 INTERFACE CHARACTERIZATION AND
DEFECT EVOLUTION OF COATED SAMPLES
UNDER THERMAL AND PRESSURE
TRANSIENTS

A Dissertation in
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by

Jason T. Harris

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The dissertation of Jason T. Harris was reviewed and approved* by the following:

Albert Segall  
Professor of Engineering Science and Mechanics  
Dissertation Adviser  
Chair of Committee  

Panagiotis Michaleris  
Professor of Mechanical Engineering  

Ivi Smid  
Associate Professor of Engineering Science and Mechanics  

Sulin Zhang  
Assistant Professor of Engineering Science and Mechanics  

Judith Todd  
Professor, Head of the Department of Engineering Science and Mechanics  

*Signatures are on file in the Graduate School
ABSTRACT

Coatings are vital to the performance of gun tubes, as they protect the structural steel substrate from both the thermal and chemical affects of the hot, erosive, combustion gases. Gun tube coatings are often evaluated by the pulsed-laser heating experiment, though it does not include pressure affects. Live firing tests included all effects, but are expensive and difficult to conduct. Recognizing the short-comings of current evaluation techniques, a method was developed to evaluate interfacial properties by introducing an indentation induced flaw, and studying the flaws evolution under thermal and pressure transients.

The effects of severe thermal- and pressure-transients on coated substrates with indentation-induced, blister defects were then analyzed using experimental and finite element methods. Both explicit and implicit FEA approaches were used to assess the transient thermal- and stress-states and the propensity for fracture related damage and evolution, while undergoing uniform convective heating and pressure transients across the surface. Spherical indentations along with in-situ acoustic emissions, c-scans, and finite element modeling were utilized to induce the defects, and to quantify interfacial adhesion and cohesive zone properties. Preliminary results indicated complex interactions between the boundary conditions and their timing and the resulting propensity for damage birth and evolution. Given the need for robust coatings, the experimental and modeling procedures explored by this study will have important ramifications for coated tube designs and the evaluation of candidate materials.
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CHAPTER 1. INTRODUCTION

1.1 Purpose

During gun firings, the bore of large caliber cannons experiences a severe combination of
temperature and pressure gradients; surface temperature increases approximately 1,500K in less
than 2 ms before cooling down nearly as rapidly, while internal pressures reach over 300 MPa.
In recent history, gun tube thermal damage has increased considerably because of higher
combustion gas temperatures associated with improved performance, according to Underwood et
al. [1]. Thermal loading causes a rapid expansion of the interior of the tube that can result in
yielding, phase changes in the structural steel, and/or the release of beneficial residual stresses
that were applied during an autofrettage process. In addition, hydrogen embrittlement of steel
will occur if combustion gases penetrate the coating and will result in lower fracture toughness.
The thermo-mechanical-chemical loading conditions also contribute to a process known as
erosion, which leads to premature failure of gun tubes. Recognizing the prospect of imminent
gun tube failure after coating and steel separation, gun tube researchers have focused efforts
towards improving the coating bond. Understanding crack driving forces and stress states at the
tip of an interfacial crack, through finite element modeling, analytical analysis, and experimental
work, will provide insight for improving gun tube coating systems.

Cohesive zone models were developed to model progressive crack growth. The model is based
on a traction-separation relationship for which stresses, as a function of interface separation
distance, at the crack tip increase to a maximum value, before decreasing until the surfaces
completely separate. Combined with a coupled thermal-mechanical finite element model of the
coating and steel, the cohesive zone is ideally suited to model the interfacial crack growth for
gun tube coating systems. For mixed mode fracture there are four basic cohesive zone
parameters; Mode I and II cohesive strength, which are the maximum interfacial stresses, and Mode I and II critical fracture energy, which are the integrations of the traction as a function of separation for the respective mode. Several studies have been attempted to measure the critical fracture energy as a function of the contribution of modes I and II, however no standard has been established. In addition, cohesive strengths are extremely difficult to measure, and are more often used as tuning parameters; they are varied in finite element models until experimental and numerical results agree.

The four-point bend test has been chosen to evaluate the properties of the cohesive zone for gun tube coating systems. The four-point bend test can measure the critical fracture energy for samples with various coating and substrate thicknesses, in addition to samples with residual stresses, which are usually associated with thermal and cold spray coatings. Because of the ambiguity in the measurement of the four cohesive zone properties the cohesive zone properties should be verified in an independent experiment. Indentation tests provide an opportunity for property verification by applying the properties measured with the four-point bend test to a model of a spherical indenter penetrating a coated substrate. Property verification will be confirmed by comparing the numerical and experimental results after the initiation of delamination.

Laser-pulsed heating tests have been shown to be an effective technique to simulate the thermal-mechanical loads experienced by gun tubes during firings, according to Underwood et al. [2] and will be studied experimentally and with cohesive zone finite element models. Interfacial crack growth will be studied experimentally by first introducing a blister flaw in coated samples before applying laser heating to the coating surface above the defect. Recognizing that the ball indenter will introduce plastic deformations and cohesive zone damage in addition to the blister, the
indentation experiment will be modeled in the first part of an implicit-explicit simulation before the thermal loads are applied in the second part. The approach was used to more closely approximate experimental conditions, as the entire material state will become the initial conditions for the laser heating explicit simulation. System parameters, such as the coating and substrate material, thermal and pressure transients phase difference, and blister pressure with the goal of understanding defect evolution and improving coating response to thermal transients.

1.2 Research Objective

The primary objective of this research is to develop and verify a model to study the response of refractory coatings, gun tube steel, and their interface to several thermal transients. Substrates will be coated and machined to produce various samples. The samples will be used to evaluate the cohesive zone properties using a combination of experimental, analytical, and numerical methods. Once the properties are measured, the capability of the cohesive zone to model fracture will be verified with the spherical indentation test. Finally, the response of the coating system to thermal boundary conditions will be investigated. The cohesive zone model can be used in an explicit, thermal-structural, coupled model to study the adhesion of the coating and investigate possible mechanisms to reduce crack growth and ultimately extend the gun tube’s life.
CHAPTER 2. BACKGROUND

2.1 Gun Tubes

After World War II the possibilities of different coatings and steel alloys were explored for their applications to gun tubes. Research intensified during the Vietnam War, when gun tubes became an important area of research due to performance and reliability issues with machine guns and the 175-mm gun \([3, 4]\). Army cannons initially became a focused area of research after the unexpected failure of a 175-mm inner diameter (ID) cannon tube in 1966. The fracture toughness of the failed tube was well below the average toughness of tubes from the same time. Davidson et al. addressed the problem by applying an autofrettaged design, by which a residual compressive stress was applied to offset the tensile stresses associated with firing \([5]\). In addition research focused on the use of liners, coatings and additives \([3]\). Current research for gun tubes includes functionally graded ceramics, magnetron sputtered coatings, and linings \([3]\).

Several gun tube variations exist today. The two main categories of artillery tubes are monobloc and jacketed tubes. True monobloc tubes consist of only one piece of material. However, a monobloc tube with a liner is known as a variant. The tube liner generally does not contribute significantly to the strength of the tube and may extend through various lengths of the tube. The second type of tube is the jacketed type of tube, which consists of two concentric tubes \([6]\). Other weapon tubes include small arms, recoilless and expendable tubes \([6]\).

Gun tubes generally consist of several distinct regions. The first region is the breech, through which the round is inserted. The round then rests in the chamber before firing. The bore is the inner surface of the gun tube cylinder through which the projectile travels before it exits through
the muzzle. The bore is often rifled to introduce a torque on the round. The angular momentum of the round improves its range and accuracy.

The inside of a gun tube during firing experiences an unfavorable combination of thermal, mechanical and chemical loading. Propellant gases can reach temperatures in excess of 3000°K. The thermal loading causes a rapid expansion of the interior of the tube that can cause yielding, a phase change in the structural steel, and/or release beneficial residual stresses that were applied during an autofrettage process. The pressure associated with the resisted expansion of the propellant gases is out of phase with the heat flux associated with the combustion gases (it slightly lags the thermal pulse) and factors into the stresses in the gun tube. Also, the combustion of the propellant creates hydrogen, which can lead to hydrogen embrittlement if it can penetrate any coatings present. Hydrogen embrittlement lowers the fracture toughness of the material leading to crack extension and further damage. The thermo-mechanical-chemical loading conditions also contribute to a process known as erosion, which leads to premature failure of gun tubes.

2.1.1 Coatings

Coatings are designed to not react with propellant gases and insulate the gun steel from the thermal flux caused by the combustion of the propellant. Coatings have been designed with the main purpose to protect the structural steel of the gun barrel. One of the many purposes served by coatings is resisting erosion and protecting the steel from erosion. Recent research has been concentrated on understanding the mechanisms of coating failure, assessment of known coatings and coating application techniques [7].
Chromium is a common gun tube coating; it was developed over 60 years ago and is the most common coating used on battle guns. Low contractile (LC) chromium coatings are tougher and stronger, but weaker according to Cote [8]. Other possible experimental coating techniques include physical vapor deposition, chemical vapor deposition or metal-organic chemical vapor deposition (MOCVD) [7]. Sopok compared the erosion of several different coatings which included chromium, tantalum, molybdenum, rhenium and niobium together with [9]. Sopok’s results showed that all the materials have significantly different erosion performance. Rhenium and niobium had the lowest erosivity threshold, as defined by the threshold surface temperature at the start of erosion, while chromium and tantalum had the highest. Sopok also demonstrated that the chromium performance varied little in different combustion environments.

Coatings contain cracks of varying size from the time of their manufacturing. The pressure on the gun tube inner wall and thermal heat transfer of the combustion gases that occur during the repeated firings cause the cracks to grow, and eventually cracks may reach the substrate structural steel. Once the coating cracks reach the substrate, the hot and chemically erosive combustion gases can reach into the depths of the cracks where they can react with the steel. During subsequent firings cracks can form networks that lead to the so-called mud-flap cracks in the coatings. The array of cracks can result in the detachment of the coating from the steel. The dimensions of these islands are on the same order as the coating thickness according to Johnston [7].

Underwood et al. investigated hydrogen cracking in compressively yielded fired cannons [10]. They were able to develop a threshold stress intensity factor that would govern the crack spacing of longitudinal and circumferential cracks (the circumferential cracks extend due to hoop or
circumferential stress, while the longitudinal cracks extend from the axial stress). The circumferential cracks had a larger spacing than expected, and were actually closer to longitudinal crack spacing as predicted by the threshold stress intensity factor. The researchers predicted that this was a result of the compressive residual stress from the autofrettage stress field. Based on their analysis, the researchers concluded that hydrogen embrittlement had occurred in the tube.

2.2 Erosion

One of the most thoroughly researched aspects of gun tube design is erosion. Erosion is a cause for concern because of high tube replacement costs and the reduced effectiveness of gun barrels after the onset of erosion. Permissible erosion in tank guns that require significant accuracy is about 0.5-1% and between 5 and 8% can be permitted for indirect firing weapons according to Lawton [11]. Erosion damage is generally most serious at the origin of rifling [11]. The temperature is highest here and even though the tube is only at this temperature for a short period of time, it allows the harmful combustion compounds to diffuse into the steel creating a weak brittle zone which can be susceptible to removal by the high velocity of the combustion gases [11]. Erosion mechanisms are categorized as chemical, thermal, and mechanical. Erosion can be experimentally studied several ways, such as canon firing has been simulated by laser pulse heating [12], ion beams [13] or by analyzing fired guns [8, 11, 14-17]

2.2.1 Thermo-Chemical Erosion

Johnston noted that the combustion of the propellant produces a mixture of gases, which includes carbon monoxide, carbon dioxide, hydrogen, water vapor, and nitrogen [7]. The combustion
products can react with coating or the substrate. To make matters worse, the products can penetrate into the cracks beneath the coatings when the cracks are very small according to Cote and Rickard [8].

All of the mentioned gases contribute to erosion according to a correlation developed by Lawton for the wear per round, which is given in Equation (2.1) [11].

\[ w = A t_0 \left( \frac{T_i}{T_0} \right) \exp \left( -\frac{\Delta E}{R_0 T_{\text{max}}} \right) \]  

(2.1)

where \( t_0 \) is the time-constant that has been determined by Lawton [18], \( T_{\text{max}} \) is the maximum bore temperature, \( T_i \) is the initial surface temperature, \( T_0 \) is 300K, \( \Delta E \) is the activation energy, \( R_0 \) is the universal gas constant and \( A \) is an expression based on multiple linear regression of experimental data and is a function of the contribution of each of the combustion gases. The definition is \( A \) is given in Equation (2.2).

\[ A = 114 \exp \left[ 0.0207 \left( f_{\text{CO}} - 3.3 f_{\text{CO}} - 2.4 f_{\text{H}_2} - 3.6 f_{\text{H}_2O} - 0.5 f_{\text{N}_2} \right) \right] \]  

(2.2)

The percent volume of the respective constituent compounds is represented as \( f \). As suggested by Equation (2.2) hydrogen is the most erosive, as expected because its small molecular size allows it to easily diffuse into the steel. Nitrogen is widely viewed as having a protective effect.
Researchers also believe carburization and oxidation contribute to erosion. The carburization process occurs when monatomic carbon diffuses into the steel and precipitates during cooling as iron carbide, which lowers the melting point and increases brittleness according to Sopok et al. [19]. Oxidation occurs when oxygen from carbon dioxide reacts with the steel to form iron oxide.

Hydrogen erosion and hydrogen embrittlement are the largest chemical contributors to erosion, according to many researchers. As mentioned before, Lawton identified hydrogen as the most erosive of all the combustion compounds explaining that it diffuses into the barrel, reacts with the carbon in the steel and decarburizes the steel, thereby promoting erosion by softening [11]. Higher strength steels are especially susceptible to environmental hydrogen-induced cracking. Vigilante et al. noticed a 20% increase in yield strength from 1145 to 1380 MPa resulted in a 100 times higher crack growth rate [20]. Underwood et al. analyzed the effects of hydrogen cracking at locations of tensile residual stress [21]. Machined samples were fatigue tested in a hydrogen rich environment. Analysis of the fractured samples, along with finite element simulations, suggested that stresses account for the crack initiation location and crack growth direction.

### 2.2.2 Thermal Erosion

The intense heat generated during the combustion of the propellants creates surface temperatures of more than 1,500K. The hot gases can melt the surface of the tube, which would then be easily removed by the projectile. Heat checking also contributes to erosion. During the heating the steel experiences a phase change to austenite, which can then form brittle martensite [7]. The martensite can easily crack, which makes the tube more susceptible to chemical diffusion and mechanical removal.
2.2.3 Mechanical Erosion

The shear stress caused by sliding friction is sufficient to cause material removal when the inside of the gun tube is already cracked or embrittled. Mechanical erosion is especially dominant in low temperatures when the temperatures are too low for thermal erosion and too low to assist in the diffusion of chemical compounds into the steel. Different types of erosion include abrasion, sweeping and washing from the combustion gases.

Another form of erosion occurs when the combustion gases leak past the projectile. The leaking can create jetting, which contributes to erosion. The blow-by effect was found to increase local erosion by between 200-300% [22].

2.3 Erosion Mitigation

Several different techniques have been researched to mitigate the effects of erosion. Work on the effects of propellant additives has been limited since the review done by Barcuti of additive research, up to the point of the review [23]. Titanium dioxide is one common additive. Many researchers believe it works by reducing heat transfer to the barrel, thereby reducing erosion [7].

2.4 Predicting Erosion
Researchers have been concentrating recent efforts at predicting and modeling erosion in order to more accurately evaluate gun tube life and optimum replacement intervals. Modeling also allows for the addition and removal of variables. Variables can also be optimized to limit erosion.

Sopok et al. developed the computer code for the first model able to predict thermochemical erosion [24]. The model had the ability to include the effects of wall degradations from steel phase transformations, chemical reactions, and cracking. The five different analyses of the code provided profiles for thermo-chemical ablation, conduction, and erosion for each material as a function of time, travel, and round. Later, Sopok et al. developed an updated code [19]. The unified computer does not require significant validation, but simulations require substantial user intervention to integrate the different inputs and outputs of the analyses.

Cote et al. also utilized two finite element models to understand the onset of coating-substrate separation, and plastic deformation and stresses associated with the island effect [25]. The first model was of an island crack. The model included the effects of plastic deformation and demonstrated the cupping in the crack. The lifting of the edges of islands suggests the mechanism by which the islands separate from the substrate leaving the steel exposed. The second model was designed to simulate the shear and peeling stress between the coating and the steel. The stresses were in excess of the yield strengths. It was also observed that if the crack extended through the interface and into the steel, instead of stopping at the interface, it was beneficial in terms of preventing peeling and coating separation. The thermal stresses of the firing were found to dominate, while the compressive thermal stresses from cooling were found to be more likely to cause spallation than those created during heating.
2.5 Experimentally Analyzing Erosion

The erosion mechanisms are often most easily understood by experimentally analyzing fired gun tubes. Visual observations in addition to several different levels of microscopy can provide insight into the mechanisms, causes, and locations of erosion damage. The understanding gained experimentally can often provide insight in ways to prevent erosion and extend the gun tube’s useful life.

Turley analyzed a chromium plated tank gun tube after erosive wear was seen at the origin of rifling [15]. The tube was analyzed visually, with a scanning electronic microscope (SEM), optical microscopy, and by x-ray scans. The gun tube coating experienced spalling in small areas, which were bounded by craze cracks. It was also observed that in some cases copper from the round penetrated into the cracks as it was forced through the tube. The mismatched coefficient of thermal expansion of the copper and chromium forced the crack to open during firing, expediting the crack growth. A more thorough study of chromium loss was done by Cote et al. [25]. The goal of the study was to establish coating loss factors. The gun tube fired sufficient rounds of two different types to obtain the erosion before being fatigue tested and analyzed using optical microscopy, confocal microscopy, electron microprobe analysis, scanning electron microscope (SEM), and Energy-dispersive X-ray spectroscopy (EDAX). Cote et al. noticed high temperature corrosion on the surface, leading to chromium loss. Like Turley, it was noticed that as soon as the steel was exposed to the combustion gases it was rapidly corroded and pitted.
The erosion process was well characterized by Underwood et al. [17]. It was observed that the process occurred in three steps. In the first, the cracks were permanently opened from tensile stresses. The coatings are softened, allowing the steel to be transformed. Finally, islands are formed that eventually crack off the surface. Underwood also noted that the most critical axial location in the 120 mm gun barrel for erosion is 0.6 m forward of the breech [17]. After 118 firings, the gun tube at the location had transformation and cracking in both the chromium coating and the steel.

2.5.1 Laser Pulsed Heating

Actual gun barrel firing for evaluating coatings and gun steel and characterizing damage is an expensive and cumbersome process. Researchers prefer to evaluate coatings damaged through a laser pulse heating process designed to simulate firings. However, the laser heating can only simulate the thermal effects of the firing, and not the effects of the combustion gases and the mechanical effects caused by a projectile. Underwood et al. used a Nd:YAG laser to apply a single pulse to the surface of a sample [26]. Thermal damage cracks from laser heating were observed in ZrO₂, Al₂O₃, SiAlON, Si₃N₄, and SiC. The cracking mechanism believed to be behind the damage was thermal expansion during heating that lead to compressive deformation and tensile residual deformation that lead to cracking after cooling. All of the samples experienced significant cracking, while the Al₂O₃ sample underwent fragmentation in cracked areas, the ZrO₂ and Si₃N₄ samples showed normal cracks forming and opening followed by parallel cracks, indicating that the normal cracks occurred first. The SiAlON sample experienced a significant amount of material loss.
Underwood et al. also conducted experiments on laser pulse heated tantalum and chromium coated steel samples [1]. The samples cracked in the coating, but not in the steel substrate. This was expected because hydrogen embrittlement could not occur due to the only trace amounts of hydrogen present during the laser pulse heating. The addition of the hydrogen from the combustion gases present during the actual firing is perhaps responsible for the crack penetration and extension into the steel. Underwood et al. also observed recrystallization that occurred in the steel at a depth of two-thirds of the coating thickness.

Warrender et al. were able to closely replicate the heat transfer equivalent to a firing using a variable pulse duration laser, as opposed to the fixed pulse duration laser used in most other studies [2]. The surface temperature experienced during firing and the pulsed laser simulations were very similar. The samples, also tantalum or chromium coated steel, showed failure consistent with firing results. The firing resulted in circumferential cracks in the tantalum, the spacing of which was a function of the pulse duration and fluence of the laser beam. Increasing the pulse duration of the laser appeared to increase the crack spacing and the fracture toughness of the sample, suggesting it might be an effective coating pre-treatment.

2.6 Thermal Stresses in Gun Tubes

Thermo-mechanical modeling of the firing process is important for understanding the failure mechanisms in gun tubes given the complex interactions between the severe pressure and thermal conditions; the resulting stresses in-turn drive the formation and propagation of cracks that ultimately cause a variety of failures (fatigue, debonds, erosion etc.).
Generally, there are two main types of cracks often observed in gun tubes after the application of a thermal/pressure pulse as seen during firing. According to the leading authority on gun tube failure mechanisms (Underwood et al. [27]), circumferential-radial cracks tend to grow because of the dominant hoop stresses. While longitudinal-radial stresses also tend to occur in practice due to a number of factors including curvature effects, an analysis of SEM images led to the conclusion that one reason for these cracks was a thermal expansion-residual and stress-environmental cracking scenario. In this scenario, the residual stresses in the coating caused hydrogen induced cracking normal to the axial stress.

Using this information, Underwood and co-workers [1, 14, 17, 26-30] developed a thermo-mechanical model which used the finite difference method to first evaluate the transient temperature distributions as a function of the depth for a coated steel plate; the temperatures calculations were done using measurements from bore locations where the thermal heat transfer was the most severe. For the model, linearly temperature dependent material properties were used to account for the wide temperature range that the coating and steel substrate experienced. The pulse duration and magnitude, coating thickness, and convection coefficient were all variables input into the model.

For the subsequent analysis, the transient, in-plane, and bi-axial thermal stress was approximated using Equation (2.3) as shown below.

$$\sigma_T = -\frac{E\alpha(T(x,t) - T_i)}{1 - \nu}$$  \hspace{1cm} (2.3)
where $E$ is the elastic modulus, $\alpha$ is the coefficient of thermal expansion, $\nu$ is the Poisson’s ratio, $T(x,t)$ is the temperature as a function of depth, $x$, and time, $t$, and $T_i$ is the initial, uniform temperature.

Residual stress calculations were then made by taking the negative of the sum of the stress calculated above and the yield strength as shown by Equation (2.4).

$$\sigma_R = -\sigma_T - \sigma_Y$$  \hspace{1cm} (2.4)

In Equation (2.4) $\sigma_Y$ is the yield strength. In the most recent application of the model, a modification was applied which included the temperature dependence of the yield strength.

Under these assumptions, the temperature distribution was then verified by analyzing the microstructure of the steel in order to determine the depth of the steel transformation that occurs at 1020 K; the finite-difference calculations agreed with the temperature at the transformation depth.

After comparing the temperature distributions in the steel substrate when chromium, tantalum, and molybdenum coatings were used, it was observed that the molybdenum transferred the most heat and thus, had the highest temperatures in the substrate. However, it had approximately the same surface temperature as the chromium coating. In addition, the temperature distribution of tantalum was between the chromium and molybdenum, with the model predicting the highest
surface temperatures. Predicted stress magnitudes in the coating increased consistently from chromium, tantalum, to molybdenum.

Increasing the coating thickness also enlarged the tensile residual stresses in the coating, but had varying effects on the temperature distributions. Interestingly, the surface temperature was lower for the thicker coating, as well as at the interface. However, the overall temperature was higher as a function of the distance from the surface for the thicker coatings. Increasing the heating pulse duration had the predictable effect of amplifying the temperature in both the coating and substrate, while also moving the tensile residual-stresses further from the surface.

The temperature used for the shear-stress evaluations was the difference between the temperatures determined at locations 0.5 and 1.5 times the crack depth; this difference reflects the temperature that governs the shear failure and removal of the coating segment. Recognizing that when a crack opens, transient compressive stresses are released, a force balance was developed and rearranged to provide an expression for the shear stress at the interface.

\[
\tau = \left[ E_z \alpha_z \left( T_{z/2} - T_{3z/2} \right) \right] \frac{z}{y*}
\]  

(2.5)

In Equation (2.5), \( \tau \) is the interface shear stress, \( E_z \) and \( \alpha_z \) are the elastic modulus and coefficient of thermal expansion at the depth of the crack, \( T_{z/2} \) is the temperature at a depth of half of the crack length, \( T_{3z/2} \) is the temperature at a depth of one and a half crack lengths, \( z \) is the crack length and \( y* \) is the length of the segment from the crack, parallel to the surface. The term inside the brackets is an estimate of the transient residual stress. Results indicated that the maximum
shear stresses in the chromium coating agreed well with the observed shear-strength for that material as determined by micro-hardness measurements. In another important study, Petitpas and Campion [31] predicted the fatigue life of uncoated gun barrels using finite elements. The authors discovered that the pressure pulse governed the growth of cracks over a four millimeter range and that the fatigue life was increased by a factor of 10 after an autofrettage.

Crack growth at the interface leads to coating delamination, which exposes the underlying substrate to erosion. According to Newaz et al. [32], thermal stresses cause compressive yielding and residual tensile stress and cracking during cooling. As such, graded coatings where the composition varies functionally from the interface to the surface (Bao and Cai [33, 34]) are often used. Despite these efforts, many coatings typically fail when cracks parallel to the surface form at the interface, eventually leading to delamination in various forms (Zhu et al. [35] and Hutchinson and Evans [36]).

2.7 The Bauschinger Effect and Autofrettage

The Bauschinger effect is often observed in gun steels. Effective numerical modeling of the effect requires accurately measuring experimental stress-strain results. The Bauschinger effect is characterized by several principal features, shown in Figure 2.1.

The material is first loaded linearly until the yield strength, at which point plastic nonlinear stress-strain behavior can be observed (from points 1 to 2 to 3). The loading is then reversed to compressive stress, which has a reduced elastic modulus and after passing the yield point, nonlinear compressive stress-strain behavior (from points 3 to 4 to 5). The material is then
loaded in tension, returning to the maximum stress (from pints 5 to 6 to 3). During autofrettage the gun is over pressured in tension and the repeated loading is also seen in the Bauschinger effect results. The result of the Bauschinger effect in the autofrettage process is that the desirable compressive residual stress is lost because of prior plastic tensile straining.

![Figure 2.1: Illustration of Bauschinger Effect.](image)

The effects of the Bauschinger effect were analyzed on A723 steel, a type of steel commonly used in gun tubes, as well as PH 13-8Mo and HY180 by Troiano et al. [37]. The unloading modulus was observed to decrease after plastic pre-straining. The strength was reduced between one and two percent of the total strain. The stress-strain results from Troiano et al. were applied by Parker et al. to ascertain the effects of the compressive residual stresses that resulted from the autofrettage process on pressure vessels [38]. The work done by Parker et al. included the development of criteria for overstrains. The residual compressive hoop stress at the inner radius should reach a maximum value at the percentage overstrain level below which reversed yielding does not occur.
In another study by Troiano et al., several gun tube sections were overstrained before slit tests were conducted [39]. A finite element model was developed to simulate the process and was found to agree well with analytical techniques for the prediction of residual stress in thick-walled autofrettaged cylinders. An important result was that it appeared that the Bauschinger effect was minimized by performing a post-autofrettage thermal treatment, which consisted of loading to 1.2% strain in tension, 0.25% strain in compression, the removal of loading and the application of heat at 360°C for one hour. Parker also did research on efforts to minimize the Bauschinger effect [40]. The fatigue-based life of the tube can be extended by between a factor of 2 and 30 using the process that consisted of an initial autofrettage, one or more heat soak and autofrettage sequences and a final heat soak, which was deemed optional.

Geometric changes in cylinders complicate the autofrettage process. Possible disruptions in axisymmetry in gun tubes include erosion groves, rifling or cross-bore holes sometimes used for cooling. Parker et al. explored the effects of cross bore holes in gun tubes in terms of stress concentrations, stress intensities and fatigue life in autofrettaged tubes [41]. For cracks originating from the holes, the lifetime is only 60% of the lifetime if the crack originates from the bore.

2.8 Cold Spray Coating Deposition

Cold spray is a coating process where small particles, typically 1 to 50 μm, are directed towards a target surface. A supersonic gas jet carries the unmelted particles at velocities greater than 500 m/s. The cold spray process is characterized by excessive deformations in both the target surface and the particles. The concept of cold spray was derived as an offshoot of supersonic wind tunnel
testing by Alkhimov et al. [42] and Tokarev [43]. In order to cold spray a material, it must first be available in powder form, which is fluidized after mixing with high-pressure preheated gas. The gas and powder are then fed into the nozzle, accelerated to supersonic speeds, and then cooled to room temperature and in the divergent section of the nozzle. Several variations of the original design have been researched, in addition to the effects of various characteristic dimensions. Further details of the cold spray procedure have been described by Stoltenhoff et al. [44]. The exact mechanisms that bond the particles to the target surface is not well understood. The prevailing theory is that the plastic deformation removes any surface films that might prevent bonding, such as oxides, and allows for intimate conformal contact at high uniform contact pressure according to Dykhuizen et al. [45].

