest. Consequently, this testing is not conducive to testing individual specimens. As shown in Chapter 4, these sensors can be used in a bidirectional manner to characterize the thermal properties of the material above or below the sensor itself. This promising area of research would enable materials to be thermally characterized before the end application. For example, these sensors could be embedded into the build plate of additive manufacturing processes to provide feedback to the machine on the correct build parameters as well as a quantification of the thermal properties to the end customer. One technical challenge is the advanced excitation and filtering required to obtain this information. Significant effort must be placed on the creation of electronics that are purpose built around this application. In this way, the physical footprint of the electronics could be reduced allowing for easier integration into existing systems. Additionally, processing validation over a variety of materials would be necessary before implementation.

Overall, this dissertation is poised to have implications extending beyond the studies contained within it. The work presented within this dissertation is ultimately aimed at the creation of novel measurement devices using new techniques that enable measurement robustness without sacrificing accuracy. Therefore, the themes presented throughout this document may find use in areas of the thermal-fluidic sciences and the information presented throughout this document may serve as a pivotal piece of this technology advancement.
REFERENCES


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Shawn Siroka

Shawn Siroka originates from the Borough of West Hazleton, PA where he was provided with both a strong moral and educational background at Hazleton Area High School. Mr. Siroka, a proud Penn Stater, received his BS and MS in Mechanical Engineering at University Park in 2016 and 2019 respectively. After, Shawn pursued a PhD at the Steady Thermal Aero Research (START) Lab under the guidance of Dr. Karen Thole and Dr. Reid Berdanier. His dissertation research utilizes nanofabrication processes to manufacture thermal energy sensors for gas-turbine applications.

Mr. Siroka has published his research findings in five archival journal papers and several non-archival papers and presentations at national and international conferences. He also has two pending patents gained through his research efforts. At the university level, Mr. Siroka has held two peer-elected positions as the Graduate and Professional Student Association (GPSA) College of Engineering (COE) Delegate and University Park Allocation Committee (UPAC) Graduate Student Representative in 2017. At the international level, Mr. Siroka was elected as the ASME International Gas Turbine Institute (IGTI) Student Advisory Committee (SAC) Chair. Through this three-year position, he helped to organize the largest turbomachinery conference in the world while campaigning for student-centric activities. During his tenure, he coordinated a student paper review, a student poster session, and tutorial session for technical presentations.

Mr. Siroka has received numerous accolades for his academic achievements, volunteerism, and research efforts. Specifically, he has been awarded the Alan J. Brockett Award in 2019, the NASA Space Grant Scholarship in 2018, the W.M. Rohsenow Presentation Prize in 2017, and an ASME IGTI Scholarship and travel award in 2016. Upon graduation, Shawn will be employed with Industrial Technology Research (ITR) in Bethlehem, PA. Through this position, Mr. Siroka will further his passion and understanding of sensor design, installation, and signal processing related to predictive maintenance for industrial applications.