Current studies of cold spray focus on optimizing the nozzle dimensions and designs, research on the interfacial bonding mechanisms, and material combinations according to Gärtner et al. [46]. Finite element simulations of particles impacts at the target surface is one of the most common approaches to understanding the bonding mechanisms; Assadi et al. [47] and Li et al. [48, 49] have published research on the topic. Current finite element models are thermomechanical and utilize advanced material models, such as Johnson-Cook. Given the short duration of particle impact events, the problems are well-suited to be solved with explicit finite element codes. The particles also undergo excessive deformation during impacts, causing significant distortion in the original shape and finite element mesh. In order to overcome the excessive deformation of the elements in the mesh, the Arbitrary Lagrangian-Eulerian method is sometimes used, where the finite element mesh is not attached to the material. The computational mesh can move within the boundaries to optimize the individual element shape. Alternatively, remeshing is often used to optimize the mesh during sub-steps of the solution at points in time when the deformation in individual elements becomes excessive.
The velocity of the particle at impact is one of the key parameters during cold spray coating deposition. A critical velocity, below which particles fail to adhere to the target surface, is generally between 300 and 1200 m/s and is a function of a wide range of parameters. However, excessive particle velocity can lead to erosion on the target surface and particle shattering upon impact according to Bala et al. [50]. Further experimental and theoretical details about the critical velocity of the particles during cold spray can be found elsewhere, such as the work of Li et al. [48]. The carrying gas, which is usually nitrogen, helium, or air, is pressurized to typically around 1.7 MPa according to Bala et al. [50]. The gas is preheated before the addition of the power to prevent particle softening and to increase the speed of the gas as it expands in the nozzle. The speed of the powder is a strong function of the gas preheating temperature. The critical velocity of successful deposition is determined by correlating the deposition efficiency with the particle size distribution. Deposition efficiency is a function of the nozzle geometry as well as other process parameters, such as gas temperature and pressure according to Gärtner et al. [46].

Cold spray coating technology offers significant benefits compared to thermal spray techniques and other coating deposition methods. Lima et al. [51] suggested using cold spraying to apply nanostructured coatings. Nanostructured coatings are desirable due to their unique microstructure. When such coatings are thermally sprayed, any partial melting will cause the particles to revert to the regular microstructure after resolidification. Avoiding bulk melting and composition and phase changes is often also desirable for standard coatings as well. The elimination of solidification stresses also allows for the deposition of thicker coatings than are achievable with other methods. For chromium coating applications, the cold spray technique is preferable to electroplating as it does not require the use of hexavalent chromium, a known
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carcinogen. The cold spray technique is also often used because it can deposit coatings with few undesirable inclusion-type defects and very little oxidation according to Bala et al. [50].

2.9 Coating Delamination

Gun tube coating delaminating exposes the underlying gun tube to the erosion mechanisms, which were discussed earlier, and is expedited by the thermal gradients experienced during firing. Most thermal barrier coatings are ceramics generally applied to turbine blades, which experience much different transient thermal loading [32]. Thermal expansion stresses cause compressive yielding and residual tensile stress leading to cracking during cooling [30].

Oftentimes thermal barrier coatings are graded, the composition of which varies linearly from the coating-substrate interface to the surface. Ceramics are commonly used as coatings to protect steels from melting, wear, corrosion and oxidation. Failure of thermal barrier coatings is usually a result of the coefficient of thermal expansion mismatch between the coating and the substrate. The mismatch problem is often dealt with through the use of functionally graded coatings (FGC) [33, 34] Thermal barrier coatings in the presence of nonlinear thermal gradients generally fail by cracks forming parallel to the surface, causing the delamination of the coating. In the regions that separate from the surface, a portion of the coating is often left on the substrate, as observed by Zhu et al. [35].

Hutchinson and Evans analyzed two examples of delamination associated cracks [36]. The types analyzed were an isolated crack or edge delamination parallel to the interface and a shrinkage crack caused by sintering of the top layer of the coating. Hutchinson et al. were able to
develop the relationships for the energy release rates for the two types of cracks. Analyzing their relationships for the energy release rates, they determined that increasing the coating modulus or the difference between the deposition and surface temperatures increases the likelihood of delamination.

The buckling and energy release rates and stress intensity factors associated with coating delamination have also been addressed by Bao et al. analytically [33]. Evans and Hutchinson further explored the mechanics of coating delamination in thermal gradients for oxide coatings experiencing cooling in a later paper [52].

Newaz et al. analyzed the delamination of the crack mainly from buckling [32]. The mechanism that promotes the coating buckling is not well understood. Some researchers believe that the crack initiates from planar compressive stresses due to the rapid cooling, others believe it is because of the coalescence of cracks or a through thickness shear crack. El-Borgi et al. also studied the delamination of thermal coatings by buckling after the appearance of a crack [34]. Equations were developed for the displacement, buckling load and stress intensity factors.

Conroy et al. [53] briefly analyzed the problem of interfacial delamination for interfacial flaws. The authors cited the interruption of heat conduction across the interface for a crack that opens during the thermal cycling, though conduction at the edges takes place. With only inefficient convection through gas conduction across the interface, the temperature difference between the peak coating and substrate temperature increases dramatically. As the crack region increases, so does the temperature difference. The authors speculated that the crack growth rate grows as the service life increases. In addition the gases in the crack will become pressurized as the
temperature of the coating increases, further driving crack growth. The mechanism discussed led the authors to hypothesize three methods for coating removal: complete separation from the substrate, gas choking during blowdown, whereby the crack retains high pressure gas, and an oxide forms at the interface pushing the coating upwards and into the path of the projectile.

2.10 Cohesive Zone Elements

Researchers have used cohesive zone elements to model the separation or delamination of at an interface. Cohesive zone modeling (CZM) is one of the most common methods of modeling separation. Other methods include the Virtual Crack Closure Technique (VCCT) used by Xie et al. [54], crack growth simulation by advancing the crack front when the local energy release rate reaches a critical value as shown by Alam and Wahab [55], and Shi and Zhang [56], material models that degenerate from Continuum Damage Mechanics studied by Chaboche et al. [57], a mixed-mode embedded-process-zone (EPZ) shown by Yang and Thouless [58], and several other finite element interface models [55, 59, 60].

Cohesive zone models have been used to simulate fracture in a variety of materials including polymers, metals, ceramics, bi-material systems, metal matrix composites, and fiber-reinforced plastic composites. The concept of CZM was first proposed by Barrenblatt [61] and Dugdale [62]. Barrenblatt first developed the idea of CZM as a traction-separation law for decohesion. Dugdale modeled the crack tip analytically and observed that the normal stress at a crack tip is limited by the yield strength of elastic-perfectly-plastic materials. According to the traction separation law, the traction across the interface first increases as a function of separation to a maximum value before decreasing and eventually decreasing to zero.
Shet and Chandra [63] analyzed the process of energy flow during fracture. The energy first flows into the fracture process zone in front of and behind the start of the cohesive zone. The portion of energy in front advances the crack tip, while the part behind works to continue the separation, the distribution of which depends on the shape of the stress-displacement model.

Turon et al. [64] developed important guidelines for parameters used in cohesive zone analyses. According to Turon et al. two conditions must be met for successful implementation of CZM: the cohesive contribution to the pre-crack growth compliance must be small enough to not introduce an artificial compliance and the element size must be less than the cohesive zone length. The value suggested for the interface stiffness is given in Equation (2.6).

\[
K = \frac{\alpha E_3}{t} \tag{2.6}
\]

where \(E_3\) is the transverse elastic modulus, \(t\) is the thickness of the coating or substrate and \(\alpha\) is a parameter with a value much greater than unity (\(\alpha \gg 1\)), the author recommended a number greater than 50 and also provided a comparison of load-displacement curves for different stiffness values compared with experimental results for dual-cantilevered beam tests. Turon et al. also provided an equation for the length of the cohesive zone.

\[
l_{cz} = ME \frac{G_c}{\left( \varepsilon^0 \right)^2} \tag{2.7}
\]
In Equation (2.7) $E$ is the elastic modulus of the material, $G_c$ is the critical energy release rate, $\tau^0$ is the maximum interfacial strength, and $M$ is a parameter that is based on model choice and is between 0.21 and 1.0.

Xu and Needleman [65-67] did extensive work early in the development of cohesive zone models. Their models were two-dimensional, modeling the cohesive zone as initially elastic, leading to exponentially decaying strength. Ruiz et al. [68] expanded the model to three dimensions. The three-dimensional model contained the possibility for crack extension along the interface of every element face. The model was used to simulate the dynamic collision of a ball with a plate. Zavattieri and Espinosa [69] and Wei et al. [70] analyzed the effect of microstructure on the dynamic failure process. Wei et al. did their work in the context of thermal barrier coatings, undergoing thermal shock. The microstructure sections were scanned and converted to finite element meshes, converting each pixel to a finite element. The model was verified by comparing the predicted crack patterns to the actual micrograph obtained experimentally. Zavattieri and Espinosa [69] did similar work, meshing the microstructure and applying the cohesive zone model to the grain boundaries, where separation can occur. Zhou et al. [71] analyzed the three-dimensional, stochastic fracture of ceramics under a dynamic tensile load. The approach took into account the probabilistic nature of ceramic failure and applied the cohesive elements at random element interfaces according to the stochastic distribution of the flaws. Alam and Wahab [55] focused on the problem of the separation of two materials by applying cohesive zone elements along the interface of dissimilar materials to analyze to model fatigue crack growth.

Li et al. [72], Xie and Waas [73], and Goyal et al. [74] all independently developed and verified cohesive zone models for the Mode I separation of dual cantilever beams (DCB) test specimens.
Xie and Waas verified their results by comparing with VCCT and analytical solutions, while Goyal et al. compared their numerical results to analytical solutions and those obtained using linear-elastic fracture mechanics (LEFM). The simulations were done using ABAQUS finite element software and cohesive zone elements; linear and quadratic elements were studied. Xie and Waas’ work is also significant because they developed a new computational approach that drastically reduced computational time, compared to other previously developed cohesive zone modeling solution procedures. Li et al. analyzed several different specimens, including three-point bend specimens and single-lap-shear specimens. The results were verified experimentally, noting that the numerical results predicted both the strength of the joints and the failure mechanism.

Cohesive zone elements have been applied to a wide range of different problems. Mirzaei et al. [75] applied cohesive zone elements to the dynamic problem of the explosion of a gas cylinder. The problem was unique in the application of the cohesive zone elements, while capturing specific features of the crack growth, such as cyclic crack growth, cyclic bulging of the crack flaps, crack curving and branching, and fast non-incremental growth. Mirazaie et al. simulated the explosion of a gas cylinder and verified the results based on observations of the exploded cylinder. Rahul-Kumar modeled large, inelastic deformations in three different models: a peel test, a compressive shear test, and interfacial failure in a multilayer elasto-plastic polymer system [76]. The peeling test demonstrated the ability of the cohesive element to model interfacial fracture by allowing for crack growth and illustrated the interaction between fracture energy and macroscopic energy loss. The shear test illustrated the ability of cohesive elements to analyze large inelastic deformations. Finally, the problem of cylinder indentation on a layered system was studied, demonstrating the ability of cohesive elements to model the crack nucleation and extension process.
Siegmund and Brocks simulated the ductile fracture of metals using a cohesive zone model by adapting it to the mechanical behavior of voided cells [77]. The cohesive zone parameters are triaxially dependent, which is achieved by a coupling of the cohesive elements to the adjacent solid elements. The authors developed a global toughness measure that included the contributions of global plasticity and local separation. The crack growth resistance allows for local and global contributions of crack growth resistance, the effect of the specimen size and geometry and the local stress triaxiality influences local quantities.

Xie et al. compared two different approaches: a progressive failure analysis (PFA) with complete and partial bond void representation and the virtual crack closure technique with complete and partial bonding [78]. Continuous and discrete cohesive zone models were compared and determined to be equivalent, though discrete elements are more convenient for reasons of convergence and gap dimension restrictions. By the continuum cohesive zone model (CCZM), the region ahead of the process zone ahead of the crack tip is treated as a continuous compliant layer, whereas in the discrete cohesive zone model (DCZM), elements are treated as point wise. If proper contact surface information can be evaluated, PFA and VCCT can both be used to simulate debonding. In a separate work, Xie et al. used a Fernlund-Spelt (FS) test structure to evaluate coupon level mixed mode fracture tests and use the bond properties to model full size hat stiffened adhesively bonded structures using the VCCT.

Secchi et al. verified a thermal mechanical interface crack numerical model experimentally [79]. A crack was modeled without a pre-determined crack path. The unknown nature of crack growth direction was accommodated by successive remeshing of the geometry based on fracture evolution using a posteriori refinement technique. Fagerstrom and Larsson also developed a cohesive zone
formulation for thermo-mechanical simulations [80]. The thermo-mechanically coupled fracture interface was developed in the form of a thermo-hyperelastic-plastic continuum with non-linear isotropic plastic hardening, which is representative of steel and other common metals.

Ferney et al. studied the oscillatory crack growth in a rectangular glass sample that was heated, then dipped into water [81]. As the glass samples were dipped into the cooling water, an oscillatory crack grew from the prescribed initiation location. Ferney et al. allowed crack growth along any element edge, allowing the crack to follow the global path seen experimentally. The global crack growth pattern was similar to what was experimentally observed.

Arata et al. analyzed the crack growth in a compact tension specimen [82]. A cohesive zone model was compared with results from LEFM. For the cohesive model, the properties were uniform along each interface. The best-fit value of the initial flaw length scales with the cohesive surface characteristic length.

Further applications of the cohesive zone elements were done by Chen et al. [83] and Camanho et al. [84] in the area of delamination simulation in composites. Camanho et al. simulated mixed-mode progressive delamination. The geometries studied were multilayered composites, tested in the three-point bend configuration and the peeling configuration. The simulations of which were found to agree well with experimental results. The samples were then tested and compared with numerical results using a mixed-mode bending (MMB) test fixture. The comparison of the results illustrated the inability of the cohesive zone elements to model all of the characteristics of composite laminate failure, such as fiber bridging during the unidirectional dual-cantilever beam (DCB) specimen failure. Chen et al. compared two FEA packages, ABAQUS and LUSAS, the
former with a user-defined subroutine for the cohesive zone model. Both numerical approaches agreed well with experimental and analytical results for dual-cantilever beam and end-loading split tests and simulations.

Recently, researchers have been studying the problem of interface failure of coated solids under contact loading [85-87]. Xia et al. characterized the bilinear cohesive law by its strength in normal and shear and fracture toughness, which was assumed to be the same for all fracture modes [85]. The authors noted both shear and normal cracks and used the model to investigate the effects of different parameters such as coating thickness and material properties on the force and depth needed to separate the coating from the substrate. Abdul-Baqi and Van der Giessen similarly investigated the delamination of a hard film on a soft substrate during indentation [86, 87]. The authors applied an exponential form of the cohesive law and analyzed the nondimensional effects of geometry, material, interface and loading parameters on delamination. An important conclusion of the work of Abdul-Baqi and Van der Giessen was their analysis of the indentation test to estimate the work of interfacial debonding from the maximum indentation depth and the final delamination radius [86]. The test method to determine the work of interfacial debonding was done by Hainsworth et al [88]. Based on fracture mechanics, the authors were able to develop an equation for the critical fracture energy in Equation (2.8).

\[
G_c \approx \frac{2Et^3 (\Delta \delta)^2}{3r^4}
\]  

In Equation (2.8) \( E \) is the elastic modulus of the coating, \( t \) is the coating thickness \( \Delta \delta \) is \((\delta_{max} - \delta_{res})\) which can be determined from the nanoindentation curve, and \( r \) is the radius of the coating debonding. The authors noted the importance of having a better method to more accurately
measure the debonding radius. According to Abdul-Baqi and Van der Giessen, the method tended to overestimate the interfacial strength and energy of separation.

2.11 Finite Element Modelling of Progressive Crack Growth

Common methods of evaluating fatigue and progressive crack growth include linear elastic fracture mechanics (LEFM), similar methods with retardation to model plasticity, crack tip opening displacement (CTOD) and crack tip opening angle (CTOA). The finite element analysis of crack growth generally can be accomplished with a series of simulations. The procedure generally involves producing cracked specimen geometry, sometimes with separate CAD software. The cracked specimen must then be meshed, generally with collapsed stress concentration elements. Relevant parameters are then evaluated and used to predict the direction and magnitude of the crack growth. The geometry is updated and the number of cycles to produce the crack growth step size is calculated before the model is re-meshed. The outlined procedure has been applied and verified for a variety of different situations and for several different crack growth theories.

Barsoum and Barsoum applied a similar method to the one described to investigate the fatigue life of welded structures using LEFM while incorporating residual stresses from the welding process [89]. The effects of the residual stresses were included by a process of isoparametric stress mapping between the meshes with and without the fatigue crack. The crack growth angle was predicted using the maximum circumferential stress at the crack tip, according to the $\Delta\sigma_{0\text{max}}$ theory. The crack deflection angle, $\phi$, between the path of the crack growth and crack front under Mode I and II loading is given in Equation (2.9):
\[ \varphi = 2 \arctan \left( \frac{K_I}{4K_{II}} \pm \frac{1}{4} \sqrt{\left( \frac{K_I}{K_{II}} \right)^2 + 8} \right) \]  

(2.9)

where \( K_I \) and \( K_{II} \) are the Modes I and II stress intensity factors.

The residual stress was incorporated by superposing the applied and residual stress intensity factors. The residual stress intensity factor is solved by using a weight function solution. For best results the residual stress intensity factor should be calculated at each stage of the crack growth. The authors recognized that the application of the residual stress intensity factor did not change the effective stress intensity factor range. The effective stress ratio, \( R_{eff} \), does change with the application of the residual stress and can be calculated using Equation (2.10)

\[ R_{eff} = \frac{K_{eff}^{\text{min}}}{K_{eff}^{\text{max}}} = \frac{K_{\text{applied}}^{\text{min}} + K_{\text{residual}}}{K_{\text{applied}}^{\text{max}} + K_{\text{residual}}} \]  

(2.10)

In Equation (2.10) \( K_{\text{applied}} \) corresponds to the maximum and minimum applied stress intensity factors, while \( K_{\text{residual}} \) represents the stress intensity resulting from the residual stress. The mapping of the residual stress was done based on inverse isoparametric mapping, which allows for mapping from one mesh to another.

Two-dimensional models showed good agreement with published results in their ability to incorporate residual stresses. The authors noted good agreement between residual and effective stress intensity factors when compared with literature. However, the predicted \( K_{eff} \) was more accurate for smaller cracks.
Similar work in incorporating residual stresses was conducted by Hutson et al. [90]. The work was motivated by the desire to incorporate beneficial residual stresses from shot-peening in analyses in order to more accurately predict the inspection interval of critical components. An initial elastic-plastic analysis was done first to capture the residual stresses from the shot-peening process. A residual stress profile was measured and entered into the model with a user-subroutine, before being solved as an equilibrium step prior to the loading cycle. The fatigue crack growth procedure was done by calculating the stress intensity factor at each node along the crack front. The LEFM approach used by Hutson et al. to predict crack growth is governed by the Paris Law Equation, given in (2.11):

\[
\frac{da}{dn} = C(\Delta K)^n
\]  
(2.11)

where, \( da/dn \) is the crack growth per cycle, \( \Delta K \) is the stress intensity factor range and \( n \) and \( c \) are constants for a particular set of stress ratio and temperature. The direction of crack growth is again a function of the Mode I and II stress intensity factors, but the researchers used the maximum tangential stress criterion in Equation (2.12):

\[
\theta = \cos^{-1}\left(\frac{3K_I^2 + 8K_I^2K_{II}^2}{K_I^2 + 9K_{II}^2}\right)
\]  
(2.12)

where \( \theta \) is the out of plane growth direction, while the Mode I and II stress intensity factors are \( K_I \) and \( K_{II} \). The authors were able to find good agreement between fatigue crack growth finite element simulations, with and without the presence of residual stresses, to experimental results.
As expected, the residual stresses improved the fatigue life considerably, while having the greatest influence on small cracks, which corresponded to the greater influence of the residual stress on the stress intensity factor.

Thermal-mechanical loading can be an important contribution to fatigue crack growth. Choi et al. studied the problem of fatigue crack growth in a notched specimen with the intention of optimizing crack growth for subsequent testing purposes [91]. The structural analysis was preformed based on the thermal analysis, from which stress intensity factors were used to predict the magnitude and direction of the crack growth. The researchers were able to reduce the crack growth time by 38.5%.

Fracture along interfaces has been extensively studied using finite elements by Lui et al. [92] and Leblanc et al. [93]. Lui et al. studied the fracture analysis of a crack during an interfacial indentation test. The test was designed to evaluate interfacial adhesion by measuring the length of cracks created by Vickers or Knoop indenters. Interface fracture resistant is strongly dependent on the amount of crack surface shear, Mode II, to surface opening, Mode I, which is represented in Equation (2.13)

\[
\psi = \tan^{-1} \left( \frac{\text{Im}(K^{L^e})}{\text{Re}(K^{L^e})} \right) 
\]

(2.13)

The parameter \( \psi \) is the mode mixity at the distance \( r = \hat{L} \). Other parameters are defined below.
\[ \varepsilon = \frac{1}{2\pi} \ln \frac{1 - \beta}{1 + \beta} \]  

(2.14) a

\[ \beta = \frac{\mu_1 (k_2 - 1) - \mu_2 (k_1 - 1)}{\mu_1 (k_2 + 1) + \mu_2 (k_1 + 1)} \]  

(2.14) b

where \( \mu \) is the shear modulus for the two respective materials, and \( k=(3-\nu)/(1+\nu) \) for plane stress and \( k=(3-4\nu) \) for plane strain, where \( \nu \) is the Poisson’s ratio of the material. The stress ahead of the crack tip can then be characterized in Equation (2.15):

\[
\left( \sigma_{yy} + i \sigma_{xy} \right)_{\theta=0} = \frac{K_{I}^{ic}}{\sqrt{2\pi r}} = \frac{(K_1 + iK_2)r^{ic}}{\sqrt{2\pi r}}
\]

(2.15)

where \( r \) and \( \theta \) are the polar coordinates ahead of the crack tip, \( \varepsilon \) and \( \beta \) are defined in Equation (2.14) and \( K \) is the complex stress intensity factor. If Mode III loading is present, the authors recommended adding the stress field in Equation (2.16) and the contribution of mode mixity in Equation (2.17):

\[
\left( \sigma_{yz} \right)_{\theta=0} = \frac{K_{III}}{\sqrt{2\pi r}}
\]

(2.16)
\[
\phi = \cos^{-1}\left(\frac{K_{III}}{\sqrt{|K|^2 + K_{III}^2}}\right)
\]  

(2.17)

In cases of plasticity, the authors recommended using a modification developed by Shih and Asaro [94].

\[
\zeta = \hat{\psi} + \varepsilon \ln \left(\frac{|K|^2}{\sigma^2 \hat{L}}\right)
\]  

(2.18)

The authors used the Abaqus Contour Integral option to evaluate the stress intensity factors and the J-integral along the crack front. The J-integral characterize the energy release rate for a virtual crack extension of the crack front. The authors noted that near surface cracking could occur if the strain hardening properties of the two materials are dissimilar enough. The authors also noted the importance of including plastic deformation to propagate cracks.

Schüller and Lauke simulated the interfacial crack propagation using an energy release rate failure criterion to evaluate the viability of the elements at the crack tip [95, 96]. This essential work of fracture incorporates the failure mechanisms in front of the crack into a process zone. The method was used to determine the fracture toughness of a bimaterial polymer interface. An elastic-plastic material law was used to model the material. The simulation results were compared with theoretical results developed using the essential work of fracture and found to be in good agreement.
Maiti et al. applied the crack opening displacement (COD) in conjunction with a finite element analysis to model mode I and mixed mode stable crack growth in steel [97]. In their analysis, the load was increased until the COD at the first corner node reached a critical value. The procedure was repeated until instability was reached. According to the procedure, the nodes were released and replaced by reaction forces. While the analysis could not include the effects of residual stress-strain field in the wake region and the plastic hardening ahead of the crack tip, it did include an increase in closure forces to compensate. The researchers also saw good results when comparing the load against displacement for finite element results with experimental measurements for Mode I and mixed mode loading conditions.

Leblanc et al. used a crack growth elimination model to generate interface toughness curves [93]. The researchers assumed that bimaterial interface crack growth occurs when the energy release rate (ERR) at the crack tip is found to exceed a critical value. The authors used similar theory as above to evaluate the stress and stress intensity factors. The numerical results were compared with experimental results, validating the testing procedure and the finite element model.

Pasqualino et al. studied the problem of crack growth in the girth welds of steel catenary risers [98]. The risers experienced a constant tensile load and a cyclic bending moment. Authors used the maximum energy release rate vector, $G_{\text{max}}$, to predict the crack growth. The local energy release rate values for different crack growth extensions at each node were determined from the finite element analysis. Crack growth magnitude and direction were determined over a number of cycles by numerical integration. The change in energy release rate per cycle, $dG/da$, is assumed constant over the number of cycles. For Mode I loading and plane stress conditions the relationship between $K$ and $G$ is given in Equation (2.19):
\[ K = \left( \frac{EG}{1 - \nu^2} \right)^{1/2} \]  

The authors found that the addition of plastic strains did not have significant effects on the fatigue life of the weld because the strains were insignificant when compared to the plastic strains at the crack tip. Significant fatigue life reductions were seen when the non-time-dependent tensile force was increased.

### 2.12 Evaluating Cohesive Zone Properties

The evaluation of the cohesive zone properties for an interface is not a trivial problem. Universal standards or procedures for measuring the properties do not exist. Most often the properties are evaluated using a combination of experiments and numerical modeling, although analytical solutions have been developed for select cases. The properties generally used to define the cohesive zone are critical fracture energy and maximum stress for each of the fracture modes considered. Most researchers have discovered that the actual shape of the cohesive zone is insignificant, while the stiffness is most often used as a tuning parameter or approximated based on geometry and material properties.

The critical fracture energy is recognized as an experimentally measureable parameter. However, significant issues exist in terms of experimentally measuring the strength of an interface. As strength is used to characterize an interface, it generally refers to a critical uniform stress, in tension or shear, required to produce interfacial failure. The problem with this approach is that interface failure frequently initiates at a flaw or the region of high stress due to factors such as stress concentrations, resulting in fast fracture along the interface. Such test are notorious for
producing large amounts of scatter, as the apparent strength of the interface is a strong function of flaws, specimen preparation, and testing procedures. Recognizing such limitations for interfacial strength measurement, researchers often use the normal and shear cohesive strength parameters as tuning parameters in a finite element model. In such an approach, Mode I and II fracture energies are first measured and input to the model, while the normal and shear cohesive strengths are adjusted to reproduce experimental results with the numerical model. Such an approaches has been used by Yang et al. [58, 99] and Omiya [100], while a similar inverse approach has been used by Bocciarelli and Bolzon [101, 102].

2.12.1 Mode I: The Wedge-Peel and Peel Tests

Two common methods to evaluate the Mode I fracture properties of adhesives and interfaces are the wedge test and the peel test. The wedge-peel test is generally used to evaluate the interface properties for which the adherents are the same material and thickness. Alternatively, the peel test is used to evaluate the adhesion of thin films or coatings, which are pulled from the interface at different angles.

The wedge test evaluates the properties of the interface by forcing a wedge between the interfaces of two similar adherents. The wedge-peel test requires the presence of bending-induced plastic deformation to evaluate the cohesive zone parameters. Kim and Kim [103] and Kim and Aravas [104] developed a relation between material properties and test parameters that were required for valid results:
\[
\frac{\sigma_0}{E} \leq 3 \frac{G_{cn}}{\sigma_0 h}
\]  

(2.20)

where \(\sigma_0\) and \(E\) are the yield stress and elastic modulus of the adherents, \(G_{cn}\) is the bond toughness, and \(h\) is the thickness of the adherent. Ferracin et al. [105] proposed a method for experimental evaluation of bond toughness and peak cohesive strength. The method required a measurement of crack length and permanent curvature of substrates after test completion. A calibration procedure must be completed first, where curves of constant stress are calculated for the interface toughness as a function of radius of curvature. To apply the method, experimental parameters are first calculated from the curves before the fracture energy can be evaluated.

Daghyani [106] calibrated bond toughness based on experimental measurements of the radius of curvature during wedge-peel tests. The maximum stress was evaluated to calculate the traction forces in the adhesive layer using a finite element model with isotropic plasticity.

Thouless et al. [107] used post-fracture observations to evaluate the toughness of the interface for a double cantilevered beam that failed with large plastic deformations in the adherents. Again, the adherents were split with a wedge before the post-fracture radii of curvature were measured. The applied moments at fractures were evaluated based on the post fracture radii of curvature of the adherents. Because the moment can be calculated directly from the radii of curvature, the authors provided a method to calculate fracture toughness using a simple measurement and material properties. Thouless et al. [108] also determined that toughness was independent of thickness of the adherents.
The peel test has the advantage, compared to the wedge test, of being able to evaluate interface properties for thin coatings. Kinloch et al. [109] developed a method to measure adhesive toughness for the peel test that included the effects of energy absorption and deformation in the coating, or peeling arm. The peeling arm was treated as a beam that allowed rotation at the crack front and expressions were provided to calculate the fracture toughness for different conditions of plasticity. The authors found that interface toughness was not a factor of material properties, but was affected by the rate of propagation of the peel front.

Kim and Aravas [104] used an energy balance to relate peel force to fracture energy, assuming beam theory and that the coating is an elasto-plastic beam under plane strain. For elastic peeling, it was found that peel force is a direct function of interface fracture energy. Including elasto-plastic effects requires that the work consumed by plasticity be subtracted from the peel force per unit thickness of adherent. Kim and Kim [103] considered the different stages in crack growth in terms of plasticity and compared experimental and theoretical results, while providing a closed form solution for peel strength contributions from decohesion and bending. A diagram was constructed that allowed for the determination of interface toughness from peel tests for elastic perfectly plastic coatings.

### 2.12.2 Mode II: The Dual-Cantilevered Beam and End-Notched Flexure Tests

The end-notched flexure (ENF) specimen is commonly used to measure Mode II fracture properties of an adhesive that joins two beams. Before the test, a crack is started on the edge of the specimen. After which, the specimen is loaded into a three-point bend configuration to grow the crack. In the event that stable crack growth occurs before the crack reaches the center of the beam, the loading geometry provides an ideal experiment for studying Mode II fracture. The test
has already been used to analyze fracture using LEFM concepts by Chai [110]. Yang et al. [99] expanded the concept to include extensive plastic deformation accompanying failure. Experimental and numerical results were compared to evaluate the cohesive zone properties. First the shear strength of the interface was measured using a butt joint experiment. After the shear strength was evaluated, a finite element model was developed with cohesive zone elements at the interface. Experimental results were compared with numerical simulations in order to select the interface fracture toughness that reproduced the experimental results with the numerical model. Once the interface properties were evaluated, they were used to predict the response of ENF samples with beams of different thicknesses and found to produce results that matched experiments.

2.12.3 The Shear Button Test

The shear button test has been used to measure both fracture toughness and normal and shear strength of bi-material interfaces. Samples are produced by manufacturing an island or button of coating on the substrate. A displacement is applied perpendicular to the button until the island separates from the substrate; the applied load as a function of displacement is measured throughout the test.

Interfacial adhesion button tests have been used by many authors to evaluate the bond strength [111-114] and fracture energy [111, 115]. Szeto et al. [114] used experimental results in conjunction with a finite element model to evaluate the normal and shear strength of an interface. Two different tests were conducted; one test where the displacement was applied at the bottom of the button, closer to the interface, and another at the top. After the test was simulated
numerically, a stress averaging method was used to avoid the stress singularity and measure the normal and shear strength.

Tay et al. [111] developed a modified shear button test to measure the fracture toughness of a bimaterial interface. The researchers started a pre-crack for the button and continued the rest of the test following the same procedure as a conventional shear button test. A finite element model was used to evaluate the stress intensity factors for Mode I and II loading at the crack tip. The relationship between the stress intensity factors and fracture energy release rate developed by Hutchinson and Suo [116] was used to determine the fracture toughness. The energy release rate could then be decomposed using the phase angle.

2.12.4 Mixed Mode Tests

Researchers have attempted to use a single test method to measure the critical fracture energies of different modes. By using the same method to measure properties of both modes, the properties can be measured with a single test, and will be consistent between the different modes. A common test to determine the fracture energy as a function of the combined modes is the peel test. The angle between the force applied to the coating and the substrate can be varied between 0° and 180° in order to introduce varying contributions from each mode into the fracture. At the extremes, pure Mode I or II fracture occurs.

Kinloch et al. [109] discussed the effects of mode mixity on the fracture toughness and neglected the effects. The conclusion was justified since the peel angles were not near the extremes and the
authors suspected that there would be little difference between the Mode I and II fracture toughness values.

The peel test was used by Karbhari et al. [117-119] to analyze the contributions of the different fracture modes and the Mode I and II fracture toughness values. The researchers analyzed different composites bonded to concrete. The authors assumed that the bond failure occurred at a controlled rate, the peel force was directly related to the work of detachment and failure occurred due to mixed mode loading. In order to include the effects of both modes, the authors first recognized that the interfacial fracture energy can be decomposed as follows:

\[ G = G_I + G_{II} \]  \hspace{1cm} (2.21)

where \( G \) is the total fracture energy and \( G_I \) and \( G_{II} \) are the contributions from each mode. Further, a measure of the contributions of each Mode is defined by the phase angle, given in Equation (2.22).

\[ \tan^2 \psi = \left( \frac{G_{II}}{G_I} \right) \]  \hspace{1cm} (2.22)

where \( \psi \) is the phase angle of the loading, defined by Equation (2.23)

\[ \psi = \tan^{-1} \left[ \frac{1 - (1 + \varepsilon \cos \alpha)}{1 + \varepsilon \sin \alpha} \right] \]  \hspace{1cm} (2.23)
where \( \varepsilon \) is the strain in the peel arm and \( \alpha \) is the angle between the substrate and the force that the coating is peeled at. The authors measured the fracture energy for different peel angles, \( \alpha \), and decomposed the fracture energy to the different modes. The critical fracture energies were then measured by plotting the Mode II fracture energy as a function of mode I fracture energy, and recognizing that at pure Mode I or II fracture, the other mode’s contribution is zero. A similar analysis of the peel test was done by Moidu et al. [120]. The main focus of the research was to characterize the amount of energy dissipated in the coating arm as it was pulled.

### 2.12.5 Four-Point Bend Test

The four-point bend test is popular because it consistently and accurately evaluates the fracture energy at a bimaterial interface. Because of the configuration, it is especially useful for coating applications. Sample preparation is generally straightforward; a crack is first inserted through the coating thickness at the center, before being extended into both sides of the interface. However, Ashcroft et al. [121] have completed the test without pre-cracking. The four-point bend test is an attractive configuration for determining interfacial fracture energy because the fracture energy is a simple function of experimental parameters, load and displacement, and material and specimen parameters. Though the experiment is relatively straightforward, several factors such as plastic deformation and brittle crack growth limit the applicability of the method.

The four-point bend test geometry is shown in Figure 2.1. A precrack is first inserted into the coating, generally using a diamond-coated saw blade. Researchers generally introduce the interface precrack by loading the sample in a three- or four-point bend configuration. The effect of the precrack length has been analyzed by Clyne and Gill [122], who found that at half-crack lengths of more than twice the coating thickness, the energy release rate was no longer a function...
of the initial crack length. During the test, the displacement can be applied at the load points of the fixture, shown on the bottom in the figure, while the plate is supported. Though the four-point bend configuration was designed to grow the crack symmetrically, the displacement of the coating at the center is often measured, with linear variable displacement transducers (LVDTs) or optical microscopy, for verification.

![Diagram of four-point bend loading arrangement](image)

**Figure 2.2:** The four-point bend loading arrangement.

The four-point bend test specimen for evaluating the interfacial critical energy release rate of coated specimens test was first developed by Charalambides et al. [123]. The authors used the Euler-Bernoulli beam theory and the plane-strain condition assumptions to calculate the energy release rate of crack growth. In the following equations, the subscript “1” denotes the coating, while “2” denotes the substrate. The elastic modulus and Poisson’s ratio are given by $E$ and $\nu$, respectively, while $h$ is the thickness. The authors calculated the strain energy release rate using Equation (2.24).
where \( M = P s / 2b \) is the bending moment per unit width, where \( P \) is the total load, \( s \) is defined in the above figure, and \( b \) is the specimen width. Subsequent research [124] included the effects of friction at the loading points. To account for this, the moment, \( M \), from Equation (2.24) can be replaced with Equation (2.25).

\[
M = M^p + M^f = \frac{P s}{2b} \left( 1 - \frac{\mu h}{s} \right)
\]  

(2.25)

where \( \mu \) is the coefficient of friction between the loading points and the specimen, and \( h \) is the total specimen height. The remaining parameters, \( \lambda \), \( I_c \), and \( I_2 \), are defined below.

\[
\lambda = \frac{E_2 (1 - v_1^2)}{E_1 (1 - v_2^2)}
\]  

(2.26)

\[
I_c = \frac{h_1^3}{12} + \frac{\lambda h_2^3}{12} + \frac{\lambda h_1 h_2 (h_1 + h_2)}{4 (h_1 + \lambda h_2)}
\]  

(2.27)

\[
I_2 = \frac{h_2^3}{12}
\]  

(2.28)
The authors could not obtain unique critical strain energy release rates if the friction term was neglected. The importance of residual stress effects, if present, was also noted in the research.

Another analysis of the crack propagation for the four-point bend test was provided by Howard et al. [125] based on the change in compliance of the beam that results from the extension of an interfacial crack. The strain energy release rate, $G$, for crack propagation can be calculated according to Equation (2.29).

$$ G = \lim_{\Delta a \to 0} \frac{P_1 P_2}{4b} \left( \frac{\Delta C}{\Delta a} \right) \approx \frac{P^2}{4b} \left( \frac{dC}{da} \right) $$  \hspace{1cm} (2.29)$$

where $P_1$ and $P_2$ are the loads at the initiation and termination of the crack, and $\Delta C$ is the change in compliance resulting from the change in half-crack length, $\Delta a$. If one assumes steady crack growth, the change in compliance with respect to the half-crack length, $dC/da$, is a constant.

The displacement of the rollers can be related to the load and half-crack length.

$$ u = a \frac{P_s^2}{2} \left[ \frac{1}{K_s} - \frac{1}{K_c} \right] + \frac{P_s^2}{K_c} \left( \frac{s + L}{6} + \frac{1}{4} \right) $$  \hspace{1cm} (2.30)$$

where $s$ and $L$ are illustrated in Figure 2.2, and $K_s$ and $K_c$ are the stiffness of the debonded and bonded (composite) beam, respectively. The stiffness or compliance of a beam can be calculated from the integral shown in Equation (2.31).
\[ K = \int_{\text{beam}} E(y) y^2 \, dA \]  

(2.31)

The distance from the neutral axis is \( y \) and \( E(y) \) is the elastic modulus as a function of the distance from the neutral axis. \( K_s \) and \( K_c \) are given in Equations (2.32) through (2.34).

\[
K_c = \frac{E_s}{1 - \nu_s^2} h_d b \left( \frac{h_d^2}{3} + h_s \delta + \delta^2 \right) + \frac{E_d}{1 - \nu_d^2} h_d b \left( \frac{h_d^2}{3} - h_d \delta + \delta^2 \right) 
\]

(2.32)

\[
K_s = \frac{E_s h_s^3 b}{12 \left( 1 - \nu_s^2 \right)}
\]

(2.33)

\[
\delta = \frac{1}{2} \left( \frac{E_d}{1 - \nu_d^2} h_d^2 - \frac{E_s}{1 - \nu_s^2} h_s^2 \right) \left( \frac{E_s}{1 - \nu_s} h_s + \frac{E_d}{1 - \nu_d} h_d \right) 
\]

(2.34)

The subscripts for the deposit (coating) and substrate are \( d \) and \( s \), respectively. The thicknesses of the deposit and substrate are given by \( h_d \) and \( h_s \), \( s \) is defined in Figure 2.2, \( b \) is the sample width, and the elastic modulus and Poisson’s ratio are \( E \) and \( v \). The distance that the neutral axis is moved from the center is \( \delta \) and is defined as positive if it moves from the center in the direction of the coating.

From Equation (2.30), the compliance can be defined as the rate of change of roller displacement with respect to the load, as shown in Equation (2.35).
\[ C = \frac{du}{dP} = \frac{as^2}{2} \left( \frac{1}{K_s} - \frac{1}{K_d} \right) + \frac{s^2}{K_c} \left( \frac{s}{6} + \frac{L}{4} \right) \]  

(2.35)

From Equation (2.35), the rate of change of compliance with respect to the half-crack length can then be defined.

\[ \frac{dC}{da} = \frac{s^2}{2} \left( \frac{1}{K_s} - \frac{1}{K_d} \right) \]  

(2.36)

Equation (2.37) can then be obtained by combining Equations (2.29) and (2.36) and assuming symmetric crack growth.

\[ G \approx \frac{P^2 s^2}{8b} \left( \frac{1}{K_s} - \frac{1}{K_d} \right) \]  

(2.37)

The criterion for the critical strain energy release rate is then: \( G > G_c \). In other words the critical energy release rate is the energy release rate required to grow a previously arrested crack and corresponds to a critical load, \( P_c \). Howard et al. [125] outlined how to calculate the critical energy release rate for stable and unstable crack propagation. For the case of crack bursting, the authors recommended that the \( P^2 \) be replaced with the crack initiation and termination loads, \( P_i \) and \( P_2 \). After the development of a procedure to measure the fracture energy for the four-point bend test, the authors also included an analysis of the effects of asymmetry and residual stresses and recommended using a floating platen to negate the effects of asymmetric crack growth.
Finally, the authors developed an additional term for the strain energy release rate to include the effects of the residual stress.

In subsequent research on the topic, Howard et al. [126] expanded on the research summarized above. Contributions to the energy release rate for the residual stress were included for the residual stresses expected in the absence of applied loading and an interaction term that is dependent on the applied loading and residual stress state. The updated energy release rate is given in Equation (2.38).

\[
G = \frac{P^2}{4b} \frac{dC}{da} + \frac{P}{2b} \frac{du_r}{da} + \Omega 
\]

where \( u_r \) is the residual displacement measured after unloading and \( \Omega \) is the available strain energy release rate for the unloaded beam. The residual stress terms can then be defined in Equations (2.39) and (2.40):

\[
\frac{du_r}{da} = -s(\kappa_s - \kappa_r) 
\]

\[
\Omega = \frac{1}{2b} \left( K_s \kappa_s^2 + K_d \kappa_d^2 + \frac{E_s A_s \varepsilon_s^2}{(1-v_s)} + \frac{E_d A_d \varepsilon_d^2}{(1-v_d)} \right) 
\]

The cross-sectional areas of the deposit and substrate are given by \( A_d=bh_d \) and \( A_s=bh_s \), \( s \) is defined in Figure 2.2, the elastic modulus and Poisson’s ratio are \( E \) and \( v \), the average axial strains in the bonded substrate and coating are \( \varepsilon_s \) and \( \varepsilon_d \), the unbonded curvature of the substrate...
is $\kappa_s$, the curvature of the bonded composite beam is $\kappa_c$, while $\kappa_{sr}$ and $\kappa_{dr}$ are the average strain gradients through the unbonded substrate and deposit. The same approach to account for residual stress was used by Yamazaki et al. [127].

Based on the experimentally measured curvatures mentioned above, the curvatures before, $\kappa_c$, and after, $\kappa_d$ and $\kappa_s$, debonding can be related to the assumed linear residual stress distribution according to Clyne and Gill [122].

\[
\sigma_d = \frac{2K_d(\kappa_d - \kappa_c) + 2K_s(\kappa_d - \kappa_c)}{bh(h + H)} + E_d(\kappa_d - \kappa_c)y_d \tag{2.41}
\]

\[
\sigma_s = \frac{2K_d(\kappa_d - \kappa_c) + 2K_s(\kappa_d - \kappa_c)}{bh(h + H)} + E_s(\kappa_s - \kappa_c)y_s \tag{2.42}
\]

where $\sigma_s$ and $\sigma_d$ are the stresses in the in the substrate and deposit, respectively, while $y_s$ and $y_d$ are the distance coordinates from the respective neutral axes for the substrate and deposit.

In addition, the phase angle can be calculated based on the applied moment and the assumed residual stresses. The moments and forces per unit thickness at the crack tip, while in the constant moment region between the inner loading lines, were calculated by Howard et al. [128] for the debonded side of the crack tip in the coating, $M_1$ and $F_1$, and substrate, and the composite side of the beam, $M_3$ and $F_3$. 

53
\[ F_i = E_d A_i \varepsilon_{dr} = \frac{-\Sigma_d (\kappa_d - \kappa_c) - \Sigma_s (\kappa_s - \kappa_c)}{0.5(h_d + h_s)} \] (2.43)

\[ F_3 = 0 \] (2.44)

\[ M_1 = \Sigma_d \kappa_{dr} = \Sigma_d (\kappa_d - \kappa_c) \] (2.45)

\[ M_3 = -M_{app} = -\frac{P_s}{2b} \] (2.46)

The above moment equations can then be applied to the scheme of Suo and Hutchinson [129] to determine the phase angle of the fracture during the four-point bend test. The phase angle can then be calculated according to Equation (2.47).

\[ \Psi = \tan^{-1} \left[ \frac{\lambda \sin \omega - \cos(\omega + \gamma)}{\lambda \cos \omega + \sin(\omega + \gamma)} \right] \] (2.47)

The parameter \( \omega \) is a function of the Dundars’ parameters, and the ratio of coating and substrate thicknesses, defined below.

\[ \alpha = \frac{\Gamma(\kappa_2 + 1) + (\kappa_1 - 1)}{\Gamma(\kappa_2 + 1) + (\kappa_1 - 1)} \] (2.48)
\[ \beta = \frac{\Gamma(\kappa_2 - 1) - (\kappa_1 - 1)}{\Gamma(\kappa_2 + 1) + (\kappa_1 - 1)} \]  

(2.49) 

\[ \Gamma = \frac{\mu_1}{\mu_2} \]  

(2.50) 

where \( \mu \) is the shear modulus, in addition \( \kappa=3-4\nu \) for plane strain and \( \kappa=(3-\nu)/(1+\nu) \) for plane stress. The subscript 1 signifies the deposit, while the 2 is used for the substrate. The parameters \( A, I, \) angle \( \gamma, \Sigma, \) and \( \eta \) are defined below.

\[ A = \frac{1}{1 + \Sigma(4\eta + 6\eta^2 + 3\eta^3)} \]  

(2.51) 

\[ I = \frac{1}{12(1 + \Sigma \eta^3)} \]  

(2.52) 

\[ \sin \gamma = 6\Sigma \eta^2(1 + \eta)\sqrt{AI} \]  

(2.53) 

\[ \Sigma = \frac{1 + \alpha}{1 - \alpha} \]  

(2.54) 

\[ \eta = \frac{h}{H} \]  

(2.55)
where $h$ is the thickness of the deposit and $H$ is the thickness of the substrate. The load terms, defined in terms of Equations (2.43) through (2.46), $P$ and $M$ are given by:

$$P = P_1 - C_1 P_3 - C_2 \frac{M_3}{h}$$  \hspace{1cm} (2.56)

$$M = M_1 - C_3 M_3$$  \hspace{1cm} (2.57)

where,

$$C_1 = \frac{\Sigma}{A_0}$$  \hspace{1cm} (2.58)

$$C_2 = \frac{\Sigma}{I_0} \left( \frac{1}{\eta} - \Delta + \frac{1}{2} \right)$$  \hspace{1cm} (2.59)

$$C_3 = \frac{\Sigma}{12I_0}$$  \hspace{1cm} (2.60)

$$A_0 = \frac{1}{\eta} + \Sigma$$  \hspace{1cm} (2.61)
\[ I_0 = \frac{1}{3} \left[ \sum 3 \left( \Delta - \frac{1}{\eta} \right)^2 - 3 \left( \Delta - \frac{1}{\eta} \right) + 1 \right] + 3 \frac{\Delta}{\eta} \left( \Delta - \frac{1}{\eta} \right) + \frac{1}{\eta^3} \]  \quad (2.62)

\[ \Delta = \frac{1 + 2 \Sigma \eta + \Sigma \eta^2}{2 \eta (1 + \Sigma \eta)} \]  \quad (2.63)

Recognizing that interfacial fracture during the four point bend test is highly mixed mode, the phase angle is an important component of the critical fracture energy. Equation (2.47) calculates the phase angle for the four-point bend test as a function of applied loading and includes the significant effects caused by residual stresses.

Tsui et al. [130] used the above equations to compare model predictions with experimental data. The authors used Ti-6Al-4V substrates coated with boron carbide by plasma spraying. The authors first measured the curvatures of the samples, cut a notch through the coating with a diamond impregnated wire on a slow speed saw, before an interface precrack was introduced. During four-point bend testing, the load and displacement were continually monitored until the crack had propagated to the inner load point. The stable or critical crack load was recorded and used, in addition to the measured curvatures and strains of the debonded coating and substrate, to calculate the critical energy release rate. Numerically calculated residual stresses, determined based on finite element modeling of the coating deposition process, matched well with the distribution based on the curvature and strain measurements. The authors also observed that significant error would have been introduced had the residual stresses been ignored.
Katipelli et al. [131] used the method of strain energy release rate developed by Charalambides et al. [123] to calculate the critical energy release rate. Assuming that the coating is sufficiently thin when compared to the total height of the specimen, the researchers simplified the expression for the strain energy release rate:

\[
G = \frac{18M^2(1 - v_2^2)^2 E_1 h_1}{(1 - v_1^2)^2 E_2^2 h^4}
\]  

(2.64)

where the subscripts 1 and 2 denote the coating and substrate, respectively. The coating thickness is \(h_1\), and the total thickness is \(h\).

Phillips et al. [132] compared three- and four-point bending configuration results. The three-point arrangement resulted in slightly lower critical fracture energy measurements, and almost identical phase angles, between 30° and 50°.

### 2.12.6 The Blister Test

The blister test is useful for evaluating the adhesion of thin coatings to substrates. A blister test sample is constructed by drilling a hole in the substrate which stops at the coating interface. In some tests, a crack is started before the test by preventing bonding between the coating and substrate during coating deposition. Pressure is applied at the hole in the bottom of the substrate and the height of the resulting blister is measured as a function of the applied pressure. The advantages of the blister test are that the samples are easy to manufacture and they are evaluating the actual coating conditions. However, the test only evaluates the fracture energy and does not
provide a mechanism to evaluate the strength of the adhesion and the contributions of the different modes can be difficult to separate.

Jensen [133] was one of the leading researchers in applying the blister test to determine the adhesion toughness of bonded coatings. Relationships were developed for the energy release rate and mode mixity applicable during the debonding. Jensen assumed small deflections in the coating and developed a universally used equation for the mode mixity at the crack tip.

\[ \tan \psi = \frac{\sqrt{12} M \cos \sigma + hN \sin \sigma}{-\sqrt{12} M \sin \sigma + hN \cos \sigma} \]  

(2.65)

where \( M \) and \( N \) are the moment and force at on the coating, determined from membrane or beam theory, and \( \omega \) is a function of the coating and substrate material properties. The effects of mode mixity were further analyzed by Jensen [134], who calculated the critical energy release rate based on the load or the deflection of the blister. The authors also developed relationships for a point load instead of a pressure inside the blister. All the results were verified numerically. Residual stresses were included in the blister test analysis by Jensen and Thouless [135].

The transition between plate and membrane behavior for a blister is an important consideration analyzed by Cotterell and Chen [136]. Comparisons were made between plate and membrane behavior; the blister behavior was more closely replicated by plate theory in experiments. The authors found that as the blister deflection increased, the interfacial fracture energy increased. In addition, the interfacial fracture energy increased to a point as the film thickness decreased. Modified forms of the closed-form solutions for mode mixity and energy release rate were
developed. The researchers found that the addition of residual stresses affected the energy release rate, phase angle and could possibly cause a non-circular delamination shape.

2.12.7 Indentation Tests

Indentation tests are used to evaluate the properties of interfaces between dissimilar materials. The details of specific indentation tests vary, but the test generally consists of an indenter, commonly a cone, pyramid (Vicker’s indenter), sphere, or wedge that is displaced into the coating surface. According to Ritter et al. [137, 138] both the Vicker’s and ball indenters initiate an interfacial crack along the perimeter of the contact zone, which grows away from the indenter with further loading. The Vicker’s indenter initiates debonding at a significantly lower applied force and the deformation is primarily plastic. Debonding initiates due to indenter contact stresses that propagate away from the indent impression zone. As the indenter is displaced, the contact force increases, as do the stresses at the interface. The loading at the interface is primarily Mode II at debonding initiation; after sufficient loading and the onset of coating buckling, the loading becomes primarily Mode I. The various indentation tests have been studied thoroughly, both experimentally and numerically. Numerical simulations have commonly included cohesive zone elements at the interface to simulate the separation of coatings from substrates.

Matthewson [139] studied the resulting interfacial shear strength measurement techniques for ball indentation and Vicker’s indenters. Three possible types of debond initiation were studied: debonding with elastic deformation under the indenter, debonding with plastic deformation under the indenter, and the case where the indenter penetrates the substrate. The authors first analyzed typical steps of an indentation analysis. After application of loading to the indenter,
interfacial stresses increased as the coating material deforms. At a critical shear stress, the coating separated from the substrate at a radial location equal to the contact perimeter. However, the interface directly underneath the indenter remained un-fractured due to high compressive stresses. Further load increases cause the film to lift off of the substrate in a Mode I loading state as a result of the restraint from the un-separated interface. If sufficient plastic deformation occurs in the substrate during indentation, the area under the indenter’s contact will undergo Mode I debonding due to elastic recovery in the coating.

Matthewson [139] developed solutions for the shear stress at the initiation of debonding. In the case of a predominately elastic debonding initiation, the interfacial shear stress at the initiation of debonding, $\tau_c$, was given by Equation (2.66), while the stress outside of the contact zone ($r>a_c$, where $r$ is the radial position), $\tau_0$, was given by Equation (2.67).

$$\tau_c = G \left[ \frac{\alpha}{2} K_1 \left( \frac{\phi a_c}{h} \right) + \frac{\nu a_c}{(1-2\nu)R} - \frac{\beta}{2} I_1 \left( \frac{\gamma a_c}{h} \right) \right]$$ \hspace{1cm} (2.66)$$

$$\tau_0 = G \alpha K_1 \left( \frac{\phi r}{h} \right)$$ \hspace{1cm} (2.67)

where $a_c$ is the contact radius at debonding, $G$ is the coating shear modulus, $\nu$ is the Poisson’s ratio, $R$ is the radius of the ball indenter, and $h$ is the coating thickness. Additional variables are defined in Equations (2.68) through (2.71):

$$\phi = \sqrt{\frac{6(1-\nu)}{4+\nu}}$$ \hspace{1cm} (2.68)
\[
\gamma = \sqrt{\frac{3(1 - \nu)}{2(1 - \nu)}} \tag{2.69}
\]

\[
\beta = \frac{\frac{\phi\alpha_e}{h} K_1 \left( \frac{\phi\alpha_e}{h} \right) - K_1 \left( \frac{\phi\alpha_e}{h} \right) \frac{h}{2R} (1 - 6\nu)}{\gamma K_1 \left( \frac{\phi\alpha_e}{h} \right) I'_1 \left( \frac{\gamma\alpha_e}{h} \right) - \phi I'_1 \left( \frac{\gamma\alpha_e}{h} \right) K_1 \left( \frac{\phi\alpha_e}{h} \right) (1 - 2\nu)} \tag{2.70}
\]

\[
\alpha = \frac{-4}{(4 + \nu)\phi K_1 \left( \frac{\phi\alpha_e}{h} \right)} \left[ (1 - 6\nu)h \frac{2(1 - \nu)}{2R(1 - 2\nu)} + \beta I'_1 \left( \frac{\gamma\alpha_e}{h} \right) \right] \tag{2.71}
\]

where \( I_1(x) \) and \( K_1(x) \) are first order modified Bessel functions, and \( I'_1(x) \) and \( K'_1(x) \) are their derivatives. Also useful is the relationship provided by Matthewson which related the indenter load to the contact radius, and is shown in Equation (2.72):

\[
P = \pi a^2 G \left[ \frac{2\nu h(1 - 6\nu)}{3R(1 - 2\nu)^2} + \frac{4\nu h}{3a(1 - 2\nu)} I'_1 \left( \frac{\gamma\alpha_e}{h} \right) + \frac{2(1 - \nu)}{1 - 2\nu} \left( \frac{a_e^2}{4hR} - \delta \right) \right] \tag{2.72}
\]

where \( \delta \) is defined in Equation (2.73):
Using Equations (2.72) and (2.73), the load at the initiation of debonding can be calculated, and then used to calculate the contact radius.

The interfacial shear strength for the cases of plastic deformation, for Vicker’s indenters, can be calculated using Equation (2.74).

\[
\tau_c = \frac{-0.56P_c / 2b_c^2}{\left[K_1\left(\frac{\phi b_c}{h}\right)\right] - 2\left[K_1\left(\frac{\phi K_1}{h}\right)\right] + \frac{\sqrt{h}}{b_c\phi^2}}
\]

(2.74)

where \(b_c\) is half the length of the coating/indenter contact and all other parameters are as defined earlier. In addition, the shear strength for the case in which the Vicker’s indenter penetrates through the coating and into the substrate is given in Equation (2.75).

\[
\tau_c = \frac{\left(P_c - 2H^*c_c^2\right)}{2\left(b_c^2 - c_c^2\right)}
\]

(2.75)

where \(H^*\) is the hardness of the substrate, \(c_c\) is the contact diagonal on the substrate, and all other parameters are as previously defined. The analysis is typically accurate to within 5%, which increases as the ratio of contact radius to coating thickness is increased.
The solutions presented above were also employed by Ritter et al. [137, 138] to study adhesion of thin polymer coatings on glass. Experimental observations and results were used with the solutions of Matthewson [139] to analyze interfacial crack growth during indentation [138]. During indentation, a crack was initiated at the perimeter of the contact zone and grew outwards. The authors noted that the primary difference between Vicker’s and ball indenters was that debonding initiation for tests with the Vicker’s indenter occurred at a significantly lower load and the deformation at debonding was primarily plastic.

Good agreement was observed when the experimental and analytical results were compared for the contact radius as a function of applied load and the interfacial shear strength as a function of the coating thickness. In a subsequent paper [137], the authors compared shear stresses measured with indentation tests to those measured using lap-shear tests. The measured indentation interfacial strengths were over an order of magnitude higher than those measured using the lap shear test. The authors suspected the discrepancy was a result of the indentation test measuring the intrinsic interfacial shear strength, while the measured lap shear strength was controlled primarily by large flaws at the location of the stress concentration.

Sun et al. [140] and Sen et al. [141] have studied indentation of a sphere onto a coated substrate using finite element simulations. Sun et al. [140] studied the plastic deformation of TiN coatings. For the system studied, the authors observed plastic deformation in the substrate underneath the indenter before such deformation was seen in the coating, except for the cases of thick coatings. Overall, the authors noted that thin coatings on soft substrates experience adhesive and substrate failure, while thick coatings on strong substrates will experience cohesive failure in the coating.
Sen et al. [141] studied the problem of the indentation of an elastic sphere on an bilinear elastic-plastic substrate with a thin, elastic coating layer that is stiffer than the substrate. Plots were provided of the stresses on the surface, the stresses along the central axis underneath the indenter and the stresses along the interface. The authors also reported that the undesirable (for the purposes of parameter evaluation) interface yielding occurs for thicker layers, while the normal and shear stresses at the interface decrease as the layer thickness is increased.

Sriram et al. [142] have studied the problem of cracks through the coating during the indentation process in terms of the energy release rate of the cylindrical cracks. The authors found that cracks near the indenter experienced stable growth; the energy release rate decreased with crack length. Additionally, the stable crack growth regime dissipates at radii away from the indenter.

The effects of a rigid indenter versus a deformable indenter were studied by Faulkner et al. [143]. The authors found that the assumption of a rigid indenter underestimated the radial tensile stress that is responsible for the film cracking at the interface. In terms of delamination, the rigid indenter actually proved to provide conservative estimations of the interfacial stress; interfacial shear stress decreases as the stiffness of the indenter was increased.

Abdul-Baqi et al. studied interface delamination for strong films on ductile substrates [87, 144] and cracking of brittle coatings on ductile substrates [145] using a cohesive zone to model the interface, and the crack surfaces in the case of the later study. In the former study, the indentation induced delamination of an elastic coating on an elastic-perfectly plastic substrate was simulated. The authors found that delamination occurred in a shear mode, and not a normal
one. Debonding initiation was also found to occur at a radial position at two to three times the contact radius, opposed to the perimeter of the contact radius predicted by the researchers referenced earlier. Also, in contradiction to the works cited earlier, the authors found that after debonding was initiated, the crack grew in both directions, and arrested on the inner front when it reached the highly compressive stress under the indenter. Though, delamination is largely due to shear stresses associated with plastic substrate plastic deformation, the normal stresses contribution is significant due to the coupling effect of the stress components assumed by the researchers. The same authors also analyzed the process of delamination resulting from indenter unloading [144] with the same basic assumptions as the earlier study. Unloading delamination was found to be driven by stresses normal to the interface. The bending moment caused the region directly under the contact area to be lifted off of the substrate. For constant indenter depth, thicker and stiffer coatings, and substrates with a higher yield strength resulted in larger interfacial normal stresses. The authors also found that compressive residual stresses delayed film delamination. Cohesive surfaces were also used to simulate through-coating cracks [145] for brittle coatings. The initiation of a through coating crack can be easily seen on a kink on the plot of the load against displacement. The load drop is larger for cracks that initiated at larger radii because the energy released by a crack is proportional to its surface area. Cracks in the coating initiate on the surface and almost immediately travels through the coating, stopping before the interface because of compressive stresses.

Hu et al. [146, 147] studied interface behavior during spherical indentation tests. Unlike other papers found on the topic, which lacked experimental verification, the authors verified their finite element model. The authors used parameters from an experimental analysis conducted by Huang et al. [148] and compared delamination as a function of load and coating thickness. Good overall trend agreement was observed, though the average relative difference was 170%. Given the complexity of the system, the authors recognized this as good agreement, citing the work of
Chai and Lawn [149] where the relative difference between simulations and experiments was as large as a factor of three and considered acceptable. Hu et al. [146], in both their verification and modeling phases, assumed that the interface traction-separation properties could be derived from the plane-strain stress intensity factor and failure strength of the weaker component of the system. The authors used the Xu and Needleman model for the cohesive zone. The Mode I critical stress intensity factor is related to the normal work of separation by Equation (2.76):

\[ K_{lc} = \frac{E\phi_n}{\sqrt{1 - \nu^2}} \quad (2.76) \]

where \( K_{lc} \) is the Mode I critical stress intensity factor, a material property, \( E \) is the elastic modulus, \( \nu \) is the Poisson’s ratio, \( \phi_n \) is the normal work of separation. The work of separation is given in Equation (2.77):

\[ \phi_n = e\sigma_{max}\delta_n \quad (2.77) \]

where \( e \) is Euler’s number, \( \sigma_{max} \) is the normal strength, and \( \delta_n \) is the characteristic length. The authors assumed the shear and normal work of separation were equivalent. The shear work of separation is the same as Equation (2.77), except Euler’s number is replaced by \( (e/2)^{0.5} \) and the normal subscripts are replaced with shear subscripts. In addition the upper bound estimate of the shear strength, \( \tau_{max} = \sigma_{max}/3^{0.5} \).

The authors found that the delamination area decreased as a function of coating elasticity and thickness, and increased as a function of compressive residual stress. The coating with the
greater elastic modulus experienced less deformation, thus reducing substrate plastic deformation, and delamination. As the coating thickness increases, the delamination area decreases due to augmented plastic deformation. The compressive residual stresses contribute to delamination largely because of the interdependence between fracture modes.

Similar research has been completed for conical indenters. Li and Siegmund [150] conducted one such analysis that produced some relevant observations. The authors cited the significance of the cohesive energy, noting that increasing the cohesive energy required a larger indentation depth to produce delamination and that the higher cohesive energy reduced the crack growth rate. The authors noted that initial debonding was a mixed mode process culminating in a buckling process by which the coating lifts off the surface. As the indentation depth increases, the crack tip transitions to Mode I conditions. Similar modeling efforts using cohesive zone elements to simulate delamination have been conducted by Zhang et al. [100, 102, 151] and produced similar observations.

2.12.8 Fracture Energy as a Function of Phase Angle

Debonding at an interface is often complicated by contributions of more than one crack opening mode, while cracks growing in an isotropic material will grow in the direction that maintains Mode I loading conditions. Depending on the toughness of the materials on either side of the interface, significant contributions of shear loading can occur. The test specimens mentioned above often result in a mixed mode conditions where the phase angle, a measure of the contributions of Modes I and II, is between 0 and 90 degrees. The phase angle is defined in Equation (2.78).
\[ \Psi = \tan^{-1}\left(\frac{K_{II}}{K_I}\right) \]  

Mode I and II stress intensity factors are \( K_I \) and \( K_{II} \), respectively. Pure Mode I loading occurs when \( \Psi=0^\circ \), and pure Mode II loading occurs when \( \Psi=90^\circ \). A major concern of the mode mixity during interface crack growth is the measurement of the critical fracture energy. Friction contributions, at and directly behind the crack tip, caused by shear loading result in an increase in the critical fracture energy as the phase angle increases and more energy is dissipated by frictional contact. As the roughness of the interacting surfaces, characterized by the parameter \( \lambda \) in the equations below, increases, so does the frictional energy dissipated. Three relationships for the mixed mode fracture toughness are given in Equations (2.79) through (2.81) by Hutchinson and Suo [116].

\[ \Gamma(\Psi) = G_k \left[ 1 + (\lambda - 1)\sin^2 \Psi \right] \]  

\[ \Gamma(\Psi) = G_k \left[ 1 + \tan^2 [(1 - \lambda)\Psi] \right] \]  

\[ \Gamma(\Psi) = G_k \left[ 1 + (1 - \lambda)\tan^2 \Psi \right] \]

In the above equations, the case of an ideally brittle interface is characterized by \( \lambda=1 \), while the limit for mode mixity is given by \( \lambda=0 \). According to Equation (2.80) and (2.81) the toughness increases sharply as mode mix ratio approaches pure Mode II loading; Equation (2.81) is unbounded as the loading becomes pure mode two. However, Equation (2.79) levels off at pure
Mode II loading. The above equations suggest symmetric behavior as a function of phase angle, though this is not always the case. Hutchinson and Suo [116] provide plots of the fracture toughness as a function of phase angles for several different values of $\lambda$.

Several different mechanisms contribute to increased fracture resistance as the contribution of Mode II fracture increases, such as plasticity, crack front deflections, and crack shielding. Typical interfaces are non-planar and the crack surface contacts at undulations and facets along the interacting surfaces act to shield the crack tip, increasing fracture energy at large phase angles. Evans and Hutchinson [152] developed a model that examines contact at undulations and addresses the interactions of the two surfaces in the contact zone behind the crack tip. The geometry of the proposed non-planar interface is shown in Figure 2.3.

![Figure 2.3: Illustration of assumed interface geometry of crack tip.](image)

The authors provided an analysis for crack shielding by simulating the microcracks with the continuous linear spring model. The linear spring model has been solved by Budiansky et al. [153], who provided the following relevant result:
where $K_{II}$ is the remote Mode II stress intensity factor and $K'_{II}$ is the crack tip Mode II stress intensity factor. The function $k(\alpha)$ is given in Table 1 of the reference (as $1/\lambda$). With the continuous linear spring model, Hutchinson and Evans developed an expression for crack shielding:

$$\frac{\Gamma(\Psi) - G_{k}}{G_{k}} = \frac{\tan^2 \Psi \left\{ 1 - k \left[ \alpha_0 \left( 1 + \tan^2 \Psi \left( \frac{\Gamma(\Psi) - G_{k}}{G_{k}} + 1 \right) \right) \right] \right\}}{1 + \tan^2 \Psi}$$

where the function $k(\alpha)$ was described above, $\alpha_0$ is defined in Equation (2.84), and all other variables are as defined earlier.

$$\alpha_0 = \frac{\pi \left( EH^2 / 4G_{k} \right)}{32 \left( 1 - \nu^2 \ln \left[ 1 / \sin \left( \pi D / 2l \right) \right] \right)}$$

All parameters are as defined earlier. Based on the analysis, the authors concluded that two regimes of $\alpha_0$ exist: $\alpha_0 \geq 1$ and $\alpha_0 \leq 10^{-3}$. For the condition of maximum cracking shielding, Equation (2.83) can be reduced to Equation (2.85).

$$\frac{\Gamma(\Psi) - G_{k}}{G_{k}} = \frac{\tan^2 \Psi}{1 + \tan^2 \Psi}$$
Alternatively, in the first regime, there is essentially no shielding at large phase angle.

The authors designated a material parameter, $\chi = EH/G_{IC}$, which governs the transition between the above mentioned extremes. The transition region occurs over a relatively small range of $\chi$, between approximately 10 and $10^{-2}$. Consequently, a large value of $H$ or a small value of $G_{IC}$ can result in mixed mode fracture resistance, governed by Equation (2.81) with $\lambda = 0$. Alternatively, when the parameter is below $10^{-2}$ the critical fracture energy is generally not a function of the phase angle. Though the analysis does not account for elastic mismatch across the interface, it does provide a mechanism for quantifying the roughness of an interface and compares reasonably well with experimental results.

2.13 Thermal Gap Conductance

Crack initiation and growth at a bimaterial interface has been known to have significant effects on the thermal conductance across that interface. Thermal conductance across an interfacial crack is important, as it contributes to the thermal gradient at the crack tip, and the resulting thermal stresses at the crack tip. Recognizing that the stresses at the crack tip will drive crack growth, an accurate approximation of the conductance across an interface crack is necessary to model crack growth from thermal loading. Cooper et al. [154] analyzed the thermal contact conductance across a rough, conforming interface, while Yovanovich et al. [155] compiled solutions to several other interface heat transfer conditions, including heat conduction across large parallel plates.
Yovanovich et al. [155] solved the problem of gap conductance between two parallel plates. The infinite plate assumption applies for cases where the distance between the plates is much greater than the amplitude of the surface roughness. Such an assumption is reasonable away from the crack tip for interfaces not in contact. For cases where the faces are in contact, the gap conductance simplifies to the limiting case of maximum conductivity. Assuming that the gap between the plates is filled with a stationary diatomic gas, the gap conductance for two parallel plates is given in Equation (2.86).

$$h_g = \frac{k_g}{d + M}$$  \hspace{0.5cm} (2.86)

where $k_g$ is the thermal conductivity of the interfacial gas, $d$ is the distance between the surfaces, and $M$ is defined below.

$$M = \left( \frac{2 - \alpha_1}{\alpha_1} + \frac{2 - \alpha_2}{\alpha_2} \right) \frac{2\gamma}{(\gamma + 1)Pr} \Lambda$$  \hspace{0.5cm} (2.87)

where $\alpha_1$ and $\alpha_2$ are the accommodation coefficients, $\gamma$ is the ratio of the specific heats, $\Lambda$ is the molecular mean free path of the gas, and $Pr$ is the Prandtl number. A perhaps more useful and insightful equation for $M$ was provided by Song et al. [156].

$$M = M_0 \frac{T}{T_0} \frac{P_{g,0}}{P_g}$$  \hspace{0.5cm} (2.88)
where $T$ and $P_g$ are the temperature and pressure of the gas, respectively, $M_0$ is the gas parameter value at the reference values of gas temperature and pressure, $T_0$ and $P_{g,0}$. The thermal conductivity for air is $k_g=0.026$ W/mK, while the gas parameter at $T_0=50$ °C and $P_{g,0}=1$ atm is $M_0=0.373\times10^6$ m. The authors found good agreement with the model and experiments done for which the gap was filled with helium, argon, and nitrogen gases.
CHAPTER 3. PROBLEM STATEMENT

3.1 Objective

The present research investigated the feasibility of using an experiment wherein a ball indentation is made in a coated sample surface to evaluate the mechanical properties of the interface. Those properties will then be applied to a thermal-structural model to study the evolution of an indentation-induced flaw.

3.2 Technical Background

The technique of using a combination of numerical and experimental methods to evaluate the cohesive zone properties of a coated specimen was proposed, with the ultimate goal of simulating the response of an interfacial flaw during the application of thermal and pressure transients to the coating surface. A proposed concept takes its cue from the ball indentation test’s application for the interfacial strength measurement of transparent coatings. Cohesive strength was measured based on observations of the crack front as a function of the applied load. It was expected that this crack size as a function of the maximum load was also a function of the critical energy release rate, such that a unique combination of material properties governing the growth of the interfacial crack resulted. The method can accurately predict the responses of the coating separation mechanism. Therefore, it was expected that the method would also be capable of accurately applying the same properties to numerically predicting the response of properties in a second test that would extend the crack by different mechanisms. Thus, validation for the technique would be provided. Given the validation of the cohesive zone, and its application in the material set, the effects of surface thermal and pressure transients on flaw evolution could then be studied.
3.3 Experimental Procedure

The following theoretical and experimental studies will be conducted in order to develop the technique of material property measurement and to apply those properties to studying interfacial flaw evolution.

1. A preliminary analysis of the stress state at the tip of a through coating crack will be analyzed with a thermal-structural analysis of gun tube coatings. The effects of various material parameters will be analyzed in terms of the stress state at the crack tip.

2. In order to evaluate the response of an interfacial flaw, a method for evaluating the cohesive zone properties of the coating–substrate interface must be developed. How best to measure cohesive zone properties is an area of research under debate, as techniques of evaluating individual property components and combining them to form an accurate description of the interface are still being developed. Most researchers maintain that the strength measurements taken in button-type tests are not representative of the entire interface. Therefore, a method that measures all the properties on the same sample, thereby presenting a complete description of the interface, is preferred. It should be recognized, too, that as a function of the cohesive zone properties, the indentation test introduces an interfacial flaw and also provides a context for measuring the resulting flaw evolution under thermal and pressure transients. Therefore, it is necessary to develop a model to determine the properties of the cohesive zone based on experimental results that include the evolution of the flaw size at different indentation loads, as determined by ultrasonic analysis after the indentation test has been completed.

3. As stated previously, the experimentally evaluated properties were used in a simulation of the indentation experiment, first to evaluate the properties, and then to introduce damage for
further study. A comprehensive finite element simulation will be developed, for which results obtained from a different finite element simulation of an indentation experiment simulation will be imported as the initial conditions for considering the evolution of an interfacial flaw to severe thermal and pressure transients. The contributions of the individual boundary conditions will be explored in order to evaluate current experimental techniques, as well as improvements in coating performance. As a last step, the damage mechanisms of the final gun tube coating will be explored in depth, as the internal blister pressure is delayed relative to the surface.

4. Conclusions will be drawn from the experimental and numerical results. Recommendations for improving the experimental and theoretical protocols presented herein will also be offered.
CHAPTER 4. PROPERTY MEASUREMENT AND VALIDATION

4.1 Experimental Procedures

4.1.1 Sample Preparation

A first set of samples tested comprised a nickel-aluminum coating thermally sprayed onto steel substrates. Samples were prepared by Michael Schroeder of Metallic Bonds, Ltd., in Beloit, WI, by spraying onto 0.14 m (5.5 in) square steel substrates. The plates were then machined into the appropriate samples for testing. For the indentation tests, the samples were machined into squares, approximately 2.5 mm, in order to better simulate axisymmetric conditions. The four-point bend samples were machined to the same thickness and width dimensions, but were 11.43 mm long.

A second set of samples was prepared from 7075 aluminum sprayed onto 7075 aluminum substrates with T6 heat treatment. The four-point bend samples were machined to the same length and width dimensions as the nickel-coated steel, whereas the indentation tests were performed on samples of various coating thickness dimensions.

4.1.1.1 Substrate Preparation

The cold-spray substrate was a 1 foot (0.305 m) and 1/8 inch (3.18 mm) thick square plate with polished surfaces that was ordered from McMaster-Carr. Subsequently, the top surface was lightly sanded in order to facilitate better bonding with the cold-spray coating. Not only did
sanding remove most of the surface oxidation and contaminants that could otherwise adversely affect bonding, it also created a slightly irregular surface, which by increasing surface area, improved bonding. However, even with sanding, it is likely that some contaminants remain on the interface where they may have a negative effect on bonding.

4.1.1.2 Coating Deposition

Aluminum 7075 powder was used for the cold spraying. A -325 μm mesh powder was chosen because the particle size and distribution facilitate cold spraying. Powder 7075 aluminum was purchased from Valimet, Inc., and was dried before being applied. The cold spray application was performed by the Applied Research Lab (ARL) at the Pennsylvania State University.

4.1.1.3 Four-Point Bend Sample Preparation

4.1.1.3.1 Thermally Sprayed Nickel on Steel

After the coatings had been applied, the plates were machined into four-point bend test samples with approximate measurements of 0.020 m (0.79 in) wide and 0.11 m (4.5 in) long. To mitigate friction between the coating and the load points, the surface was ground in order to achieve a smoother finish, resulting in a final thickness of 0.60 mm. For the samples in this study, the coating was notched at the center across the width with a wire EDM (electric discharge machining) to the interface. In recognition of the fact that the four-point bend test evaluates the critical energy release rate based on the change in compliance of the beam as the crack grows, the substrate was machined down to a thickness of 2.6 mm with the wire EDM. In addition, the samples were oriented on the plate to mitigate the effects of residual stresses on crack growth by aligning the samples perpendicular to the direction of the curvature. Visual inspection indicated
that the residual stresses were primarily uniaxial based on slight curvatures in one direction of the plate. As this was the case, the samples were oriented perpendicular to the direction of the residual stresses so that the contribution of the stresses to crack growth, and ultimately to the energy release rate would be minimal. Six initial samples were produced from the original substrate, from which the remaining metal was saved for subsequent indentation tests.

4.1.1.3.2 Cold-Sprayed Aluminum on Aluminum Substrates

In order to produce a unique set of samples, with properties that would differ from the nickel-coated steel described above, aluminum beams were also produced by cold spraying. A cold-spray powder of 7075 with a -325 mesh was sprayed onto a 7075 aluminum plate that was 1/16" (0.00152 m) thick. Two coatings were sprayed; one equal to the thickness of the substrate, and the other equal to half the thickness of the substrate. Several passes were required to build up the appropriate coating thickness. To prepare the surface for cold spraying, it was lightly sanded to remove oxidation and contaminants. Helium was used as the carrier gas, because nitrogen cannot achieve the speeds required for deposition. The nozzle temperatures for the spraying varied from approximately 180 to 195°C, and the gas pressure for the runs was approximately 2.07 MPa (300 psi). The coating was sprayed in passes along the length of the sample. Due to surface roughness, and in order to remove surface oxidation, the samples were sanded with an emery cloth, resulting in a smoother surface finish for reduced friction between the sample and the loading fixture. The samples were machined into four-point bend bars, and a notch was milled into the center with a 1/32" mill bit. The four-point bend samples were 10 mm wide and 114 mm long, and the final thickness of the coating was 1.5 mm.

4.1.1.4 Indentation Sample Preparation
4.1.1.4.1 Thermally Sprayed Nickel on Steel

The indentation tests were planned in order to meet the ultimate goal of simulating the experiment with a finite element model as already described in Section 4.2.5. Therefore, the samples and experiment were designed to best accommodate the assumptions of the model. The nickel-coated steel samples were cut into squares to create loading conditions accurately described by the axisymmetric indentation finite element model. Indentation samples were cut from the 10 mm by 115 mm four-point bend samples into 10 mm squares. In addition, curvature resulting from residual stresses along the length of the sample was easily observable. During testing, any flattening of the curvature caused by the residual stress would have created a uniaxial stress in the direction of the length of the sample. To mitigate such stress, the rectangular plate was cut into squares.

4.1.1.4.2 Cold-Sprayed Aluminum on Aluminum Substrates

Several different indenters were simulated on the aluminum samples with a coating thickness of 1.5 mm (Section 4.1.1.3) with the finite element model (Section 4.2.5), and based on the results, it became clear that this coating was too thick to produce the interfacial stresses required to induce delaminations at the interface even for very weak bonds. Based on this preliminary analysis, the samples described in Section 4.1.1.3 were repurposed for the indentation test after some of the coating had been removed to produce samples more suitable for indentation testing. The coating was milled down to a thickness of between 0.5 and 0.75 mm; this variation is a function of the residual stress curvature that made it impossible to create a coating surface that was completely parallel to the interface. For each indentation test, the coating thickness at the indentation site was recorded.
4.1.2 The Four-point Bend Test

As described in Section 2.12.5, the four-point bend test can be used to evaluate the interfacial fracture energy between coatings and substrates. The calculation of the critical energy release rate based on the four-point bend experiments requires accurate elastic modulus and Poisson’s ratio information for the coating and substrate, in addition to the residual stress state characterization. Additionally, four-point bend tests require that the coated specimen be pre-cracked and inserted into a fixture with an outer load span that supports the sample and an inner load span that bends the sample to progressively separate the coating from the substrate. From the recorded load-displacement measurements, the critical load can be identified as the plateau and used to calculate the critical energy release rate. However, the four-point bend test, as described here, presents several significant challenges, which make it impractical for all but a select few material systems. In fact, the systems that can be evaluated must have a relatively low-modulus and high-yield strength coatings and substrates, such that before yielding delamination occurs. Additionally, once crack growth begins, it must continue steadily in order to determine the load at which stable crack growth occurs for the evaluation of the critical energy release rate.

The aluminum samples described above constitute a challenging material combination for cohesive zone property measurement. Based on previous experience with cold spraying, it was expected the deposited coating system would form a strong bond and yet exhibit brittle interfacial fracture characteristics. Therefore, stable crack growth would not occur during steady-state conditions, even if it were possible to introduce a pre-crack. The particular material used herein highlights one of the shortcomings of the four-point bend test in calculating the critical energy release rate: i.e., the method only works in cases where a pre-crack can be introduced and grows as a function of the applied load. To overcome these obstacles, modifications were made
to the four-point bend test experiment. The sample was directly loaded in the four-point bend fixture, without first attempting to introduce a pre-crack. Loading was applied at a constant rate of displacement. The sample was then loaded until a crack forms that runs from the center of the sample to the load points. After the test was completed, the results rather than being used for analytical calculations, were be used to numerically determine the cohesive zone parameters with the finite element model described in Section 4.2.4.

### 4.1.2.1 Residual Stress Measurement

In order to calculate the residual stresses added during the coating deposition, the curvatures of the composite beam and the components after separation must be measured. Curvature measurements of the composite beam, coating, and substrate were recorded using a contact profilometer that required the samples to be completely separate in order for the residual stresses to be evaluated. Curvatures were taken on the coated side of the composite specimen and on the tops of the separated components. The surfaces of the specimens were smooth and continuous, such that the measurements produced a good overall representation of their curvatures resulting from residual stresses with three points, which were then fit with a radius of curvature for residual stress calculations.

A Tencor P-10 Surface Profilometer was used for the measurements. As curvature measurements do not rely on surface roughness details, a fast scan speed and low sampling rate, 1000 μm/s and 50 Hz, respectively, were used. The range of the profilometer (the maximum measurable distance between the highest and lowest features in a scan) was 1048 μm, and the range of the stylus radius was 20 μm.
The curvatures of the sample were measured before fracture and those of the coating and substrate after fracture. Then, the linear residual stress distribution was approximated using Equations (2.41) and (2.42) based on the radius of the composite beam curvature before fracture, and the coating and substrate after.

4.1.2.2 Mechanical Testing

The specimens were prepared for testing by first cutting through the coating to create a notch. For the nickel-coated steel specimens, the notch was created with a wire EDM, and for the aluminum samples with a 1/32” thick mill bit. As already mentioned, the aluminum samples described above provide a challenging material combination for cohesive zone property measurement. Previous experience with cold spraying suggests that the deposited coating system would form a strong bond and have brittle crack growth characteristics. Based on these expectations, the system would not provide the opportunity to create a precrack. In the unlikely event that a precrack were to be introduced, the system would not allow for steady-state crack growth. This particular material set highlights one of the shortcomings of the four-point bend test in terms of calculating the critical energy release rate; the method only works in cases where a pre-crack can be introduced and then grow as a function of the applied load without yielding. To overcome these obstacles, modifications were made to the four-point bend test experiment; the test was loaded in the four-point bend fixture without first attempting to introduce a pre-crack. Loading was applied with a mechanical loading frame (Instron 5866, Instron Corp., Norwood, MA), and the load and displacement data recorded. The sample was loaded until a crack, which was expected to grow instantaneously, formed. After the test was completed, the results were not used for analytical calculations; instead, they were applied to determine the cohesive zone parameters numerically with the finite element model described above.
Specimens were prepared for four-point bend testing by creating a right-angle notch by first milling through the coating to the interface with a 1/32” mill bit. A mill was used to avoid any possible thermal effects associated with the wire EDM at the interface. However, the nickel coatings were notched with a wire EDM as the thermal effects at the crack tip were not first considered. The inner and outer span lengths of the test fixture were completely adjustable. A floating plate, which allowed for self-alignment, was used to aid symmetric crack growth. The fixture span inner and outer dimensions maintained the preferred 2:1 ratio, with support and load dimensions of 110 and 55 mm, respectively. The rollers were made of steel and each had a diameter of 6.35 mm. The fixture was placed in an Instron tensile tester with a 10 kN load cell, and in order to simulate quasi-static loading conditions the load was applied at a rate of 1 mm/min. The samples were carefully aligned in the fixture to ensure that the notch would be both centered and parallel to the load and support fixtures. The output was recorded in an Excel workbook for subsequent analysis.

In order to calculate the residual stresses that arose during coating deposition, the composite beam’s curvature must be measured before the test, and the coating and substrate after they are separated. Curvature measurements were recorded using a contact profilometer, as described above.

4.1.3 The Ball Indentation Test

In addition to measuring the geometry and material parameters, ball indentation tests are often used to calculate the critical energy release rate, which is a function of the radius of the
debonded region under the indenter. However, because the test requires the debonded region to be measured as a function of the applied load, it is difficult to apply the method to opaque coatings as an in situ determination of the radius is impossible. Despite this limitation, the potential of the ball indentation test to obtain interfacial properties has been recognized, as the onset of debonding and debonded area are strong functions of the cohesive zone properties. Hence, the indenter load-displacement output, the load at which the interface begins to separate, and the final crack dimensions can be compared to the finite element simulations of the indentation test in order to determine the cohesive zone properties.

Traditional techniques for measuring interfacial properties have several disadvantages that make them unsuitable for this application. As mentioned above, the four-point bend test showed large-scale yielding before damage initiation in the case of the nickel-coated steel set, or instantaneous failure in the case of the aluminum samples. This made initiating a pre-crack and subsequent steady state crack growth impossible in both cases. Other tests, such as the scratch test, are more qualitative in nature, whereas peel and wedge tests, are difficult to perform on thin coatings. Given these deficiencies, a new approach, based on the indentation test was developed to evaluate the cohesive zone properties of the coating and substrate systems.

As such, a new method to measure the cohesive zone properties using indentation tests will be based on a numerical and experimental approach to the finite element model. Indentation experiments will be conducted with a mechanical loading frame (Instron 5866, Instron Corp., Norwood, MA) capable of applying loads from 1N to 10kN. However, only the 10 kN load cell was used for the indentation experiments in the present study. A tungsten-carbide indenter ball was placed between the ram and the sample surface. Based on how likely they were to cause damage at the interface as suggested by preliminary finite element modeling, several different
indenter sizes were selected and tried. During the tests, the ball was quasistatically pressed into the coating surface at a rate of 0.5 mm/min, during which the acoustic emissions were monitored. Loading was applied until the desired load or displacement was achieved, after which the loading was gradually manually. As described in the next section, acoustic emissions were recorded to provide information about the initiation of the debonding in situ and post-test c-scan images were used to determine the extent of the delamination, quantified by the inner and outer radii of the delamination zone.

4.1.3.1 Acoustic Emission Monitoring

Acoustic emissions were monitored during the test to determine when debonding commenced. An acoustic emission sensor (Micro 80, Physical Acoustic Corp., Princeton Junction, NJ) was placed on the sample. Since the tests required that the sensor be moved frequently from sample to sample, it was necessary to ensure that it was not damaged. For this purpose, instead of glue, honey was used to couple the sensor to the sample surface. The sensor was held in contact with the sample or stage surface using a c-clamp. After passing through a 20 dB gain amplifier, the acoustic emissions were monitored with the Mistras 2001 system (Physical Acoustic Corp., Princeton Junction, NJ). A NI-DAC system, which is Dos-based, was used to record the acoustic emission results. For each test, the cumulative energy amplitude, the number of hits, and the energy of each event were recorded at a frequency of 4 MHz. The threshold, which was adjustable, was set to 37 dB after preliminary experimentation. It was found that higher thresholds withheld important high-energy events, whereas lower values also missed big events. Though it seems counterintuitive for a lower threshold to result in fewer events, the system actually hits a “lockout” time. At the lower threshold, the system detects a lower threshold hit and by the time the channel is available for the next hit, a larger energy event has been missed. Such a condition is a common issue, and can only be avoided by carefully selecting appropriate
thresholds. The location of the acoustic emission sensor is shown in Figure 4.1b. As previous experience had shown that a second acoustic emission sensor mounted onto a loading ram provides redundant information; only one such sensor was used in these experiments.

Figure 4.1: Indentation experiment arrangement, showing (a) the load frame, computer, and stage, and (b) a close-up of the stage and specimen arrangement.
4.1.3.2 Indenter Selection

Special care was taken to select an appropriate indenter. If the chosen indenter is too small, it will cause excessive plastic deformation and penetrate the coating without creating the interfacial stresses necessary to cause separation. On the other hand, an indenter that is too large will require a large load to deform the surface and may not induce delamination for thin samples. As the goal of the experiment was to induce delamination at the interface, numerical simulations were run to aid in the selection of an indenter with an appropriate diameter. The first requirement of the chosen indenter is that it must induce delamination before the load cell limit of 10 kN was reached. In addition, the goal of introducing damage at a load within the acceptable range of the load cell must be balanced with minimizing plastic deformation, because large-scale deformation introduces numerical difficulties. In addition, accurate material property information for large plastic deformations is difficult to obtain.

The experiment was designed for the specific purpose of measuring the interface properties; therefore, the indenter selection process was complicated by the fact that these were not known. The material properties for similar systems were assumed based on published research and varied within reasonable limits to approximate how the coating system might delaminate under indentation. Based on the preliminary simulations, an indenter diameter of 2 mm was chosen for the first set of experiments on the nickel-coated steel system. As already discussed, indenter selection was seen as a compromise between excessive deformation and the maximum load level to initiate delamination.
4.1.3.3 Indentation Testing

As discussed earlier, the indentation test was used in conjunction with numerical analysis to determine the properties of the interface in the coating systems. Indentation tests were performed at a constant loading rate of 0.05 mm/min. A 10 kN load cell was attached to the load frame to allow the large loads required to initiate delamination in even strong coatings. A ram extension was attached to the load cell to provide a flat surface for impressing the indenter ball into the coating surface. Plastic deformation occurred during the first indentation in the ram extension. For subsequent indentations, the indenter ball was placed in the groove formed during the first sequence to minimize the contributions of the loading ram plastic deformation to the load and displacement measurements. The experimental set-up is shown in Figure 4.1

Before each indentation experiment, an acoustic emission sensor was placed on the end of the sample. Honey was used as a coupling medium, as it is an excellent conductor of acoustic energy. A c-clamp was used to hold the sensor to the surface and was clamped over the stage, sample, and sensor. However, the nickel-coated steel samples are too small to fit under the indenter when the sensor is attached to the sample. For these tests, the sensor was attached to the stage, again with honey as the coupling fluid. Mineral oil was placed between the bottom of the stage and the sample to facilitate the transmission of acoustic emissions from the sample, to the stage, and then to the sensor. Such an arrangement is not ideal, as attaching the sensor to the stage may mean the sensor picks up acoustic noise from the load frame that would otherwise be reduced if it were attached to the sample. In addition, the acoustic emissions from the sample may be damped, as they must now travel through the stage before being received by the sensor. However, this was the only way to monitor acoustic emissions in the sample.
After the load cell had been balanced and the gage length reset, the indenter ball was placed in the grove on the load ram above the sample and then slowly moved towards the sample surface until the load began to increase. Once the contact was confirmed, manual loading was stopped and the BlueHill software controlling the Instron was engaged. The software was programmed to load the same at a constant rate. In addition, the acoustic emission monitoring system was engaged to provide data to match the load and displacement measurements. After the desired load and/or displacement were achieved, the loading and acoustic emissions monitoring were stopped and the sample was slowly unloaded manually. Once all the data based on the measurements had been saved, the samples were labeled, and saved for subsequent c-scan analysis.

For later tests, it was decided that the acoustic emissions should also be recorded during unloading. In order to do this, the setpoint of the sample load frame was adjusted over a time of 100 seconds to slowly unload the sample back to its original position. During the unloading, acoustic emissions were recorded as a function of time, which would later be correlated with a load level based on information about the distance the ram must travel to reach the setpoint and the 100 s that it would take to get there. One hundred seconds was the maximum time allowed to reach the setpoint because this was the limit of the Instron control software, and though it varied by the extent to which the indenter was displaced, it was faster than the loading rate.

4.1.3.4 C-Scan and Scanning Acoustic Microscope Imaging of Interfacial Defects

After being indented, the sample interface was imaged by ultrasonic c-scan in order to find areas where the coating and substrate had separated. During c-scan inspection using an Okos Hydra scanner (Okos Solutions, LLC, Chantilly, VA) the sample was placed in a tank of water, which
carried the ultrasonic signal from the transducer to the sample. To monitor the reflections on ultrasonic energy reflected from the sample, the c-scan system was used in pulse-echo mode, for which the transducer was also the receiver. As is the case with c-scan imaging, and especially for composite samples, special care must be taken when analyzing the signal returned to the transducer. At every discontinuity in the sample such as a flaw, delamination, or interface, portions of the signal are both reflected and transmitted. The signal, which originates at the transducer travels to the surface, where some of the signal is reflected directly back to the transducer, and the rest is transmitted into the sample. After traveling into the sample, the energy reaches the interface, at which point it is again reflected and transmitted. Reflection and transmission repeats every time the energy reaches an interface, resulting in multiple signals separated in time being returned to the transducer. The signal returned to the transducer can be interpreted based on the time that it takes different parts of the signal to return. An equation for the time it takes for a signal to pass through a composite sample, to reflect from a surface, and to return to the transducer is given below:

$$\Delta t = \frac{2\Delta x_w}{v_w} + \frac{2\Delta x_1}{v_1} + \frac{2\Delta x_2}{v_2} + \ldots$$

(4.1)

where $\Delta t$ is the time it takes for the wave to travel through the sample, $v_w$ is the wave speed of sound in water, $\Delta x_w$ is the distance between the sample and the transducer, $v_1$ is the wave speed through the first layer of the sample, $\Delta x_1$ is the thickness of the first layer of the sample, $v_2$ is the wave speed through the second layer of the sample, $\Delta x_2$ is the thickness of the second layer of the sample, and where more terms can be added if required for a sample with more than two layers.
To use Equation (4.2), it is necessary to first choose an interface of interest. As the terms for the materials between the transducer and the chosen interface must be included in Equation (4.2), it is possible to determine the time at which to expect the portion of the signal for the reflection. Based on this determination, gates can be added to the signal analysis software, Odis version 3.11.15 software (Okos Solutions, LLC, Chantilly, VA), to capture the amplitude and time of flight information of the portion of the signal during the scan. A gate was included in every measurement, to record amplitude over a few different portions of the sample. The first interfacial reflection was measured, as was the first back-wall reflection (when apparent) as was the second interface reflection and perhaps other portions of the signal as deemed necessary. Because of the curvature of the sample, the signal will arrive at slightly different times at different ends of the sample. In order to keep the gate on the correct part of the signal, a follower gate was added to ensure that the other gates would measure the correct portion of the signal as the arrival time changes with variations in sample curvature. The signal amplitude can then be plotted based on a custom color palette, a black-and-white scheme, or grayscale. In addition, when a particular part of the signal is of interest, for example the back-wall reflection, the energy of the transducer can be focused on that portion of the sample to maximize the amplitude of that portion of the signal and so to create a greater contrast between the delaminations and the undamaged interface.

4.2 Modelling

4.2.1 Introduction to Cohesive Zone Elements

The cohesive zone model (CZM) was developed as an alternative to the singularity-driven fracture approach, where the crack tip is assumed to be infinitely sharp, producing a singularity in the stress field. However, neither condition exists in most conditions. In addition to providing
an alternative to an approach based on fictive conditions, CZM can easily be implemented in finite element codes. Several popular codes, such as ANSYS [157] and ABQUS [158], include cohesive zone elements.

Several cohesive zone models have been developed. The main difference between them is the shape of their respective traction-displacement response curves. All the models can be described in reference to two parameters: cohesive energy ($\phi$) and cohesive strength, ($\sigma_{max}$) or in place of the latter, maximum separation displacement ($u_n^c$). The model used both in ANSYS [157] and in this research is a bilinear model with linear softening that was developed based on the Gaubelle and Bayler model [159]. The shear and normal traction displacement curves in modes I, II and, III, are shown in Figure 4.1.

![Figure 4.1: Shear and normal contact separation curve for loading on (a) Mode I and (b) Mode II.](image)

While the following analysis is based on Mode I debonding, everything applies to Modes II and III if the normal parameters are replaced with shear parameters. In Figure 4.2, $\sigma_n$ is the normal
tensile contact stress, $\sigma$ is the normal tensile contact stress, $K_n$ is the normal contact stiffness, $\delta_c^n$ is the normal contact gap at the completion of debonding, $\delta_0^n$ is the separation at the maximum normal contact tensile stress, and $d$ is the debonding parameter. The traction-separation for the debonding in Mode I and in Mode II can be written as Equation (4.2) and Equation (4.4), respectively.

\[
\sigma_n = K_n \delta^n (1 - d) \text{ for } 0 \leq \delta^n \leq \delta_0^n 
\]

\[
\sigma_n = \frac{K_n \delta_0^n}{\delta_c^n - \delta_0^n} (1 - \delta^n) \text{ for } \delta_0^n \leq \delta^n \leq \delta_c^n 
\]

\[
\sigma_s = K_s \delta^s (1 - d) \text{ for } 0 \leq \delta^s \leq \delta_0^s 
\]

\[
\sigma_s = \frac{K_s \delta_0^s}{\delta_c^s - \delta_0^s} (1 - \delta^s) \text{ for } \delta_0^s \leq \delta^s \leq \delta_c^s 
\]

The debonding parameter $d$, is a measure of the debonding progress; it is zero at Point 1 and one at Point 3, by definition. The slope of the line between Points 1 and 2 is the interface stiffness, $K_n$. If the loading is stopped and removed at any point on the line between Points 1 and 3, the slope when the load is reapplied is $K_n(1-d_n)$. The damage parameter is given below.

\[
d_n = \frac{\delta_c^n (\delta_{\text{max}}^n - \delta_0^n)}{\delta_{\text{max}}^n (\delta_c^n - \delta_0^n)} 
\]
where the variables are as earlier defined. Additionally, $\delta_{\text{max}}^n$ is the maximum separation experienced by the interface element over the previous loading history. An interface element is considered to be fractured when the following conditions are met: $\delta^n \geq \delta_c^n$, $d_n = 1$, and $\sigma = 0$. A similar damage parameter can be developed for Mode II, for which the normal superscripts in Equation (4.6) are replaced by shear parameters.

Several criteria are available to account for mode-mixity in terms of damage initiation and final fracture. These include criteria based on a quadratic nominal stress or strain and criteria based on the maximum nominal stress or strain, as shown by Equations (4.7) through (4.10).

\[
\text{MAX} \left[ \frac{\sigma_n}{N_{\text{max}}} , \frac{\sigma_s}{S_{\text{max}}} , \frac{\sigma_t}{T_{\text{max}}} \right] = 1
\]  
(4.7)

\[
\text{MAX} \left[ \frac{\varepsilon_n}{\varepsilon_{\text{max}}} , \frac{\varepsilon_s}{\varepsilon_{\text{max}}} , \frac{\varepsilon_t}{\varepsilon_{\text{max}}} \right] = 1
\]  
(4.8)

\[
\left( \frac{\sigma_n}{N_{\text{max}}} \right)^2 + \left( \frac{\sigma_s}{S_{\text{max}}} \right)^2 + \left( \frac{\sigma_t}{T_{\text{max}}} \right)^2 = 1
\]  
(4.9)

\[
\left( \frac{\varepsilon_n}{\varepsilon_{\text{max}}} \right)^2 + \left( \frac{\varepsilon_s}{\varepsilon_{\text{max}}} \right)^2 + \left( \frac{\varepsilon_t}{\varepsilon_{\text{max}}} \right)^2 = 1
\]  
(4.10)
Here, $\sigma_n, \sigma_s,$ and $\sigma_t$ are the stresses for the opening and shearing modes, respectively; $\varepsilon_n, \varepsilon_s, \varepsilon_t$ are the strains for the opening and shearing modes, respectively; $N_{\text{max}}, S_{\text{max}},$ and $T_{\text{max}}$ are the maximum stresses for the normal and shear modes, respectively; and $\varepsilon_n^{\text{max}}, \varepsilon_s^{\text{max}},$ and $\varepsilon_t^{\text{max}}$ are the maximum strains for the normal and shear modes, respectively. The normal component of stress and strain only accounts for tensile components as indicated by the brackets.

After initiation, the amount of damage accumulation for mixed-mode applications is a function of an effective displacement as shown by Equation (4.11).

$$\delta = \sqrt{\left(\delta^n\right)^2 + \left(\delta^s\right)^2 + \left(\delta^t\right)^2} \tag{4.11}$$

In the above equation, $\delta$ is the effective displacement while $\delta^s$ and $\delta^t$ are for Mode II and III, respectively. The damage parameter, originally shown in Equation (4.6) can be redefined as follows:

$$d = \frac{\delta^s \left(\delta_{\text{max}} - \delta_0\right)}{\delta_{\text{max}} \left(\delta_c - \delta_0\right)} \tag{4.12}$$

where $\delta_{\text{max}}$ is the effective maximum separation experienced by the interface element over the previous loading history, $\delta_c$ is the effective contact gap at the completion of debonding, and $\delta_0$ is the effective separation at the maximum stress.
The fracture energies for all three modes are given below.

\[ G_1 = \int_0^{\delta^t} \sigma_n d\delta^a \]  
(4.13)

\[ G_{II} = \int_0^{\delta^t} \sigma_s d\delta^s \]  
(4.14)

\[ G_{III} = \int_0^{\delta^t} \sigma_t d\delta^t \]  
(4.15)

Critical fracture energies for all three modes can be shown to be:

\[ G_{IC} = 0.5N_{\text{max}} \delta_c^{\text{sa}} \]  
(4.16)

\[ G_{IIIC} = 0.5S_{\text{max}} \delta_c^{\text{sa}} \]  
(4.17)

\[ G_{IIIIC} = 0.5T_{\text{max}} \delta_c^{\text{sa}} \]  
(4.18)

Similar to the initiation criteria, damage evolution can be defined in analytical form in several ways. These analytical definitions in the Power law form and the Benzeggagh-Keanane (BK) form are shown by Equations (4.19) and (4.20), respectively.
The fracture energy in the normal mode is given by $G_I$ while the two shearing states by $G_{II}$ (Mode II), and $G_{III}$ (Mode III). Critical fracture energies are denoted by $G_{IC}$, $G_{IIC}$, and $G_{IIIC}$, and the exponent is given by $\alpha$ in the Power law form, and $\eta$ in the BK form.

If the form of the damage evolution shown in Equation (4.19) is used with the assumption that the Mode III fracture energy is irrelevant, it is reasonable to expect that for a two-dimensional, axisymmetric application, the Power law exponent is 1. The resulting traction-based damage initiation and energy-based failure a criterion are shown in Equations (4.21) and (4.22).

$$
\left( \frac{G_I}{G_{IC}} \right)^\alpha + \left( \frac{G_{II}}{G_{IIC}} \right)^\alpha + \left( \frac{G_{III}}{G_{IIIC}} \right)^\alpha = 1 
$$

(4.19)

$$
G_{IC} + (G_{IIC} - G_{IC}) \left( \frac{G_{II} + G_{III}}{G_I + G_{II} + G_{III}} \right)^\eta = G_{IC} + G_{IIC} + G_{IIIC} 
$$

(4.20)

It is useful to define the mode mix ratio, $\psi$, between the normal and shear tractions:
\[ \tan \psi = \frac{\sigma_s}{\langle \sigma_n \rangle} \]  

Equation (4.23)

The magnitude of the effective traction vector, \( \sigma_m \), is given by Equation (4.24).

\[ \sigma_m = \sqrt{\langle \sigma \rangle^2 + \tau^2} = \frac{\langle \sigma \rangle}{\cos \psi} = \frac{\tau}{\sin \psi} \]  

Equation (4.24)

Equations (4.21), (4.23), and (4.24) can then be combined in order to define the critical traction magnitude for damage initiation as shown by Equation (4.25):

\[ \sigma_{m0} = \sigma_0 \left( \cos^2 \psi + \frac{\sigma_0^2}{\tau_0} \sin^2 \psi \right)^{-1/2} \]  

Equation (4.25)

Based on Equations (4.2) and (4.4) and on the assumption that the initial stiffness for Mode I is equivalent to that of Mode II, the equation relating the effective displacement and two displacements can be obtained:

\[ \delta = \frac{\langle \delta_n \rangle}{\cos \psi} = \frac{\delta^s}{\sin \psi} \]  

Equation (4.26)
After combining Equations (4.26) and (4.12), the damage parameter can be redefined as follows:

\[
d = \frac{\delta_c \left( \delta_c - \delta_0 \cos \psi \right)}{\left( \delta_c - \delta_0 \right) \delta_c^n} = \frac{\delta_c \left( \delta_c - \delta_0 \sin \psi \right)}{\left( \delta_c - \delta_0 \right) \delta_c^s}
\]  
(4.27)

After Equation (4.27) is combined with the traction equations (4.2) and (4.4), the following relationships for the normal transaction and the shear traction, respectively, can be obtained:

\[
\sigma_n = \frac{K_0 \delta_0}{\delta_c - \delta_0} \left( \delta_c \cos \psi - \delta_c^n \right)
\]  
(4.28)

\[
\sigma_s = \frac{K_0 \delta_0}{\delta_c - \delta_0} \left( \delta_c \sin \psi - \delta_c^s \right)
\]  
(4.29)

where \( K_0 \) is the initial normal and shear stiffness. When the final failure occurs, the displacements for the normal and shear conditions are \( <\delta^n>$=\delta_c \cos \psi \) and \( \delta^s=\delta_c \sin \psi \), respectively. The displacements can then be combined with Equations (4.13) and (4.14), to give the work done by tractions:

\[
G_I = 0.5 \sigma_m \delta_c \cos^2 \beta
\]  
(4.30)

\[
G_n = 0.5 \sigma_m \delta_c \sin^2 \beta
\]  
(4.31)
By combining the above with the fracture criteria, Equation (4.32) is obtained:

$$\delta_c = \left( \frac{\sigma_{m0}}{2G_{IC}} \cos^2 \psi + \frac{\sigma_{m0}}{2G_{IIC}} \sin^2 \psi \right)^{-1}$$  \hspace{1cm} (4.32)

Finally, the total mixed-mode fracture energy per unit area, \( \Gamma \), is defined by Equation (4.33):

$$\Gamma = G_I + G_{II} = 0.5\sigma_{m0} \delta_c = G_{IC} \left( \cos^2 \psi + \frac{G_{Kc}}{G_{IIC}} \sin^2 \psi \right)^{-1}$$  \hspace{1cm} (4.33)

From the above analysis, it is evident that for mixed mode loading, five independent parameters need to be defined before an analysis; the normal and shear strengths, \( \sigma_0 \) and \( \tau_0 \), two toughness values, \( G_{IC} \) and \( G_{IIC} \), and one stiffness, \( K_0 \). The respective responses of modes I and II in terms of fracture energy or bond strength are sometimes assumed to be equivalent in order to further reduce the problem complexity by one or two parameters. In addition, as it is hard to measure, the stiffness is often calculated using Equation (2.6) which is based on geometric and material properties. However, the problem cannot be simplified beyond three parameters.

### 4.2.2 Two-Dimensional Axisymmetric Model

Preliminary finite element modeling was completed in terms of simulating the effects of subjecting coatings to simultaneous and severe thermal and pressure transients. The preliminary work focused on developing and verifying two-dimensional models. The results achieved and the
experience gained from the preliminary modeling was then applied to the more sophisticated undertakings discussed in the following sections. Considerations such as multiple dimensions, cohesive zone formulation, cylindrical geometry, non-linearity, and temperature-dependent material properties and cracks was the focus of all subsequent research as detailed herein.

The first step of the modeling was to develop and verify a two-dimensional axisymmetric finite element representation of a coated tube subjected to thermal and pressure transients. Stress distributions for the geometry were determined in two steps. In the first step, a thermal model with a pulse, thermal boundary condition applied to the inside surface was solved. Two-dimensional thermal PLANE55 elements were used with the axisymmetric formulation activated; the element has four nodes with a single degree of freedom (temperature) at each node. The top nodes were coupled to simulate an infinitely long cylinder whereas the bottom nodes were given symmetry boundary conditions. For all the calculations, the outside of the cylinder was insulated and the initial temperature set to zero.

After the thermal simulation was completed, the structural response was evaluated for each chosen time quasi-statically. In order to interpolate the thermal response from the thermal to structural elements, the models each had the exact same mesh, elements, and nodes. To facilitate this, two-dimensional structural solid PLANE182 elements were used, again with the axisymmetric formulation activated. The inside surfaces experienced pressure, whereas the outside surfaces were stress free; zero displacement boundary conditions were applied to the bottom of the cylinder and the nodes on the top surface were coupled to move together in the axial direction. The meshed geometry is shown in Figure 4.3.
Verification was performed by comparing the FEA predicted axial stress, $\sigma_z$, with the analytical solution provided by Nied [160] for a coated cylinder subjected to an internal thermal shock. Using this solution, the non-dimensional temperature terms, $\theta_1(r^*,t)$ and $\theta_2(r^*,t)$, are given in Equations (4.34) and (4.35), respectively:

$$\frac{\theta_1(r^*,t)}{\theta_a} = 1 - 2 \sum_{n=1}^{\infty} e^{(-\rho \sigma^2)\lambda_n^2} \left[ \frac{\kappa \beta F_{00}(b^* \lambda_n, r^* \lambda_n) F_{11}(b^* \beta \lambda_n, r^* \lambda_n)}{b^* \lambda_n H(\lambda_n)} + E_{01}(b^* \beta \lambda_n, r^* \lambda_n) E_{01}(b^* \lambda_n, r^* \lambda_n) \right]$$

(4.34)
\[
\theta_n(r^2,t) = 1 - \frac{4}{\pi} \sum_{n=1}^{\infty} e^{-(\lambda_n^2 + \beta_n^2)} E_0 \left( r \beta_n, \beta_n \right)
\]

(4.35)

When the function \( H(n) \) is defined as:

\[
H(n) = \kappa \beta^2 F_{00} \left( a^* \lambda_n, b^* \lambda_n \right) \left[ E_{01} \left( b^* \beta_n, \beta_n \right) - b^{-1} E_{01} \left( \beta_n, b^* \beta_n \right) \right] \\
+ E_{01} \left( b^* \beta_n, \beta_n \right) \left[ -F_{00} \left( a^* \lambda_n, b^* \lambda_n \right) + \frac{a^*}{b^*} F_{11} \left( a^* \lambda_n, b^* \lambda_n \right) \right] \\
- \kappa \beta F_{11} \left( \beta_n, b^* \beta_n \right) \left[ -E_{01} \left( a^* \lambda_n, b^* \lambda_n \right) + \frac{a^*}{b^*} E_{01} \left( a^* \lambda_n, a^* \lambda_n \right) \right] \\
+ \beta E_{01} \left( a^* \lambda_n, b^* \lambda_n \right) \left[ -F_{11} \left( \beta_n, b^* \beta_n \right) + b^{-1} F_{00} \left( \beta_n, b^* \beta_n \right) \right]
\]

(4.36)

The roots, \( \lambda_n \), can be determined from the transcendental equation:

\[
F_{00} \left( a^* \lambda_n, b^* \lambda_n \right) F_{11} \left( \beta_n, b^* \beta_n \right) \\
+ \frac{\kappa^{-1}}{\beta} \left[ E_{01} \left( b^* \beta_n, \beta_n \right) E_{01} \left( a^* \lambda_n, b^* \lambda_n \right) \right] = 0
\]

(4.37)

While the functions \( F_{00}, F_{11}, \) and \( E_{01} \) are shown below:

\[
F_{00}(x,y) = J_0(x)Y_0(y) - J_0(y)Y_0(x) \\
F_{11}(x,y) = J_1(x)Y_1(y) - J_1(y)Y_1(x) \\
E_{01}(x,y) = J_0(x)Y_1(y) - J_1(y)Y_0(x)
\]

(4.38)
where $J_0$ and $J_1$ are Bessel functions of the first kind and $Y_0$ and $Y_1$ are Bessel functions of the second kind. All dimensions are normalized with respect to the outer radius, $c$; the radial position variable is $r^* = r/c$, the non-dimensional inner radius is $a^* = a/c$, the non-dimensional radius of the interface is $b^* = b/c$; and where $r$ is the radial position variable, $a$ is the inner radius, and $b$ is the radius of the interface. The non-dimensional thermal conductivity and diffusivity are $\kappa = \kappa_2/\kappa_1$ and $\beta = (D_1/D_2)^{1/2}$, respectively; the subscript 1 denotes the coating and the subscript 2 represents the substrate. Coefficients of the thermal expansion for the coating and substrate are $\alpha_1$ and $\alpha_2$, respectively. Temperatures as functions of position and time in Equations (4.34) and (4.35) are $\theta_1(r,t) = T(r,t) - T_0$ and $\theta_2(r,t) = T(r,t) - T_0$, where $T_0$ is the initial temperature, $\theta_a$ is equal to $T_a - T_0$, and $T_a$ is the applied values at the coating surface.

Equations (4.39) and (4.40) give the axial stress as a function of position and time, $\sigma_z(T(r^*,t))$, for the coating and the substrate, respectively.

$$
\left(1 - \nu \right) \sigma_z \left( r^*, t \right) = \frac{2}{1 - a^2} \left[ \frac{b^2 - a^2}{2} + \right.
\left. \frac{\kappa \beta}{2 \pi \lambda_n} + a^* E_{01} \left( b^* \lambda_n, a^* \lambda_n \right) F_{11} \left( b^* \beta_n, \beta_n \right) \right]
$$

$$
2 \sum_{n=1}^{\infty} e^{-\left(-D_1/c^2\right)\lambda_n^2} \times \left[ \frac{\kappa \beta}{2 \pi \lambda_n} + a^* E_{01} \left( b^* \lambda_n, a^* \lambda_n \right) F_{11} \left( b^* \beta_n, \beta_n \right) \right]
\left[ b^* \lambda_n^2 H \left( \lambda_n \right) \right]
$$

$$
\left[ \frac{\kappa \beta}{2 \pi \lambda_n} + a^* E_{01} \left( b^* \lambda_n, a^* \lambda_n \right) F_{11} \left( b^* \beta_n, \beta_n \right) \right]
\left[ b^* \lambda_n^2 H \left( \lambda_n \right) \right]
$$

$$
\left( a \leq r < b \right)
$$
\[
\frac{(1-\nu)\sigma_{\theta}^T (r^*, t)}{E\alpha_1 \theta_a} = \frac{2}{1-a'^2} \left[ \frac{b'^2-a'^2}{2} + \kappa \beta \left( \frac{2}{\pi \lambda_n} + a' E_{01} (b'^* \lambda_n, a'^* \lambda_n) \right) F_{11} (b'^* \beta \lambda_n, \beta \lambda_n) \right] \\
2 \sum_{n=1}^{\infty} e^{(-D_1/c) \lambda_n^2} \times \left[ b'^* \lambda_n^2 H (\lambda_n) \right] + \frac{\alpha_2}{\alpha_1} \frac{1-b'^2}{2} - \frac{4}{\pi} \sum_{n=1}^{\infty} e^{(-D_1/c) \lambda_n^2} F_{11} (b'^* \beta \lambda_n, \beta \lambda_n) \right] \right] - \frac{\alpha_2}{\alpha_1} \theta_2 (r^*, t)
\]

\[(b \leq r < c)\]

As shown in Figure 4.4 through Figure 4.6, reasonable accuracy was observed.

---

**Figure 4.4**: Finite element and analytical comparison of circumferential stress at time t=0.1s.
Figure 4.5: Finite element and analytical comparison of circumferential at time t=0.5s.

Figure 4.6: Finite element and analytical comparison of circumferential stress at steady state.
4.2.3 Coupled Axisymmetric FEA Model with Interface Crack

As discussed earlier, the initiation of a crack between the coating and substrate is a significant event in the evolution of damage leading to gun tube failure. Once interface separation has begun, the crack grows and starts to turn into the coating, before causing complete separation. Given the significance of this event and the importance of retarding interface crack growth, an analysis was performed to discern the effects of coating parameters on the stress state at the crack tip. These results were then compared to an analysis of an uncracked interface. The goal of the current research was to obtain a better understanding of the underlying failure mechanism. In order to accomplish this goal, a new and more powerful finite element model was developed using the axisymmetric geometry of the internally coated tube under study with a defect at the interface.

In addition to the model mentioned in the previous section, further analysis was done on a tube that had a fracture at the interface of the coating and the substrate. As shown in Figure 4.7 the model has a crack at the interface of the compound cylinder, with collapsed singular elements used at the crack tip.

For the ensuing analysis, coupled thermal-structural elements were used. The nature of these elements allows the thermal and stress distribution to be determined at every sub-step without running the structural analysis as separate steps in a quasi-static way. The switch from interpolating thermal results to the structural mesh, to coupled elements was made to significantly simplify the analysis procedure while still allowing for the most severe conditions of the transients to be easily extracted. Two-dimensional thermal solid PLANE13 ANSYS elements were used with the axisymmetric formulation activated. These elements have four
nodes, each with three degrees of freedom, namely the temperature and two displacement dimensions. For the FEA model, the top nodes were coupled for uniform displacement (generalized plane-strain) to simulate an infinitely long cylinder, the bottom nodes were given symmetry boundary conditions, and the outside of the cylinder was subjected to room temperature convection. A uniform initial temperature of zero was used for all calculations. Using the axisymmetric mesh and boundary conditions mentioned above, a model was developed with a pre-crack at the interface of a compound cylinder simulated by singular elements. For the model, the axial length of the crack length was twice the coating thickness. Symmetry boundary conditions were applied to both the coating and substrate at the crack, thus simulating a “blister”-type debonds or defects.

![Crack Tip](image)

Figure 4.7: Collapsed elements at interface crack tip.
All the material properties were input as linearly varying functions of temperature as determined by empirical data pertaining to the room temperature and the maximum temperature of 1300 K, the latter of which exceeded the maximum temperature anticipated during the transient. The boundary conditions applied to the inside of the cylinder comprised a convective thermal pulse with a heat transfer coefficient of 193,000 W/m² °K, which was followed after approximately 0.4 ms by a pressure pulse. Using known barrel conditions, the applied amplitudes were 3160K for the gas temperature and 391MPa for the pressure pulse with durations of approximately 15ms and 8ms, respectively, which were the actual boundary conditions seen in the gun tubes. Complete gun tube coating surface thermal and pressure boundary conditions are shown in Figure 4.8

![Convective Gas Temperature and Pressure as functions of Time](image)

**Figure 4.8: Thermal and Pressure Boundary Conditions.**

Before any crack configurations were considered, the axisymmetric model with coupled elements was verified via Equations (4.34) through (4.40). Fortunately, the results were in perfect agreement with the analytical solution, such that the model with coupled elements could
be used to investigate the effects of important parameters in gun tube design. Using the axisymmetric model, the researcher varied the coating thickness by adding and subtracting 10% and 20% from the baseline value. In addition, the coating elastic modulus value was changed by 20% and 40% to reflect likely variations. The studies in which these parameters have been referenced as important for coating performance are cited in Section 2.2.

### 4.2.4 Four-Point Bend Test Simulation

A finite element model for the four-point bend test was developed, the coding for which is given in Appendix B.c. The four-point bend test, as described above, is often used to evaluate the critical fracture energy of an interface. As the name implies, a rectangular coated specimen is loaded in a four-point bend fixture in such a way as to put the coated side of the specimen in tension. Given the symmetry of the problem, only half the specimen was modeled to improve computational efficiency. The sample was modeled with the coated side up, while the outer span applied the loading to the coating and the inner span supported the substrate to keep the coated side of the specimen in tension. Reflecting the testing procedure, the coating was modeled as notched by suppressing the symmetry boundary conditions on the coating. To simulate progressive crack growth during loading, cohesive zone elements were inserted at the interface between the coating and the substrate. In addition, as the four-point bend test procedure requires, a pre-crack of an arbitrary length could be inserted between the coating and the substrate by withholding cohesive zone elements. The interface pre-crack, the length of which can be measured, is necessary to evaluate the critical fracture energy, and is often formed experimentally with a three-point bend configuration.

The model was created using the Abaqus finite element software, and is shown in Figure 4.9.
Figure 4.9: Four-point bend test configuration.

The loading fixture was modeled as two half circles. The displacement of the bottom load point was constrained in all directions, while the top load point was constrained in the horizontal direction and the displacement was applied in the direction perpendicular to the surface. As stated previously, symmetry boundary conditions were applied to the substrate at the center of the beam, while the coating was left unrestrained in order to simulate a pre-crack. Interfacial friction was included as a typical value of 1.0 for the friction coefficient for aluminum–aluminum contact; additionally friction was included at the load points. Element-type CPS4R, an implicit, linear, quadratic finite element, was used for the coating, substrate, and loading points. The element is a plane stress quadrilateral element with reduced integration and hourglass control. In the cohesive zone, element-type COH2D4, i.e., four-node, two-dimensional cohesive elements were used. Viscous regularization was employed to improve the convergence of the model. In addition, after cohesive zone element failure, the elements where decohesion had occurred were deleted in order to monitor crack growth.

The finite element modeling procedure just described was adequate for steady state crack growth in which the crack grows steadily as the load is applied. However, in some cases, steady state crack growth is not possible; for instance, in samples that fail along the cohesive zone in a brittle fashion. In such instances, dynamic effects can contribute to crack growth. Because of this,
dynamic effects were included by using an implicit dynamic load step. Because the sample is loaded quasi-statically, the loading fixture should not directly introduce dynamic effects into the specimen. Based on this premise, the quasi-static dynamic approach was chosen to prevent the inclusion of any dynamic effects of the loading fixture in the simulation, and yet still allow for dynamic effects once crack initiation begins.

As mentioned in Section 2.12.5, the accumulated residual stresses significantly affect the measured interfacial properties. This is the case whether the residual stresses are the result of deformations associated with cold spraying, the coefficient of the thermal expansion difference during thermal spraying, or some other mechanism. As mentioned earlier, assuming they have linear profiles, residual stresses can be quantified based on the curvatures of the debonded coating and the substrate after separation and on the composite sample. Given the importance of residual stresses in interfacial crack growth and property measurement, the profile of the measured residual stress distribution can be applied to the four-point bend test model through a user subroutine, shown in Appendix B.f. The “sigini.f” subroutine can be used to apply three-dimensional residual stresses as functions of any of the coordinate directions. In addition, the coating and the substrate can have independent residual stress distributions.

An important point in implementing cohesive zone elements is that the cohesive zone can be meshed independently of the two sides of the interface. In fact, the cohesive zone can be meshed, collapsed to zero-thickness, and then the top and bottom tied to the coating and substrate, respectively. The independence of the cohesive zone mesh is useful, as it allows the cohesive zone to require smaller elements for the resolution in order to simulate small increments of crack growth, and yet the model remains computationally efficient. It is recommended that the ratio of the size of the cohesive zone to the structural elements not significantly exceed three. In order to
allow for easier parameterization, mesh adjustment, and simplicity, the input file was written in Python scripting.

Preliminary verifications for the four-point bend test were completed via Tsui et al. [161], since the researchers who developed the testing procedure often used to calculate the critical energy release rate under four-point bending conditions, also provided load-displacement results for one of their experiments. In addition, they provided sample dimensions, material properties, residual stress distributions, and the calculated critical energy release rate. Based on these parameters, simulations were run during which the interfacial bond strengths were varied until proper agreement was observed between the experimental and finite element results. The sample parameters can be found in Tsui et al. [161], whereas the Mode I and II strengths that provided the best agreement were found to be 40 MPa and 10 MPa, respectively. The interfacial strength parameters were bounded by bond strength measurements calculated for other thermal spray coatings calculated from button tests. A plot of the comparison of the results is shown in Figure 4.10

The finite element simulation results began with a preload on the fixture caused by the residual stresses; as such, experimental results were adjusted to match the preload. The stiffness of the linear region reported and numerical results do not match perfectly, which is likely to have been caused by a discrepancy in the elastic modulus of the substrate. In addition, it should be noted that the results providing the best agreement included a normal strength that was higher than the shear strength. Though it is not possible to be certain why this configuration provided the best agreement, it is likely to be because mode-independent fracture energy was assumed as opposed to mode-dependent fracture energies. With the fracture energy constant, the strength of the interface is the only adjustable parameter implicated in the behavior of the interface. As can be seen in Figure 4.2, when the interface stiffness and fracture energy (the area under the load-
displacement curve) are constant, the post-damage initiation state is completely governed by the bond strength. In this situation, a high bond strength results in a brittle interface, whereas a low bond strength results in a more ductile interface, the later requires more energy to grow the crack after the maximum stress is achieved. Therefore, in systems having equal fracture energies, lower bond strength suggests that there is more energy in the post-damage initiation stage and that the fracture energy in Mode II should actually be larger than was assumed in the above simulation. In most cases, the fracture energy in Mode II is significantly higher than in Mode I, mainly because of friction and other effects which dissipate energy at the crack tip.

![Graph](image-url)

**Figure 4.10:** Comparison of FEA and experimental results.
4.2.5 Ball Indentation Simulation

A ball indentation model, the code for which is given in Appendix B.a and Appendix B.d was developed for two purposes. The model was used first as verification for the cohesive zone elements, and the properties were measured during four-point bend experiments. In addition, the model was used in an implicit to explicit analysis to quasistatically introduce an interfacial flaw before the effects of rapid thermal transients were analyzed.

To simulate the delamination under loading from a spherical indenter, an Abaqus finite element model of the experiment was also developed. The model simulates a ball indenter being displaced into the surface of a coated substrate. Axisymmetry was utilized, as the geometry, loading, and subsequent structural response are not functions of the angle of rotation. In addition, residual stresses can be applied to the coating and substrate through a user subroutine, as described in the previous section. The meshed geometry of the problem is show in Figure 4.11.

The regions at the axis of the axisymmetry were constrained to prevent rotation and displacement in the radial direction, while the bottom of the model was constrained in the y-direction. The outside surface, shown in red, was coupled in the radial direction in order to simulate an infinitely large plate. Coupling was accomplished in Abaqus through a constraint equation for which the displacement of the outside edge was set equal to the top corner of the coating. The spherical indenter was modeled with quarter symmetry, while the displacement loading condition was applied to the top symmetry surface. Surface-to-surface contact interaction properties were defined at the area of possible contact between the coating and indenter surfaces, while an appropriate friction coefficient of 0.6 for steel nickel contact was defined. The coefficient was also applied to the coating surface and indenter interface.
Figure 4.11: Meshed geometry of the indentation test.

Element-type CAX4RT, an implicit, linear, quadratic finite element was used for the coating, substrate, and indenter. The element is an axisymmetric, coupled temperature-displacement stress quadrilateral element with reduced integration and hourglass control. In the cohesive zone, element-type COHAX4 elements were used. Represented by the black line in Figure 4.11, the chosen cohesive element is a four-node, two-dimensional, axisymmetric, cohesive element. Viscous regularization was used to improve the convergence of the model. In addition, after cohesive zone element failure, the failed element was deleted in order to better monitor crack growth.
As mentioned earlier in this section, the model was used in a sequential implicit-to-explicit analysis. Though the quasistatic ball indentation step of the analysis only requires structural degrees of freedom, coupled thermal-structural elements were necessary to import the results into the subsequent thermal-structural analysis. Because of the large number of increments necessary for the subsequent explicit analysis, the implicit analysis was set to double precision to improve accuracy.

4.3 Experimental Results

4.3.1 Indentation Test Results

Indentation tests were performed on the samples in order to provide the information necessary to evaluate the interfacial fracture energy and bond strength. Following the procedure described in Section 4.1.3.3, various indentation tests were performed on the coated specimens. Different techniques, such as in situ acoustic emission monitoring, post-test c-scans, and finite element modeling, were all used to develop a new method to determine cohesive zone properties. The resulting technique is unique in that it provides a way to determine cohesive zone properties, which are both notoriously difficult to measure and applicable to a wider range of samples than existing tests. Such a technique is not limited to the very narrow range of materials that can be appropriately evaluated with the four-point bend test due to the multitude of difficulties already discussed.

In order to study cases with relatively strong interfacial bonding, or for which constant crack growth is not possible, a method to evaluate a wider range of material combinations is developed.
in the present study. In addition to its applications to a wider range of material combinations, the method described will be used to determine both the interfacial fracture energy and bond strength, whereas most analytical solutions are capable of determining only one property for a given test.

Acoustic emission monitoring will also be used to help determine the occurrence of major events such as crack growth initiation and re-initiation. In addition, samples will be loaded to various levels, enabling a flaw to evolve as a function of the applied force. Finally, as the flaw size from the e-scan results as a function of maximum load, and the approximate load of delamination initiation are known, finite element simulations will be used to evaluate the interfacial properties. In the simulations, the properties of the interface are varied until the experimental and numerical results match up in terms of flaw size and the onset of damage at the interface.

4.3.1.1 Test Results for the Nickel-Coated Steel Samples

4.3.1.1.1 Acoustic Emission Results

Indentation tests were completed for the nickel-coated steel specimens. The samples, described in Section 4.1.1.3, were produced by using a wire EDM to reduce the substrate and coating thicknesses to 2.0 and 0.6 mm, respectively. Additionally, before testing, the samples were cut into 20 mm squares. All indentation tests are load-controlled at a rate of 0.05 mm/min with a 2 mm indenter. All the results of all the nickel-coated steel tests are given in Appendix A.

Nickel-coated steel indentation tests were conducted to methodically determine the final flaw dimensions and the load at which delamination initiates. Several tests were conducted and stopped at intermediate indentation loads. Preliminary tests were conducted to determine the
point at which significant acoustic events occurred, a suitable acoustic emission threshold parameter, and the load at which delamination began. Results from the preliminary tests are given in Figure A.1 through Figure A.9. Representative results are shown in Figure 4.12.

Based on the acoustic emission results, which were thresholded to remove events below 37 dB, it appears that a significant event occurred early in the test, at around 0.2 to 0.5 kN, and that another at around 2.0 to 2.5 kN. A significant event for the purposes of this analysis is defined as having a higher energy and amplitude than the background noise. The early events correspond with a slight change in the slope of the load displacement curve, shown in Figure A.1. It is likely that the slope change is the result of plastic deformation and the flattening of the sample curvature by the indenter. Further investigation will provide insight into the contributions of each of these factors.
Samples were then loaded to a fixed indentation extension of approximately 0.5 mm to provide a sampling of the data at what was deemed to be a depth capable of producing a significant level of delamination. Acoustic emissions were again recorded, but because of adjustments to the threshold value, not all results were useful. It was expected that the lower threshold would provide more information, yet the system actually hit a “lockout” time. At the lower threshold, the system detected lower energy hits. Recording all the low energy hits tied up the system, and by the time the channel is available for the next hit, the larger energy event is over and is not recorded. Because of this issue, not all the experiments produced useful acoustic emission information. Once the problem was recognized and corrected, acoustic emissions were again recorded and used to provide useful information about important events during indentation testing. As already seen, a significant event occurred at around 0.2 to 0.5 kN and again at 2.0 to 2.5 kN. Such results confirm what was seen in earlier tests, i.e., significant events occur at distinct times. Additionally, significant events appear to reliably occur at the end of the tests loaded over 3.0 kN.

The data, in Appendix A (Figure B.1 through Figure B.11) represent loading until the extension of the indenter reached 0.5 mm. In the first four samples, the load was approximately equal to 3.2 kN and the ram extension required to achieve the load was approximately 0.5 mm. In the acoustic emission results, significant events appear to have occurred early, and again late in the test. As before, these events again occurred at approximately 0.5 kN, and after 2.75 kN. Finally, the last set of samples was loaded to 1.5, 2.0, 2.6, and 3.2 kN, respectively with two tests conducted for each load. Results are shown in Appendix A, in Figure C.1 through Figure C.18. Early events were not seen in Figure C.6 or Figure C.8, but were seen in all the other experiments. Additionally, for the tests that exceeded 2.0 kN, significant events were seen between 2.0 and 2.5 kN, and again around 3.0 kN.
As explained, the acoustic emission and load results revealed that significant events occur regularly at specific load ranges. However, other events above the noise level also occur at different load levels in the plots of the acoustic events as shown in Appendix A. Whitle, the events do not seem to occur regularly, they are nonetheless above the noise floor in terms of both acoustic energy and duration.

4.3.1.1.2 C-scan and SAM Results

All the specimen substrates and coatings studied were metal, and, therefore, opaque. Because crack growth occurred below the surface and away from the edges during indentation testing, the crack dimensions must be measured by post-indentation ultrasonic imaging. Such non-destructive evaluation enables the measurement of the delamination radii of a flaw without the difficulties of cutting or otherwise destroying the sample. It is likely that different ultrasonic measurement features correspond with delaminations for different sample materials and geometries. On this basis, results must be carefully considered in order to determine whether a signal scan feature is in fact a delamination.

By analyzing and plotting the signal features, such as the amplitude and time of flight of the signal reflections, the size of the delaminated area can be determined. High-amplitude reflections from the first interface proved to be good indicators of delamination in the nickel-coated steel samples. Ultrasonic energy is less likely to pass through a delamination when there is a space between the coating and substrate, which results in more ultrasonic energy being reflected back to the transducer. This type of effect is possible because compared with the nickel coating, the substrate material is much stiffer and, therefore, keeps the interface perpendicular to the
transducer. Such conditions ensure that the energy at the interface separations is reflected, and not transmitted, whereas the planar conditions of the interface ensure that the reflection is returned to the transducer and not reflected away. A representative scan is shown in Figure 4.14.

As mentioned in the discussion of acoustic emission results, preliminary testing was performed to help determine approximate load levels above which separation is initiated and how the flaw grows as a function of maximum load. In preliminary testing, samples were loaded by extensions of 0.2, 0.3, 0.4 and 0.5 mm (corresponding to 1.3, 2.0, 2.6, and 3.1 kN) and denoted as Preliminary 1 through 4. The e-scans are shown in Figure A.3 through Figure A.9. Preliminary 1, loaded to 1.3 kN, as shown in Figure A.3, showed almost no signs of delamination. Though a slight ring was seen around the indentation, the interfacial reflection was not above the amplitude noise seen far from the indentation. Similar observations were made for Preliminary 2, loaded to 2.0 kN, as shown in Figure A.5. However, obvious delaminations were seen in the next two samples, shown in Figure A.7 and Figure A.9. Extremely high reflections were observed in the form of a ring around the indentation location. In addition, a low-amplitude ring was seen around the outside of the delamination, perhaps a result of residual compressive stresses at the edge of the delamination. The issue will be further investigated with finite element modeling. The outer flaw radius increased with higher indentation load as expected. In addition, both flaws were unsymmetrical. Though the finite element model assumes axisymmetry, such conditions cannot be expected for all indentations due to the coating-spray direction and residual stresses. Recognizing this, each flaw was characterized in three ways: maximum radius, minimum radius, and average radius. All the indentation test results for the nickel-coated steel samples are given in Table 4.1.
Table 4.1. C-scan results of the indentation tests on the nickel-coated steel samples.

<table>
<thead>
<tr>
<th>Sample Name</th>
<th>Max Load (kN)</th>
<th>Max Indenter Depth (mm)</th>
<th>C-scan observed failure?</th>
<th>Delamination Radi (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>Inner</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>Max</td>
</tr>
<tr>
<td>Preliminary 1</td>
<td>1.28</td>
<td>0.2</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Preliminary 2</td>
<td>1.93</td>
<td>0.3</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Preliminary 3</td>
<td>2.58</td>
<td>0.4</td>
<td>yes</td>
<td>1.22</td>
</tr>
<tr>
<td>Preliminary 4</td>
<td>3.09</td>
<td>0.5</td>
<td>yes</td>
<td>1.44</td>
</tr>
<tr>
<td>5A 1</td>
<td>1.51</td>
<td>0.52</td>
<td></td>
<td></td>
</tr>
<tr>
<td>5A 2</td>
<td>1.52</td>
<td>0.29</td>
<td></td>
<td></td>
</tr>
<tr>
<td>5B 1</td>
<td>2.0</td>
<td>0.36</td>
<td></td>
<td></td>
</tr>
<tr>
<td>5B 2</td>
<td>2.0</td>
<td>0.36</td>
<td></td>
<td></td>
</tr>
<tr>
<td>5C</td>
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<td>1.56</td>
</tr>
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<td>5D</td>
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<td>yes</td>
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</tr>
<tr>
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<td>0.55</td>
<td>yes</td>
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</tr>
<tr>
<td>3A</td>
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<td>yes</td>
<td>2.30</td>
</tr>
<tr>
<td>3B</td>
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<td>yes</td>
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</tr>
<tr>
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<tr>
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<td>yes</td>
<td>1.98</td>
</tr>
<tr>
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<td>0.5</td>
<td></td>
<td></td>
</tr>
<tr>
<td>4C</td>
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<td>2.00</td>
</tr>
<tr>
<td>4D</td>
<td>3.16</td>
<td>0.5</td>
<td>yes</td>
<td>2.41</td>
</tr>
</tbody>
</table>

As can be seen, the flaw size increased by approximately 0.2 mm from Preliminary 3 to Preliminary 4, which corresponds to an increase in load of 0.5 kN. From these preliminary results, it can be seen that an interfacial fracture occurs in samples loaded at between 2.0 and 2.5 kN and that the fracture grows slightly during further loading. Results were consistent for the samples measured, in terms of the size and presence of an indentation-induced flaw.

Based on the results from preliminary experiments, the decision was made to load samples to several values and determine a smaller range, within which it was expected that the initiation of the fracture would be contained. Samples were therefore loaded to 1.5, 2.0, 2.6, and 3.2 kN. Two samples were tested for each load level and analyzed with the images produced by c-scan, the results of which are shown in Appendix C in Figure C.3 through Figure C.18. Overall, similar...
observations were made about the scans with regard to the samples being slightly asymmetric, in addition to a low-amplitude reflection ring outside the delamination, and concentric with the indentation. The samples shown in Figure C.7 and Figure C.9 were loaded to 2.0 kN. A slight halo of high-amplitude reflection surrounded the indentation site, but was cloudy and undefined. The ring, which does not appear in Figure C.5 or Figure C.7, shows less definition and has lower amplitude than the obvious delaminations in the higher-indentation load samples. Based on these observations, it was determined that the samples loaded below 2.0 kN did not contain delaminations, but experienced some degree of interfacial damage.

An interesting feature was observed in Figure C.18, which shows that the center of the indentation also delaminated after the load was removed. Because the plastic deformation pushes the interface into the substrate, the center of the indenter is under high compressive stress. Therefore, the center of the sample experiences tensile stresses after the indenter is removed as the surrounding coating pulls upward. The results shown in Figure C.18 suggest that complete delamination is possible, though unlikely, for the material system after high loading has taken place.

In order to provide a wider sampling of the flaw size as a function of indentation load, further tests were performed and the results analyzed with the c-scan, as shown in Figure B.3 through Figure B.11. The results for these tests were similar to those described above. All the samples that were loaded to greater than 2.0 kN experienced delaminations. However, the samples shown in Figure B.9, were loaded to 1.85 kN, and based on previous results these samples were not expected to experience a separation at the interface. In Figure B.9, a slight ring of higher amplitude reflection is evident, but again, it is at lower amplitude and not as well defined as for the samples loaded to above 2 kN. These results, in addition to those shown in Figure C.7 and
Figure C.9, suggest that damage for the thermally sprayed nickel-coated steel system is progressive and occurs slowly over a load range.

As mentioned above, in order to analyze the signal received at the transducer, the wave speed equation must be used to determine the point at which to expect portions of the signal reflection based on their arrival time. Because the exact wave speed is generally not known, and different signal reflections can often interfere with each other, the c-scan images were verified with a scanning acoustic microscope (SAM). The SAM uses higher-frequency signals, resulting in improved resolution. In addition, because of higher-frequency transducers, there is significantly less interference between signal reflections, resulting in more straightforward signal analysis. Because the advantages of the SAM nullify many of the uncertainties associated with c-scan imaging, the decision was made to validate the c-scan delamination measurements by imaging a sample with both techniques, and then comparing results. The SAM images are shown in Figure 4.13, and the c-scan images for the same sample are shown in Figure 4.14.

Both scans measured the first interface reflection of the acoustic energy. As such, delaminations would appear as large amplitude reflections for two reasons: (1) very little energy can pass through a delaminated discontinuity, and (2) minimal energy is reflected away because of the perpendicular interface. High-amplitude reflections are shown in white in both images. Both images reveal the same asymmetric crescent-shaped flaw, with an undelaminated region on a quarter of the perimeter, as well as a ring of low-amplitude reflection surrounding the delamination. In addition, there is good agreement on the outside flaw dimension between the two images. However, the SAM image shows a larger inner diameter for the delamination. Because the transducer surface area is smaller and at a higher energy frequency than the c-scan, it is more focused on the interface in the SAM image. Because of the high resolution of the SAM
images, energy is reflected away from the transducer at the edges of the delamination where the interface is no longer parallel to the sample surface. Thus, a low-amplitude reflection was produced. The surface deformation effects are less significant for c-scan measurements because the transducer surface is larger and the energy is already defocused.

Figure 4.13: SAM image of sample loaded to 2.6 kN (0.4 mm indenter displacement).
Acoustic emission and c-scan results following the indentation tests described above were combined to form a complete description of the initiation and evolution of the interfacial crack growth. Additionally, a sample viewed with the SAM was used to validate the c-scan imaging results and confirm the flaw dimensions. In conjunction with finite element modeling, the information described in the above sections was used to evaluate the cohesive zone properties of the interface.

4.3.1.2 Test Results of the Aluminum-Coated Aluminum Samples

Aluminum samples were indented similarly to the nickel-coated steel. All tests were performed on samples with 3.0 mm thick substrates. Indentation tests used load controls and several different spherical indenters at a rate of 0.05 mm/min as described in Section 4.1.1.3. However, important differences exist between the objectives of the aluminum indentation tests and those for the nickel-coated steel. Whereas the nickel-coated steel tests were conducted with the goal of
accurately determining cohesive zone properties based on experiments and finite element modeling, the aluminum experiments were designed to validate the cohesive zone and the indentation models in addition to the property measurement technique.

Coating thickness varied significantly on the four samples tested, which made repeated tests difficult. In addition, plastic deformation in the cold-sprayed aluminum coating released high amounts of acoustic energy due to cracking, making it difficult to analyze the emissions data during loading because of the high background noise level throughout the test. On the basis of these difficulties, the decision was made to use the aluminum-coated aluminum indentation and four-point bend samples for the purpose of validating the indentation model. Because the interfaces of indentation and four-point bend tests were the same, even though the coating was milled down to a thickness more appropriate for indentation testing, the cohesive zone properties measured based on the indentation test can be applied to the four-point bend test. Accurately predicting the results of one test with properties measured from a different experiment provides validation for the indentation, four-point bend, and cohesive zone models. After the indentation tests were completed and the acoustic emissions collected, according to the procedures described in Section 4.1.3, c-scan images of the samples were recorded.

4.3.1.2.1 Acoustic Emission Results for the Aluminum-Coated Aluminum Samples

As with the nickel-coated steel, acoustic emissions were recorded during indentation testing of the aluminum-coated aluminum samples. However, the results pertaining to the indentation of the latter could not be analyzed because they included too much noise; the samples experienced significant cracking during the indentation test, creating high-energy events that were frequent enough to raise the noise level to a point that made it impossible to determine whether any other significant events were occurring. For completeness, plots of the acoustic emission results
corresponding to the aluminum indentation results and the c-scans for the individual tests are included in 0.

Crack growth is also likely to occur during unloading. Because further cracking and plastic deformation are less likely during load removal than during loading, acoustic emissions were recorded during unloading for several samples; one such plot is shown in Figure D.17. As the results were recorded during unloading, acoustic emission collection began at a load of 5 kN, and recording was stopped at the end of the test, when the load was 0 kN. As can be seen in the figure, large energy acoustic events occur at low load levels at the end of unloading. It is possible that these events are delamination initiation and growth during unloading and will require further analysis in order to determine whether they are the result of delamination or of a source of noise in the system.

4.3.1.2.2 C-scan Results for the Aluminum-Coated Aluminum Samples

C-scans were performed on the aluminum sample indentations to determine the amount of delamination that took place. In order to image the interface of the samples, ultrasonic energy was directed at the sample, the reflections of which are measured and plotted as described in Section 4.1.3.4. In order to analyze the aluminum samples, the time of flight of the signal was also recorded and used to determine the dimension of any delaminations present. Scans revealed that delamination occurred in all the samples with a coating thicker than 0.9 mm, whereas it was concluded that indentations on the thinner samples did not result in any interfacial separation.

A first set of scans was performed on the indentations of the samples with the thicker coatings, i.e., coatings thicker than 0.8 mm. Scans of the time of flight and the first interface reflection for
all the samples are shown in 0. One particular indentation was close to the edge of the sample and actually fractured to the edge. In this case, the fracture can be confirmed by visual inspection, allowing for the scan results to be used to determine the features of the internal delaminations in the aluminum samples. Such features will be used in future scans as indications of interface separation. Scans showing the amplitude of the returned signal and the time of flight for that signal are shown in Figure 4.15.

Figure 4.15: Scan of (a) amplitude and (b) time of flight of the signal for a 2.0 mm indenter loaded to 4.75 kN.

Because the scan was of a known delamination at the edge of a specimen, the characteristics of the scan features shown in Figure 4.15 can be used to determine whether other samples also had
similar damage. As can be seen from the scan of the amplitude of the interface reflection, the edges of the delaminations are characterized by low-amplitude reflections. In addition, the time of flight scan showed a significantly shorter flight time for a signal reflected from a delamination, as shown by the lighter portion of the spectrum in Figure 4.15b, than for a signal reflected from the surrounding interface. As expected, the shorter time of flight confirms that the signal travels a shorter distance at a delamination. Understanding and recognizing these characteristics makes it possible to identify the internal delaminations in the other samples.

Two more scans of indentations in coatings of approximately the same thickness and load as those just described are shown in Figure D.16 and Figure D.18. In terms of the information gained from the scans described above, the coating and substrate of the samples appear to have separated. Consistent with observations from Figure 4.15, the indentations are characterized by a low-amplitude first-wall reflection that encircles the indentation. In addition, the time of flight of the reflection is shorter for the delaminated area than for the surrounding scan. Compared with those in Figure 4.15, the scans have features consistent with those with a confirmed delamination. Based on the features just described, it was concluded that delaminations were present in the two scans using the data in Figure D.16 and Figure D.18; the diameters of the delaminations were measured and recorded (see Table 4.4).

Indentations were also made in samples with thinner coatings, on the order of 0.37 to 0.6 mm. The information from these scans is given in Figure D.1 though Figure D.12. Though these indentations were made with similar loads as those described above, they were on samples with thin coatings, and none of the e-scan features described above was present. Scans of the first-wall amplitude reflection, in addition to the time of flight, for the entire sample are shown in Figure 4.16.
As can be seen in the figure, the characteristics found to accompany delaminations are not present. Scan images reveal regions of low-amplitude reflection, though the difference between the indentation and the surrounding interface was not as significant or as well defined as for the cases of separation. More compelling evidence that supports the absence of delaminations in the samples can be seen in the time of flight images. In the samples with thinner coatings, time of flight scans of the first amplitude arrived noticeably sooner than the same kind of scan for the surrounding area for the samples with thicker coatings. However, almost no information was found to provide any indication of indentations, let alone delaminations as shown in Figure 4.16.
4.16b. Based on the amplitude and time of flight scans, it was concluded that indentations did not create delaminations on samples with coatings thinner than 0.75 mm for all of the load levels in the experiments.

The scans shown in Figure 4.16 do not show the same first-amplitude reflection characteristics and contain almost no information in the time of flight to suggest that there are delaminations like those shown in Figure 4.25. The ultrasonic reflection characteristics of delamination in the aluminum samples studied in this section differed significantly from those in Section 4.3.1.1. Delamination characteristics varied between the two largely due to the differences in stiffness between the coatings and substrates of each. In the case of the aluminum, it is likely that the low-amplitude reflections are the result of large deflections from the interface. When ultrasonic energy reaches an interface, it is reflected and transmitted, as described earlier. However, when that interface is not perpendicular to the transducer, large portions of the energy are reflected away from the transducer, creating the appearance of a low-energy reflection. It is likely that the edges of the actual indentation are regions where the most energy would be deflected because of this effect, whereas the area close to the indentation is more parallel and will reflect energy in a way that is similar to the surrounding, unaffected interface. Substrate and coating-material property similarities resulted in large deformations at the interface, and the relatively soft nickel coating the steel substrates resulted in deformations predominantly in the coating. Therefore, very little deformation occurred in the substrate relative to the coating. The interface, therefore, remained perpendicular to the transducer. As the interface remains relatively flat, ultrasonic energy can be reflected back toward the transducer, creating a high-amplitude reflection from the interface. On the other hand, in the cases in which significant deformation was seen at the interface, i.e., the aluminum samples, the energy is deflected away.
4.3.2 Four-Point Bend Test Results

Four-point bend tests on the samples of nickel-coated steel and on those of aluminum cold sprayed on aluminum as described in Section 4.1.1.3, were completed. In both cases, the tests did not produce results useful for directly calculating the critical fracture energy. In the case of the aluminum-coated aluminum samples, the fracture occurred almost instantaneously; in fact, the nickel-coated steel samples experienced significant plastic deformation before steady-state fracture could occur. Given these results, it is clear that the samples chosen for this research cannot be included in the narrow set of materials that can be properly evaluated with the four-point bend test. Nevertheless, some information was gained from the four-point bend test that was used in the later model and experimental work for interface characterization.

4.3.2.1 Test Results for the Nickel-Coated Steel Samples

Attempts to use the four-point bend test to evaluate the critical fracture energy of the nickel-coated steel samples failed, as excessive plastic deformation occurred before a pre-crack could be introduced. This plastic deformation was confined to the center of the specimen. As a result, steady state crack growth as a function of four-point indenter displacement could not be achieved. Because of the steady state crack growth requirement, the test could not be used to evaluate the critical fracture energy, highlighting a major shortcoming of the four-point bend test as it relates to interfacial characterization. In light of the difficulties associated with the application of the four-point bend test for measuring the critical energy release rate, the decision was made to test the unused specimens of the same dimensions using indentation experiments.
4.3.2.2 Test Results for the Aluminum-Coated Aluminum Samples

The aluminum samples, 7075 cold-sprayed aluminum on aluminum substrates, also proved to be poorly suited for four-point bend test evaluation. However, unlike the nickel-coated steel specimens, which showed excessive plastic deformation even before a pre-crack could be introduced, the aluminum samples experienced near instantaneous crack growth to the load span after initiation. Such instantaneous crack growth poses two problems in terms of using the four-point bend test. The test requires that a pre-crack be introduced, as the critical fracture energy is defined as the energy required for reinitiating crack growth at the tip of a pre-crack. In addition, the four-point bend test requires that the crack growth be steady and constant as a function of the applied load. As such, the aluminum samples presented enough problems for the four-point bend test to be considered an unworkable option for evaluating the critical fracture energy of the interface.

Clearly, the four-point bend test proved to be inadequate for critical fracture energy evaluation. However, it did result in crack growth, the initiation and behavior of which were strong functions of the bond strength and critical fracture energy. As such, the information from the four-point bend test, specifically the load at which the fracture was initiated and the brittle behavior of the fracture, are all functions of the interfacial properties. Therefore, once the cohesive zone properties have been measured based on any given test, the application of those properties to predict and compare results from another experiment provide a unique opportunity to validate the cohesive zone and the property measurement procedures.

For instance, the load displacement results of the four-point bend test of an aluminum-coated aluminum sample with a coating thickness of 1 mm are shown in Figure 4.17.
As can be seen from Figure 4.17, the load increases linearly as a function of the extension. After reaching a load of 500 N, the specimen failed asymmetrically. Further loading resulted in a second failure on the other side at approximately the same load. During each failure, the load experienced an instantaneous drop, attributed to the brittle nature of the cold-sprayed interface. Possible reasons for the non-symmetry of the fracture include the following: the specimen may not have been loaded symmetrically, the coating notch may have been sharper on one side, variations in coating thickness along the length of the sample, or the coating strength was not constant along the length of the sample. Regardless of the cause, the coating interface failed consistently at a bending load of approximately 500 N. In addition, it is reasonable to expect that the drop in load at the instant of interfacial failure would have been larger had the entire coating failed at the same time, as under such conditions the compliance would drop at one instant, instead of in two steps.
4.4 Modelling Results

4.4.1 Effects of Thermal and Pressure Transients (Cracked and Uncracked Tubes)

On the basis of the axisymmetric model described in Section 4.2.3, the coating thickness was varied by adding and subtracting 10% and 20% from the baseline value. In addition, the elastic modulus of the coating was varied by 20% and 40% to reflect likely variations. The parameters were cited by researchers as reported in Section 2.6 as important for coating performance. Table 4.2 shows the stress values obtained by the finite element analysis for the parameter ranges mentioned.

<table>
<thead>
<tr>
<th>Table 4.2. Stress components measured during finite element simulations.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Coating Thickness (mm)</td>
</tr>
<tr>
<td>-----------------------</td>
</tr>
<tr>
<td>Uncracked</td>
</tr>
<tr>
<td>8.40E-05</td>
</tr>
<tr>
<td>7.70E-05</td>
</tr>
<tr>
<td>7.00E-05</td>
</tr>
<tr>
<td>6.30E-05</td>
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<tr>
<td>5.00E-05</td>
</tr>
<tr>
<td>7.00E-05</td>
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<tr>
<td>7.00E-05</td>
</tr>
<tr>
<td>7.00E-05</td>
</tr>
<tr>
<td>7.00E-05</td>
</tr>
<tr>
<td>7.00E-05</td>
</tr>
<tr>
<td>Cracked</td>
</tr>
<tr>
<td>8.40E-05</td>
</tr>
<tr>
<td>7.70E-05</td>
</tr>
<tr>
<td>7.00E-05</td>
</tr>
<tr>
<td>6.30E-05</td>
</tr>
<tr>
<td>5.80E-05</td>
</tr>
<tr>
<td>7.00E-05</td>
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<tr>
<td>7.00E-05</td>
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<tr>
<td>7.00E-05</td>
</tr>
<tr>
<td>7.00E-05</td>
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</tbody>
</table>
The effects of coating thickness are arguably the most interesting from a design perspective, as the coating thickness can easily be changed during manufacturing. When the interface was intact, the changes in coating thickness had little appreciable effect on the stresses normal and parallel to the interface. On the other hand, even though it was still relatively small compared to the other stress components, the shear stress at the interface increased by 7% over the range in coating thickness. The normal and shear stress magnitudes as a function of coating thickness can be seen in Figure 4.18 and Figure 4.19, respectively.

![Figure 4.18: Maximum stress normal to the interface.](image)
In both cases, the stress normal to the interface remained compressive, but it did increase slightly with the elastic modulus. On the other hand, the tensile and compressive components of the stress parallel to the crack decreased, whereas the shear increased. None of the variations just mentioned was deemed significant from a failure perspective.

It should be noted that an important design consideration for tube and coating systems experiencing thermal and pressure transients is the resistance of the interface to crack separation and growth. In the first part of the analysis, the maximum stresses at a perfectly bonded interface were analyzed. Not surprisingly, the addition of the pressure transient proved beneficial in terms of eliminating the tensile crack opening stress for the entire period of interest. By eliminating tensile stresses at the interface, one of the major contributors to separating the coating from the substrate is prevented from having this effect. Given that the shear stresses at the interface constituted only a fraction of the overall stress, it is likely that for a perfectly bonded interface experiencing the prescribed thermal and pressure transients, a crack would not develop unless a
defect or delamination were already present. Hence, the next step was to introduce a crack-like defect to the model.

Not unexpectedly, the addition of a crack between the coating and the substrate produced larger stresses, as well as variations in their components (measured near the crack tip on the coating side of the interface) as the parameters were systematically changed. Compared to the stresses at the perfect interface, the addition of the defect increased the crack opening stress from compressive to approximately 300 MPa in tension; in contrast, the axial stress component (parallel to the interface) was largely unchanged. On the other hand, the shear stress increased four orders of magnitude.

As can be seen in Figure 4.19, an increase in the coating thickness resulted in a decrease in the maximum crack opening stress. In contrast, the shear-stresses increased slightly with the coating thickness. Again, the elastic modulus was adjusted by 20% and 40% to investigate their influence. In contrast to the earlier results for a perfect bond scenario, the maximum crack opening stress varied by more than a factor of two for the range of elastic modulus values tested. Moreover, the shear stress increased as a function of the elastic modulus. All the other stress components decreased as a function of the elastic modulus as collectively shown in Figure 4.20 and Figure 4.21.
After a crack had formed, the interface stress-state changed significantly with the crack opening tractions going from compressive to as high as 300 MPa. In addition, the shear stress increased by three orders of magnitude. It is likely that the increased stress-states observed via FEA are more than sufficient to grow the crack after initiation, especially under repeated firings. Though not studied in the current analysis, repeated firings and the accumulation of heat in the barrel...
might have lessened any protective effects of the thermal transients over time; out of phase pressure and thermal transients would have a similar influence.

When the coating thickness was decreased, the maximum crack opening and shear stresses in the coated tube diminished. However, there are clearly practical limits to decreasing the thickness of the coating. In fact, coating thickness should be sufficient to protect the substrate, especially if cracks are likely to form, as is usually the case. Hence, precautions must be taken in terms of preventing a crack from growing through the coating. Decreasing the elastic modulus also lessened the crack opening stress; however, the maximum shear near the crack increased. Unfortunately, changing the elastic modulus of the coating is not always practical or possible.

Clearly, there are conflicting considerations to account for in selecting coating type and thickness. Though they tend to produce lower interfacial stress, thinner coatings do not protect the substrate from the heat of the gases and may be more susceptible to cracks than thicker coatings. Softer coatings also tend to result in less interfacial stress; however, they produce larger shear stress, which could be problematic. Designers usually have little flexibility in tailoring the elastic modulus. As shown by the analysis, choosing optimum coating properties can be very beneficial in terms of minimizing crack-driving forces.

4.4.2 Thermal Loading of Indentation-Induced Flaws

A thermal-structural model was developed to analyze the effects of thermal transients on the pre-existing interfacial flaws just discussed. As stated in Section 5.1, the thermal finite element model reads the results of the indentation simulations. As such, all the results from the
indentation model, including those pertaining to the deformed mesh, plastic deformations, residual stresses, and the state of damage or failure of the cohesive zone elements were imported as the initial state. Adiabatic boundary conditions were assumed on all sides except at the center-top of the coating where convection was allowed over a radius of 0.0429 mm. During the simulations, the applied thermal loading consisted of a convective thermal transient via propellant gases at 3160 K, with a corresponding heat transfer coefficient of 193,000 W/m²·°K for approximately 1.0 ms. The short time span of the event necessitated the use of an explicit finite element formulation to capture any dynamic effects. Given the large range of temperatures experienced and the correspondingly strong dependence of material properties on them, temperature-dependent material properties were used. Figure 4.22 shows a close-up view of the top corner of the deformed mesh used as the initial conditions for the explicit thermal-structural model.

The indentation model resulted in a ring-type crack of the interface with concentric inner and outer radii. At the very center, bonding was maintained because of the high compressive stresses directly beneath the indenter.

![Figure 4.22: Deformed geometry after indentation.](image)
The effects of the bond-strength and critical fracture energy were also analyzed in terms of flaw growth under thermal transients for indentation-induced flaws. In order to reduce the number of interface parameters from four to two, Mode I and II fracture energies were coupled according to Equation (4.41), whereas the normal and shear strengths were related according to Equation (4.42).

\[ \Gamma(\Psi) = G_k \left[ 1 + (\lambda - 1)\sin^2 \Psi \right]^{-1} \]  

(4.41)

\[ N_{\text{max}} = \frac{1}{q\sqrt{2e}} \frac{\delta^c}{\delta_n} S_{\text{max}} \]  

(4.42)

As the phase angle increases so does the fracture energy, as represented by Hutchinson and Suo [116] and shown in Equation (4.41). The parameter \( \lambda \) governs the influence of Mode II fracture energy and is generally assumed to be a function of the surface roughness; for this study, the parameter \( \lambda \) was assumed to be 0.3, which is a common value for an interface of moderate roughness. In addition, the normal and shear strength of the interface can be considered manifestations of the cohesive zone model given by Abdul-Baqi et al. [87] as shown in Equation (4.42). The coupling parameter \( q \) was assumed to be 0.5 to relate the normal and shear strengths of the cohesive zone.

The normal strength and Mode I fracture energy were independently varied to analyze their effects on the initial flaw size after indentation, on any dimensional changes that occurred during
the thermal loading, and on the final overall size. As can be seen in Figure 4.23, the initial and final crack lengths decrease as functions of the critical fracture energy. Herein, the crack size after indentation will be referred to as the damage initiation crack size, and the growth during thermal loading will be referred to as damage evolution. The final crack size is the sum of the two crack lengths.

**Debond as a function of Critical Fracture Energy**

![Debond as a function of Critical Fracture Energy](image)

*Figure 4.23: The length of debonding as a function of critical fracture energy.*

At the lower values of critical fracture energy, the bonded area under the indenter is smaller and the outer crack radius is larger than at higher critical fracture energies. Whereas the initial damage initiation flaw becomes smaller as the crack inner radius increases for higher critical fracture energies, the outer radius actually decreases. After damage evolution, the inner radius was close to or equal to zero for smaller fracture energies, but it increased directly with the property. In contrast, the outer radius growth during damage evolution decreased with increasing critical fracture energy until there was no outward crack growth from the thermal loading. With the exception of the lowest values, crack growth during damage evolution decreased with increasing critical fracture energy.
As expected, increases in the critical fracture energy resulted in shorter crack lengths after damage initiation and evolution. The crack length from damage initiation decreased as a function of fracture energy as expected. Because indentation depth was held constant, the resulting fracture energy at the interface was not sufficient to grow a large crack. The damage evolution crack length change increases initially but then decreases as a function of the critical fracture energy. It was suspected that the initial increase is because the crack introduced during the indentation simulation was almost an order of magnitude larger than the other crack sizes and therefore, greater than the convection area. Hence, the thermal loading had less of an overall effect at the crack tip. It is likely that an initial crack, which terminates at the maximum interfacial stresses produced by the thermal loading, would maximize crack extension.

As the bond strength increased, so did the initial level of damage initiation; however, the crack length caused by damage evolution actually changed less as shown in Figure 4.24.

![Debond as a Function of Bond Strength](image)

**Figure 4.24:** The length of debond as a function of bond strength
After damage initiation as a function of cohesive strength, the inner radius initially increased, but then held constant, whereas the outer radius increased. After damage evolution, the bonded region maintained by the compressive stresses under the indenter disappeared, except for the highest bond strength evaluated. The post-damage evolution outer radius was approximately the same for all strengths. On the other hand, the amount of damage evolution decreased with increasing cohesive strength. The final crack length was approximately the same for all simulations, except when the bond strength was 4 MPa, which had a slightly higher final crack length.

The constant displacement depth of the indenter can explain the seemingly counterintuitive increase in the initial crack length with cohesive strength during damage initiation. Because the stiffness of the interface and total energy required to grow the crack (critical fracture energy) are constants, increasing the bond strength results in the expenditure of a larger portion of the required energy to grow the crack before critical strength is achieved. Subsequently, less energy is required to grow the crack to completion after damage is initiated, and the maximum separation, shown as $\delta_c$ in Figure 4.2, is also lower due to the higher bond strength. In addition, the crack length after damage evolution was found to be approximately constant for all the bond strengths evaluated. It is likely that the constant post-damage evolution crack radii is a result of the diminishing tensile/shear effects away from the convective heating that cannot extend crack beyond a radius of approximately 0.5 mm. This is due to diminishing thermal effects because of the constant radius of surface heating. Moreover, increasing the cohesive strength appears to have opposite effects on damage evolution and damage initiation. However, because of the complicated interaction between the initial crack length, the convective heating radius, and the bond strength, it is not wise to suggest that decreasing the bond strength will decrease damage evolution under thermal transients. In addition, after damage is initiated in the cohesive zone elements, the model elements must still overcome the critical fracture energy before the crack
can grow. In all likelihood, insufficient energy is available to grow the crack, even though the elements at the terminal crack length have sustained some degree of damage.

All the data generated in the critical fracture energy and bond strength study are compiled in Table 4.3.

Table 4.3. Debonded radii as a function of critical fracture energy and bond strength.

<table>
<thead>
<tr>
<th>Normal Strength (Pa)</th>
<th>Shear Strength (Pa)</th>
<th>Gic ((J/m^2))</th>
<th>Glc ((J/m^2))</th>
<th>Initial Inner Radius (m)</th>
<th>Initial Outer Radius (m)</th>
<th>Initial Debond (m)</th>
<th>Final Inner Radius (m)</th>
<th>Final Outer Radius (m)</th>
<th>Final Debond (m)</th>
<th>Change in Debond (m)</th>
</tr>
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<tbody>
<tr>
<td>1.0E+07</td>
<td>2.72E+07</td>
<td>5</td>
<td>11.35</td>
<td>4.24E-05</td>
<td>2.47E-04</td>
<td>2.04E-04</td>
<td>0.00E+00</td>
<td>5.08E-04</td>
<td>5.08E-04</td>
<td>3.04E-04</td>
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<tr>
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<tr>
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<td>11.35</td>
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<td>11.35</td>
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<td>2.47E-04</td>
<td>2.04E-04</td>
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<td>5.08E-04</td>
<td>5.08E-04</td>
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<tr>
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</tr>
</tbody>
</table>

4.4.3 Numerical Evaluation of Cohesive Zone Properties

The experiments described above were designed to provide information about the interface in order to determine the cohesive zone properties of the coating systems. Experiments on the nickel-coated steel samples were developed to provide the responses to a range of indentations. In addition, the aluminum-coated aluminum samples, which include results from the both four-point bend and indentation experiments, allowed for the cohesive zone properties evaluated in one test to be used to predict the results from another. The proposed procedure provides a
rigorous validation of both the indentation and cohesive zone model and a procedure to measure the properties.

The cohesive zone approach simulates all parts of the initiation and the evolution of damage. Accurately simulating one part of crack evolution, for example crack initiation, does not guarantee that the model will acceptably describe ensuing events. As such, accurately determining the properties that describe all the crack growth events necessarily provides confidence that the cohesive zone models the processes that contribute to crack growth, and not just a single event. It is to be expected that indentations to different load levels will result in varying crack-tip loading conditions. Therefore, modeling the crack growth at different indentation loads will provide a more robust description of the cohesive zone properties that accurately model crack growth in a coated sample.

Further validation of the indentation and cohesive zone models was provided by the four-point bend test results for the aluminum-coated aluminum samples. It was not possible to rigorously describe the important events of the crack growth process during the indentation tests, as was done for the nickel-coated steel indentation samples. However, the information gained from the two tests provides another opportunity to validate the indentation model, in addition to confirming the cohesive zone model’s ability to describe progressive crack growth with measured properties.
4.4.3.1 Cohesive Zone Property Measurements of the Nickel-Coated Steel Samples with Delamination

Delamination at various loads have been determined for the nickel-coated steel samples, as has information relating to the load at its onset based on in-situ acoustic emission analysis and on the post-test scan results of tests stopped at intermediate load levels. Delamination was expected to correspond to a high-energy acoustic event, the approximate time range of which can be investigated through finite element modeling. Experiments and finite element modeling will provide a thorough description of the delamination processes, from the crack initiation load, the evolution of the crack size as a function of the maximum load, and the final crack size after unloading.

Based on the empirically determined results, the described method proposes to evaluate cohesive bond strength and critical energy release rate by matching experimental results with finite element simulations. Such an analysis will be performed with simulations by choosing cohesive zone properties, running the indentation simulation described in Section 4.2.5, and comparing the experimental and numerical results. Such results include load-displacement data, delamination radius as a function of load, and approximate load at the initiation of delamination. Properties of the cold-sprayed nickel coating used in finite element modeling were obtained from Sen et al. [162].

Experimental indentations on the nickel-coated steel specimens were completed, the results of which are described in Section 4.3.1.1. Specimens were loaded to one of three loads, 2.6, 3.0, and 3.2 kN, before being unloaded. Three or more tests were conducted for each load level, before the delamination areas were measured and analyzed. Cohesive zone parameters were first chosen for the finite element model to match experimental results for the indentation
experiments loaded to 3.2 kN and applied to predict delamination sizes for lower load levels. The results agreed reasonably well, but required further refinement. For the first iteration of the simulations, the critical energy release rate used in the simulations was 320 J/m$^2$ and the strength for the simulations was 70 MPa, the chosen strength based on information provided by the manufacturer, and was evaluated according to ASTM standard C 633 [163]. A two-parameter cohesive zone property was chosen for its simplicity for the first attempt to determine the cohesive zone properties matching the experimental results. Delamination agreement was improved in subsequent simulations by adjusting the critical fracture energy release rates in the normal and shear modes. Agreement was optimized using the manufacturer’s reported strength, Mode I critical energy release rate of 175 J/m$^2$, and the Mode II critical energy release rate of 475 J/m$^2$. As per the axisymmetric assumption, the problem was independent of the Mode III critical energy release rate. Agreement could not be improved any further by adjusting any of the cohesive zone properties. Final flaw dimensions showed good agreement with experimental results as shown in Figure 4.25.

The average of the experimental results is represented by the blue diamond, and the range of the experimental results is shown by the bar. The finite element results slightly over predicted both the inner and outer diameter of the delamination for the samples loaded to 2.6 kN. However, the predictions of the flaw dimension produced by higher loads were quite reasonable. A sample c-scan is shown in Figure 4.26 with the numerically evaluate flaw diameter overlaid on the scan image.
Figure 4.25: Comparison of experimental and finite element results of (a) inner and (b) outer delamination diameter of nickel-coated steel.
Good agreement was also seen between the delamination diameters for the three different sets of experiments. As such, properties that produce accurate results in one test are able to predict results in others conducted at different loads, for which crack-tip loading conditions involved different combinations of shear and normal loading. The simulations suggested that crack initiation occurred at approximately 2.6 kN, whereas the other samples that also fractured did so during unloading, as the crack initiated immediately during this process. For all cases of crack extension during unloading, growth occurred at the inner radius, with the outer radius remaining constant. High interfacial shear stresses during unloading drive crack growth at the inner radius, which occurs while the interface is still under high compressive stress. The contribution from Mode I loading increased as the crack grew and the interface separated. Given the patterns of crack onset and extension direction at the various load levels, the direction, and the extent of crack propagation are functions of the load at which crack growth is initiated. A crack that initiated grew outward until loading stopped, at which point crack growth at the inner radius
occurred during unloading. In other cases for which the load did not exceed 2.6 kN, crack growth initiated during unloading and extended at the inner radius. For both scenarios, the locations for crack initiation occur at approximately the same radial location; the cracks that initiated during loading did so at a radius of 1.33 mm, as compared to a radius of 1.30 mm for those initiated during unloading because the critical load was not reached during loading.

Two cases of indentations, one loaded to 2.4 and the other to 3.2 kN were analyzed to evaluate the effects of increased loading on the final state of the interface and to compare the effects of different factors on crack growth. The finite element load-displacement plots are shown below in Figure 4.27.

![Load as a Function of Displacement](image)

Figure 4.27: Load-displacement results from finite element simulations of an indentation of 2.0 mm made by an indenter in a nickel-coated steel sample.
As noted, at lower loads the crack initiates during unloading, whereas at higher value, the crack starts during loading and grows outwardly until unloading begins, at which point it extends inwardly. In addition to the crack growth, significant plastic deformation was introduced to the coating and substrate as a result of the indentation. The final von Mises stress state is shown below for the two cases analyzed in Figure 4.28.

Figure 4.28: Comparison of von Mises stress plots from finite element simulations of indentations of 2.0 mm made by an indenter loaded at (a) 2.4 kN and (b) 3.2 kN.
A major difference between the two plots is the significantly larger amount of plastic deformation in the nickel coating for the sample loaded to 3.2 kN. Not unexpectedly, the stresses are also much higher for the latter case, with the highest magnitudes occurring directly underneath the indenter in the steel. The differences in stiffness between the coating and substrate can also be seen in the plots. Deformation due to the indentation was almost entirely limited to the coating, with just a small amount of plastic strain in the substrate because the substrate is quite rigid compared to the coating under plastic deformation. In the general case of a stiff substrate before delamination, the interface experiences mainly shear loading as the coating is pushed away from the center. Once a sufficient deformation occurs, usually after crack growth, during either loading or unloading, the coating begins to lift off the surface, at which point normal loading becomes a significant factor. The equivalent plastic strain is shown in Figure 4.29 and Figure 4.30.
Figure 4.29: Comparison of the equivalent plastic strain for the indentation of 2.4 kN on (a) coating and (b) substrate.

Figure 4.30: Comparison the equivalent plastic strain for the indentation of 3.2 kN on (a) coating and (b) substrate.
The plastic strain in the coating is orders of magnitude higher than that of the substrate. In the case of the nickel-coated steel, the substrate is essentially rigid, which forces significant shear strain in the interface and normal strains once crack growth begins. However, for samples where the coating and substrate stiffness are similar, such as the aluminum samples, the coating and substrate experience comparable strains and the relative shear motion at the interface is significantly lower. In the latter case, the system cannot build up the necessary fracture energy to separate the surfaces until after significant loading and deformation. In addition, in many such cases crack growth occurs during unloading as the coating recovers some elastic strain. Such conditions will be analyzed in the context of indentation tests of aluminum cold sprayed onto aluminum.

Also of interest is the condition of the interface following the indentation as the load increases. As the goal of the research is to analyze the evolution of a flaw when subjected to thermal and pressure transients, the state of the delamination before the application of the second stage of loading is of particular interest in understanding the evolution of the flaw. It would be expected that the extent of the damage at the interface to increase, in addition to the crack size, as a function of maximum indentation load. However, this was not necessarily the case. As can be seen in Figure 2.1, the damage variable, defined in Equation (4.6), is largely unaffected by the increase of load from 2.4 to 3.2 kN.
Figure 4.31: A plot of the damage variable, \( d \), of the interface between the nickel coating and the steel substrate as a function of distance from the center of the indentation.

As can be seen from the plot, the region under the indenter does not experience any damage. High compressive stresses and the absence of shear displacements prevent damage at the interface. Moving away from the center, damage increases rapidly, until it reaches a maximum value. In both cases, the damage increases at the same rate when moving away from the center. Finally, at a radius of approximately 2.5 mm, the damage variable rapidly decreases toward zero. The simulation of the lower indentation load has a slightly smaller region of damage, whereas the damage variable as a function of radial position decreases at a similar rate of change. The results of the simulations suggest that once the interface begins to decohere, further loading contributes less to damage at the interface. The area of delamination changes for samples loaded to higher loads; however, these results suggest that the sample already experienced significant interfacial damage once delamination had begun. Additionally, it is likely that further loading, such as the thermal and pressure loading associated with gun tube coatings, will produce similar results for the different load values, with all other things being constant.
4.4.3.2 Acoustic Emission and Finite Element Results

The finite element simulations described above can be used to further interpret the acoustic emissions results described in Section 4.3.1.1 for nickel-coated steel specimens. From these simulations, it can be seen that the delamination begins during unloading for the samples indented to 2.4 kN, and begins again at around 2.6 kN for the samples loaded further. Therefore, it is unlikely that high-energy acoustic emission hits generated earlier are the result of interfacial cracks. Instead, it is reasonable to conclude that these hits are created by plastic deformation and/or cracking in the nickel coating during indentation. As shown in Figure 4.32, significant cracks and other surface features exist in the samples, and these could explain the high-amplitude acoustic emissions throughout the test.
Figure 4.32: Stereomicroscopic images of indentations showing cracks and other surface features for samples indented to (a) 2.4 and (b) 3.2 kN.
As shown in the figure, significant cracking can be seen at the edges of the indentations for samples loaded to both 2.4 and 3.2 kN. It is likely that such cracking corresponds to a large energy acoustic emission event and would be very difficult to discern from a delamination type. The finite element simulations did not predict the onset of delamination at load levels below approximately 2.6 to 2.8 kN; therefore, during loading, one would expect to see high-energy events occurring around the same level as in the experimental results shown in Figure A.8, Figure B.2, Figure B.4, Figure C.15, and Figure C.17. As also shown in these figures, a relatively high-energy event occurs quite consistently at around 3.0 kN, which from the finite element simulations, is the load range at which delamination occurs. Whereas some earlier events were also accompanied by larger amounts of acoustic energy, the level of the events that occur later in the test are still above the background floor. As a high-energy acoustic event consistently occurs at approximately the same point, delamination is expected to initiate during the indentation experiment. It is therefore likely that this event coincides with the onset of delamination.

The experimental results were compared to those from the finite element models with varying parameters to determine the cohesive zone properties of the thermally sprayed nickel on steel samples. Cohesive zone properties were chosen that accurately describe indentation-induced crack growth for samples under different load levels, though the simulations featured crack growth caused by different mechanisms. As mentioned before, the c-scan results varied significantly and were difficult to measure. In addition, for various load levels, agreement was observed between the predicted onset of delamination and significant acoustic energy events. Given this reasonably strong agreement and the possible sources of uncertainty in the experimental results, the properties measured through finite element simulations were judged to reasonably model the interface. In addition, the properties were judged to constitute a sufficient basis for further investigating the response of the coating system to different loading conditions, such as thermal and pressure transients.
4.4.3.3 Cohesive Zone Property Measurement and Validation of the Aluminum-Coated Aluminum Samples

Indentation, four-point bend experiments, and simulations were completed in an effort to validate the approach used to evaluate the cohesive zone properties and the indentation model. Samples expected to provide useful information in both tests included aluminum-coated aluminum samples that were shaped into both a four-point bend and indentation specimens, as described in Section 4.3.2.2. Four-point bend and indentation results, which were already described above, were used in conjunction with finite element models to evaluate the properties by way of the four-point bend test. Once measured, the properties were used to predict the results of the indentation tests in order to compare them with the other experimental results. Experimental measurements of final flaw dimensions as a function of coating thickness and load, and the onset of delamination were both compared with numerical simulation results to validate the finite element models and the method used to measure the properties.

The results from the four-point bend tests, as described in Section 4.3.2.2 were used to determine the cohesive zone properties, in conjunction with the finite element model detailed in Section 4.2.4. Geometric and material property data were input into the finite element model to simulate the exact conditions of the four-point bend experiments. Once the finite element model parameters were adjusted to simulate the experiment, boundary conditions shown in Figure 4.17 were applied based on the experimental results. Preliminary expectations were that, given the sudden and instantaneous nature of the crack growth, inertial contributions would be significant. Therefore, given the abrupt crack growth and the inertial contributions, the model was adjusted to include dynamic effects. In addition, the brittle nature of the interfacial crack growth suggests that the fracture energy is minimized. In order to minimize fracture energy, the traction-
separation curve shown in Figure 4.2 was truncated such that once the cohesive zone reaches the maximum stress, it immediately fails. In other words and in reference to Figure 4.2, the cohesive zone was defined such that $\delta_{0}^{n}$ was equal to $\delta_{0}^{c}$. Additionally, the stiffness was defined according to Equation (2.6). Residual stresses were included, since they have the potential to significantly affect the crack growth. Residual stresses were calculated using Equations (2.41) and (2.42), and based on the curvatures of the sample surface before and after separation. A plot of the applied residual stress distribution and the curvature measurements used to calculate them are shown in Figure 4.33.

As shown in Figure 4.33b, the curvatures are largest for the composite beam and the coating, though the curvatures are in opposite directions. Such results are expected, as the residual stresses result from the strain required to maintain equal displacements across the interface. In addition, after separation, the substrate retains very little of the deformation. The residual stress distribution, as described above, assumes a linear variation, is largely tensile in the substrate, and compressive in the coating. Such behavior agrees with experimental observations, for which the composite beam bent away from the spray nozzle. In such a case, the substrate resists the curvature and keeps the coating in compression. Measured residual stress distributions were applied to the finite element model by means of a user-defined subroutine. With all other parameters defined, finite element simulations were conducted with the four-point bend model to attempt to best reproduce experimental results by varying the normal and shear strengths.
The cohesive zone properties, specifically the shear and normal cohesive strengths, were then adjusted in order to reproduce the experimental results. The measured cohesive zone properties accurately reproduced the load at the initiation of crack growth, fracture behavior, and the resulting load as a function of displacement. The normal and shear cohesive zone strengths that best reproduced the experimental results were then determined to be 140 and 2,000 MPa, respectively. A comparison of the experimental and finite element results is shown in Figure 4.34.
As shown in figure, excellent agreement was obtained. Because the elastic modulus of the cold-spray coating was unknown, and in all likelihood differed significantly from that of the bulk 6061 aluminum, it was evaluated using finite element modeling. In order to determine the coating elastic modulus, known material properties were input and the coating elastic modulus varied until the initial stiffness matched the experimental results. The resulting elastic modulus was 32 GPa. The experimental and numerical results both delaminated at the same load, and they have similar fracture behavior in terms of instantaneous fracture. As expected, the extremely low critical fracture energy resulted in brittle instantaneous failure in the simulation, which was in accord with the matching experimental observations. A difference in load drop between the experimental and the finite element observations are likely to be a result of the sides of the sample having failed sequentially in the experiment (Figure 4.34), whereas the model simulated symmetric failure. As such, the load drop was twice as large for the model because it experienced twice the decrease in stiffness.
The cohesive zone parameters obtained from the four-point bend simulations described above were used to model indentation tests in an effort to reproduce experimental results. Experiments presented in 0, were conducted on specimens nearly identical to the four-point bend samples, the only difference being coating thickness. All properties, including those of the cohesive zone (discussed above), were input into the model to attempt to reproduce experimental results with parameters measured from entirely different specimen geometry and loading conditions. The load and coating thickness combinations of all the specimens tested experimentally were input into the finite element model, with simulation and experimental results summarized in Table 4.4.

<table>
<thead>
<tr>
<th>Sample Name</th>
<th>Indenter Diameter (mm)</th>
<th>Maximum Load (kN)</th>
<th>Coating Thickness (mm)</th>
<th>Average Delamination Radius (mm)</th>
<th>FEA Delamination Prediction (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>Amplitude</td>
<td>Time of Flight</td>
</tr>
<tr>
<td>A1</td>
<td>4.8</td>
<td>5.4</td>
<td>0.37</td>
<td>No delamination observed</td>
<td>No delamination Predicted</td>
</tr>
<tr>
<td>A2</td>
<td>2</td>
<td>1.02</td>
<td>0.58</td>
<td>No delamination observed</td>
<td>No delamination Predicted</td>
</tr>
<tr>
<td>A3</td>
<td>2</td>
<td>4</td>
<td>0.58</td>
<td>No delamination observed</td>
<td>No delamination Predicted</td>
</tr>
<tr>
<td>A4</td>
<td>2</td>
<td>2</td>
<td>0.37</td>
<td>No delamination observed</td>
<td>No delamination Predicted</td>
</tr>
<tr>
<td>A5</td>
<td>2.5</td>
<td>4.5</td>
<td>0.37</td>
<td>No delamination observed</td>
<td>No delamination Predicted</td>
</tr>
<tr>
<td>A6</td>
<td>2.5</td>
<td>4.6</td>
<td>0.5</td>
<td>No delamination observed</td>
<td>No delamination Predicted</td>
</tr>
<tr>
<td>B1</td>
<td>2</td>
<td>5</td>
<td>0.89</td>
<td>3</td>
<td>2.55</td>
</tr>
<tr>
<td>A2</td>
<td>2</td>
<td>4.75</td>
<td>0.89</td>
<td>2.885</td>
<td>3.23</td>
</tr>
<tr>
<td>A3</td>
<td>2</td>
<td>5</td>
<td>0.89</td>
<td>3.035</td>
<td>3.33</td>
</tr>
</tbody>
</table>

Finite element simulations of indentation separation in terms of occurrence and size of the outer radius matched the c-scans of the experimental results very well. For the indentations of the coatings thinner than 0.89 mm, the system lacked the interfacial stresses required to initiate a delamination. For coatings thicker than 0.89 mm, though indented to similar loads, were able to overcome the interfacial strengths and experience a delamination.
As shown in Table 4.4, the numerical results agreed more closely with the delamination radii measured from the amplitude plots than with the time of flight results. Based on the reasonable agreement observed in the nickel-coated steel specimens, it was expected that the amplitude scans provide the best measure of the delamination at an interface. Amplitude scans of the first interface reflection will, therefore, be used to quantify the presence and dimensions of interface separation radii for ball indentations on aluminum cold sprayed onto aluminum samples. The results in Table 4.4 show reasonable agreement with experimentally measured values. In effect, the simulations consistently predicted the presence or absence of an interfacial crack for all load and coating thickness combinations. When conditions of the indentation test were such that a delamination was formed, finite element simulations were accurate in predicting its outer radius according to the first interface amplitude scan results.

Finite element simulations also appear to be accurate at predicting the load for the onset of delamination. According to the acoustic emissions plot given in Figure D.17, large energy events occurred at a load of approximately 450 N in the unloading step. During finite element simulations, the delamination began at approximately 400 N and continued as the load was reduced to zero. Such results are also consistent with observations from acoustic emission recordings during loading, as no delamination-type event stood out from the noise of the system, suggesting that any delaminations that were present occurred during unloading. While the load is being removed, the compressive stresses that maintained the interface are dissipating, as the coating and substrate recover their respective elastic strains. For cases where the elastic recovery is enough to create interfacial stresses, the combination of which satisfies Equation (4.9), the specimen will delaminate during load removal.
When compared to the simulations of the indentations described in Section 4.4.3.1, the aluminum cold sprayed onto the aluminum substrates experienced significantly more deformation at the interface, as can be seen in Figure 4.35. The larger deformation is due to the similarities in stiffness, which allow more strain to be transferred into the substrate than for cases in which the substrate is stiffer than the coating. Both coating and substrate were also softer than in the previous case, especially in the case of the difference in stiffness between the steel and aluminum substrates for both elastic and plastic deformation. Figure 4.35 shows indentations of 5.4 and 5.0 kN, indented into substrates of 4.8 mm (3/16 in), with coatings of thicknesses equal to 0.37 and 0.89 mm, respectively.

The simulation shown in Figure 4.35a did not result in a delamination, while the sample shown in Figure 4.35b did, despite the former being subjected to a lower indentation load. Such results underscore the significance of coating thickness for the aluminum samples. Despite the presence of higher stresses in both the coating and substrate in the indentation of the thinner coating, a delamination was not observed, whereas in the sample with the thicker coating, the effective stiffness of the coating created the interfacial stresses necessary to separate the coating from the substrate during unloading.
Figure 4.35: Comparison of finite element simulations of aluminum cold sprayed onto aluminum, with (a) coating thickness of 0.37 mm, indented to a load of 5.4 kN (b) coating thickness of 0.89 mm, indented to a load of 5.0 kN.

As mentioned above, the indentation of the sample with the thin coating was to a higher load. In addition, because of less coating penetration and more loading in the substrate, this sample also had higher indentation stiffness. The slope of load as a function of displacement, shown in Figure 4.36, was higher for indentations in samples with thinner coatings, reflecting the higher stiffness of the substrate when compared to the coating.
Based on the results of the two simulations described above, it can be observed that the coating thickness appears to have a significant effect on separation during indentation testing of the aluminum cold sprayed onto aluminum samples. Such an effect can be described by the effects of residual stress and the relaxation of elastic strains and their effects on the interface. Plots of the normal and shear stresses in the cohesive zone are shown in Figure 4.37.
Figure 4.37: Comparison of finite element simulations of aluminum cold sprayed onto aluminum showing normal and shear stresses in the cohesive zone for samples with (a) 0.37 mm thick coating and (b) 0.89 mm thick coating.

The plots show the normal and shear stress in the cohesive zone at the instant crack growth starts in the sample with a coating thickness of 0.89 mm; crack growth did not occur in the simulation of the thinner coating, but the image is from the same load level during unloading. In both plots, the normal stress is shown in red. High compressive stresses occur directly under the indenter in both simulations. However, only tensile stresses contribute to Mode I crack growth in the cohesive zone model. Looking at Equation (4.9), it can be seen that the normal and shear stresses combine to contribute to crack growth according to a power law, where each component is
divided by the respective strength, and then squared. According to the properties obtained from the four-point bend testing and simulations, those values are 13 and 1400 MPa, respectively. Recognizing this, and the fact that the tensile stresses are larger in magnitude than the shear stresses, one would expect the normal stresses to contribute the most to fracture, relative to the shear components. Unlike the simulations of the steel specimens with a nickel coating, where the fracture is dominated by the shear stresses, the interfacial separation of the cold-sprayed deposit from the aluminum substrate is driven almost entirely by tensile components. Whereas the hard steel coating underwent very little deformation causing higher shear stresses at the interface as compared to the nickel surface, the aluminum coating and substrate share the deformation.

Normal stress, shown in Figure 4.35a, is almost a third of the stress recorded from the simulation shown in Figure 4.35b. In addition, as observed in Figure 4.35b, the shear and tensile stress peaks are at approximately the same location at 2.35 mm from the center of the indenter. Interfacial separation continues from the initiation point in both directions, driven mostly by high tensile stresses at the interface.

After the loading was removed, significant plastic strain was observed in the simulations. It is likely that the plastic strain contributes to the crack growth during unloading. Interfacial damage also occurs during unloading. As such, it is the result of the relaxation of the elastic stresses. Plastic stress and strain is, therefore, a function of unrecovered deformation. Images of the plastic strains after unloading are shown in Figure 4.38 and Figure 4.39 for simulations with coatings 0.37 and 0.89 mm thick, respectively.
Figure 4.38: Comparison of finite element simulations of aluminum cold sprayed onto aluminum, with (a) coating thickness of 0.37 mm, indented to a load of 5.4 kN (b) coating thickness of 0.89 mm, indented to a load of 5.0 kN.
Figure 4.39: Comparison of finite element simulations of aluminum cold sprayed onto aluminum, with (a) coating thickness of 0.37 mm, indented to a load of 5.4 kN (b) coating thickness of 0.89 mm, indented to a load of 5.0 kN.

As the load is removed, plastic strain under the indenter pulls the coating down at the center. In both simulations, the highest residual strains were seen in the coating, but the values at the center of the substrate were also significantly higher than those in the surrounding material. The high strains at the center also create a lip at the edge of the crater. High tensile stresses at the interface are the result of the interface resisting the coating’s tendency to deformation required to maintain
bonding over the interface of the substrate. Deformation at the edge of the indentation crater is shown in Figure 4.40.

![Graph showing surface displacement as a function of radial distance.]

**Figure 4.40:** Normal displacement of the substrate surface for a coating thickness of 0.89 mm, indented to a load of 5.0 kN.

Thinner coatings have less stiffness, resulting in lower tensile stresses at the interface. On the other hand, the thick coatings such as those shown in Figure 4.35b and Figure 4.39 have higher effective stiffness and offer more resistance to conforming to an irregular interface, resulting in high tensile stresses at the interface. A comparison of Figure 4.40 and Figure 4.37 shows that the highest tensile stress corresponds to the area just outside the area of the greatest normal displacement at the interface.

Crack growth in the simulations of samples with 0.89 mm thick coatings were analyzed in two simulations to match the experiments of the samples loaded to 4.75 and 5.0 kN. Unlike the nickel-coated steel samples which were determined to have relatively large cohesive zone
fracture energies, the samples of aluminum cold-sprayed onto aluminum were determined to have fracture energy proceeding only from the elastic loading contributions. As the aluminum samples failed directly after satisfaction of Equation (4.9), the damage variable must be either zero or one. A plot of the damage variable for samples with a coating thickness of 0.89 mm loaded to 4.75 and 5.0 kN is shown in Figure 4.41 below.

![Figure 4.41: Normal displacement of the substrate surface for a coating thickness of 0.89 mm indented to a load of 5.0 kN.](image)

The damage variable transitions abruptly from zero to one, as shown in the figure. As expected, the sample loaded to 5.0 kN has a significantly larger crack size than the sample under a lower load. It is likely that the effect is due to the observations regarding the interface described in the preceding paragraphs. Loading the sample further creates greater deformations in the substrate, which in turn creates higher tensile stresses across the interface outside the indentation crater. Hence, larger tensile stresses over a greater area result in a bigger crack size. There is also a cohesive element at the edge of the crack in the 5.0 kN load sample that did not fail. It is likely that the normal and shear stresses at this point did not combine to satisfy Equation (4.9), though the element next to it did.
The conditions pertaining to interfacial damage, failure, and crack growth were analyzed for the indentation tests. The aluminum samples were found to delaminate under different conditions than the nickel-coated steel specimens. In both sets of materials, the cohesive zone properties were measured. A four-point bend test was used to evaluate aluminum-coated aluminum cohesive zone properties, which were then successfully applied to predict the onset and size of the delaminations to those of identical samples subjected to indentation loading. The cohesive zone properties were also evaluated using a similar method as that used for the nickel-coated steel specimens, which accurately modeled the initiation and growth of an interfacial crack for three different indentation loads. Indentations in the nickel-coated steel specimens created significant deformations in the relatively softer coating, which resulting in high shear stresses at the interface. Delaminations were observed during loading for samples at higher loads, whereas the other samples delaminated during unloading under simulation conditions.

The cold-sprayed aluminum on aluminum substrates presented a completely different material set for exploring the application of the cohesive zone modeling. Brittle fractures were observed in the four-point bend tests, which were used to evaluate the cohesive zone parameters. Properties were applied to the indentation tests and found to accurately model crack growth. The steel substrate was found to be stiff relative to the coating in the other set of samples; however, the aluminum coating and substrate had similar material properties and were found to delaminate by normal and shear mechanisms during indentation unloading.

Given the thorough property measurement and validation procedure, the method of indentation experimentation and finite element modeling were found to reasonably determine the cohesive zone properties. In addition, the indentation test was found to adequately introduce damage to
the interface of the coating systems for further study under thermal and pressure transient loading. The resulting damage was studied in order to contribute to understanding of the initial conditions and flaw evolution of the coating system when subjected to live gun tube firing boundary conditions.

4.4.4 Parameteric Study of Variations in Cohesive Zone Parameters

A parametric study was conducted on the indentation finite element model in order to ascertain the effects of variations in cohesive zone parameters on resulting delamination dimensions. Two indentation force values were studied in order to observe the response of the indentation induced delamination to variations in the cohesive zone properties. Because of the role the indentation finite element model served in the property measurement method, it was necessary to understand the effects of parametric variations of cohesive zone properties. Cohesive strength, Mode I critical energy release rate, Mode II critical energy release rate, and interfacial stiffness were varied independently in order to ascertain the trends at the measured cohesive zone parameters.

The interfacial cohesive strength was first varied. Because the measured strength was 70 MPa, the sensitivity to further changes in the strength were evaluated in the proximity of this point. As described earlier, cohesive strength was assumed to have been mode independent. The sensitivity of changes in cohesive strength on the inner and outer delamination radii are shown in Figure 4.42.
Figure 4.42: (a) Inner and (b) outer radii as functions of interfacial cohesive strength. Experimentally measured values are shown as black circles.

In both loading cases, with loads to 3.0 and 3.2 kN, the inner and outer radius increased with cohesive strength. Such a situation was observed in the simulations described in Section 4.4.2, due to the greater energy expended during elastic loading. As the total cohesive energy is a parameter independent of cohesive strength, higher strength directly increases the portion of elastic energy in the cohesive zone, resulting in less fracture energy resistance after reaching the cohesive strength. The increased elastic energy results in more brittle interfacial behaviour and
less interfacial separation before fracture. In the case of the indentation simulation, the relationship resulted in larger flaw dimensions with increased cohesive strength.

Similar to the cohesive strength, the interfacial stiffness was assumed to have been mode independent. Delamination inner and outer radii as functions of the interfacial stiffness are shown below in Figure 4.43. As can be seen in the figure, the delamination dimensions were relatively independent of the interfacial stiffness, except in the cases of extremely high stiffness, for which case both the inner and outer flaw radii were significantly larger than for other stiffness values. A slight increase in the outer radius with increasing stiffness can be seen in Figure 4.43b, due to the effects described in the cohesive strength discussion above.

Sensitivity of changes in Mode I critical energy release rate on the inner and outer delamination radii are shown in Figure 4.44.
Figure 4.43: (a) Inner and (b) outer radii as functions of interfacial stiffness. Experimentally measured values are shown as black circles.
As expected, increasing the Mode I critical energy release rate resulted in smaller flaws. The indentation-induced flaw was sensitive to increasing Mode I critical energy release rates until approximately 200 J/m$^2$ when loaded to 3.2 kN. However, when a 3.0 kN load was simulated, the Mode I critical fracture energy was only sensitive to 175 and 125 J/m$^2$ at the inner and outer radii, respectively. As described in Section 4.4.3.1 Mode I component of the measured critical energy release rate was 175 J/m$^2$. The results suggest that the evaluated energy is in a region for
which the delamination radii were functions of variations in Mode I critical fracture energy for
the simulation of 3.2 kN indentation force, and to a lesser extent an indentation force of 3.0 kN.

Similar to the Mode I critical energy release rate, the Mode II critical energy release rate was
varied. The inner and outer delamination radii as functions of Mode II critical energy release rate
are shown below in Figure 4.45.

![Figure 4.45: (a) Inner and (b) outer radii as functions of Mode II critical energy release rate. Experimentally measured values are shown as black circles.](image-url)
Similar observations to those made for the Mode I critical energy release rate also applied in Mode II. The inner radius increased with the Mode II component, while the outer decreased. The outer radius was a function of increasing Mode II critical energy release rate for all values tested, while the inner radius was much less sensitive, showing only one jump in radius as the Mode II component of the critical energy release rate increased. As mentioned in Section 4.4.3.1 the Mode II component of the measured critical energy release rate was 425 J/m$^2$. The results shown in Figure 4.45 suggest that the evaluated energy is in a region for which the delamination outer radii, and to a lesser extent, inner radii, were functions of the Mode I critical fracture energy.

Because the model assumed axisymmetry, the results were independent of the Mode III component of interfacial fracture energy. The results described indicate the sensitivity of the cohesive zone parameters in the range of the values measured by the method. Additionally, delamination dimensions appear to be relatively independent of the cohesive zone stiffness, except for the case of extremely high values.
CHAPTER 5. FLAW EVOLUTION

5.1 Thermal Loading on a Coating with an Interfacial Pre-crack Model

A thermal-structural model, the input file for which is included in Appendix B.b was developed to analyze the effects of thermal transients on indentation-induced interfacial flaws. Unlike the previously mentioned Abaqus finite element models, the thermal model was written in Abaqus input file format language because the *import command used to interpolate the finite element results from the implicit indentation was not available in Python scripting. The approach used here is unique in that it includes all the effects of the damage initiation process and provides a complete description of the coating, interface, and substrate state before thermal transients are be applied.

For the required implicit-to-explicit analysis to be performed, the indentation model must be run first and its results stored in a *.odb (output database file). During the explicit analysis, the results, including plastic deformations and residual stresses, were applied as the initial state. The state of damage for cohesive zone elements at completion of the indentation phase became the initial conditions and failed elements were not imported. Only the implicit element types need to be redefined as explicit elements, as the deformed mesh of the coating, substrate, and interface was imported. Explicit, linear, axisymmetric elements, designated as CAX4RT, were used for the coating and substrate. The elements had both thermal and structural degrees of freedom and employed reduced integration and hourglass control; all cohesive elements were four-node, axisymmetric, explicit elements, and were designated as COHAX4. During the analysis, failed elements were deleted in order to easily monitor crack growth.
Insulated boundary conditions were applied to all sides, while a flux was designated to a predefined area on the surface at the center of the disk to simulate laser heating. Structural boundary conditions from the model described in Section 4.2.5 were maintained. The short period during which the simulated laser heating took place, on the order of microseconds, necessitated the use of the explicit finite element formulation to capture the dynamic effects. In order to govern the conduction of heat across the interface as the coating and substrate separate, a relationship for thermal conduction was used across the cohesive zone elements. In recognition of the large range of temperatures experienced and the strong dependence of material properties on temperature, temperature-dependent material properties were used.

As a crack grows, the coating separates from the interface. Separation contributes to decreased gap conductance across the interface, as direct conductance is no longer possible. Heating the coating increases the gap distance, which results in even higher temperatures in the coating, as less heat is conducted into the substrate. Such an effect increases the distance between the interfaces further. Given the positive feedback associated with increased interface gap distance, its effects are likely to contribute to crack growth and therefore cannot be ignored. As such, the effects of gap conductance were included using the approximation of Yovanovich [164], and assuming the properties of air for the gap between the interface. A plot of the gap conductance as a function of gap clearance is shown in Figure 5.1.
As can be seen in the figure, the gap conductance starts out being large, but asymptotically approaches zero. This is to be expected, as when the interface is direct contact, there is almost no thermal resistance. As the separation distance increases, conductance decreases, until almost no heat can be transferred across the interface.

Because of the strong influence of pressure on crack growth, pressure on the newly formed crack faces was implemented with an Abaqus user-subroutine, VDLOAD. During the thermal cycling and resulting thermal contraction and expansion of the coating, cracks can open up through the coating thickness. The cracking may allow combustion gases to penetrate the coating and become trapped at the interface after the crack closes during the thermal expansion of the coating. Internal pressure contributes to the cracking and promotes Mode I opening at the crack tip. In addition, the gases, if heated and cooled by these thermal events, provide a cyclic contribution to both crack growth. Given that the interfacial crack growth will form new faces, the internal pressure boundary loading will grow to cover them. Because the resulting interfacial crack growth is a function of the area to which the pressure was applied, the pressure distribution as a function of time had to be determined iteratively. After the model was solved, the pressure...
was adjusted in order to account for the crack growth from the previous iteration. The procedure was repeated until satisfactory agreement was seen between the crack tip and the applied pressure. An example user-subroutine is given in Appendix B.e.

Figure 5.2 shows a close-up view of the top inside corner of the deformed mesh used as the initial conditions for the explicit thermal-structural model.

![Figure 5.2: Initial and boundary conditions for thermal and pressure transient simulation.](image)

The indentation simulation resulted in a ring-type crack of the interface with an inner and outer radius. The very center maintained bonding because of the high compressive stresses directly beneath the indenter. The thermal flux was applied on the coating surface at the center of the disk.
5.2 Flaw Evolution Modelling Results

The evolution of indentation-induced flaws was also analyzed using the finite element method. An explicit finite element model as described in Section 5.1 was applied in a sequential fashion where finite element results of a ball indentation implicit finite element analysis were imported. The indented state was then used as the initial conditions for studying the evolution of thermal and pressure transients. Such a method applies all the effects resulting from the indentation process, including plastic deformation, crack growth, and cohesive zone element damage as initial conditions in the thermal-structural explicit simulation. Studies were conducted for which a flaw was introduced through indentation via an implicit simulation. The results were imported into an explicit model to study interfacial damage and crack growth under severe thermal and pressure transients. Flaw evolution was studied in the nickel-coated steel specimens, for which the cohesive zone property measurement was just discussed. An initial flaw with inner and outer radii of 0.40 and 1.45 mm, respectively, was introduced with a 2.0 mm diameter indenter loaded to 2.8 kN. In the final simulation in the analysis, several thermal and pressure transients were applied to the coating surface and the evolution of the indentation-induced delamination was analyzed.

A thermal-structural model, described above, was developed next to analyze the effects of thermal transients on the pre-existing interfacial flaws just discussed. Nickel-coated steel specimens were used for this study of the thermal and pressure transients. Explicit, linear, axisymmetric elements were used for the coating and substrate. The chosen cohesive element was a four-node, two-dimensional, axisymmetric element. Adiabatic boundary conditions were assumed on all sides except at the top of the coating, where convection was applied as shown in Figure 5.2. In addition, as the crack grew and the coating separated from the interface, the conductance across the gap decreased as a function of separation distance.
During the simulations, the applied thermal loading consisted of a convective thermal pulse via propellant gases, the heat-up period of which lasted for only 0.8 ms. The short time span of the event necessitated the use of an explicit finite element formulation to capture the dynamic effects. Figure 5.2 shows a close-up view of the top corner of the deformed mesh used as the initial conditions for the explicit thermal-structural model. As already described, the indentation model resulted in a ring-type crack of the coating with an inside and outside radius at the center of the disk. At the center, bonding is maintained because of the high compressive stresses directly beneath the indenter.

All the material properties were input as functions of temperature, as determined by the empirical data between the room temperature and the highest temperature for which data was available. It was assumed that no further softening occurred. Generally, nickel is not used in gun tube coatings as it experiences more significant thermal softening relative to conventional gun tube coating material. In all the simulations, a thermal boundary condition was applied to the coating surface, which consisted of a convective thermal pulse via propellant gases at 3100 K, with a varying corresponding heat transfer coefficient of approximately 160,000W/m² °K during heating and of approximately 22,500 W/m² °K on the back end of the pulse. The convective pulse was applied over the entire sample surface. In addition, some simulations included a pressure transient, which peaked at 391 MPa and followed the thermal peak by 0.4 ms. The pressure pulse was applied for 3.2 ms, whereas the entire thermal pulse lasted for approximately 10 ms, after peaking at 0.8 ms. The boundary conditions are shown in Figure 5.3.

Nickel-coated steel specimens were used to study the thermal and pressure transients. All the nickel and steel material properties, including those just described, in addition to the measured
cohesive zone properties, were also used in the thermal modeling. Adiabatic boundary conditions were assumed on all sides except at the top of the coating, where convection was applied to the entire coating surface. In addition, as the crack grows and the coating separates from the interface, the conductance across the gap was expected to decrease. Effects of gap conductance were included using the approximation of Yovanovich [164], and the properties of air for the gap between the interface were assumed. The interfacial gap conductance was already shown in Figure 5.1. Interstitial pressure was assumed in the simulations with the pressure transient. If and when crack growth occurred, the interstitial pressure was assumed to follow the crack tip. The interstitial pressure area was solved iteratively, using the crack tip from the previous simulation as the new boundary for the pressure area until satisfactory agreement was achieved. Such an analysis provides a useful context for the study of the effects of thermal and pressure transients on the evolution of an interfacial, indentation-induced flaw.
Figure 5.3: (a) Convective coefficient, (b) convective gas temperature, and pressure transient applied to the coating surface.
5.2.1 Gun Tube Boundary Conditions – The Effects of Thermal and Pressure Transients on Flaw Evolution

Gun tube boundary conditions applied in the finite element model contain both the thermal and pressure boundary conditions described in the preceding section. A pulsed-laser heating test, as described in Section 2.5.1, is often used to evaluate the thermal response of coatings, though the pressure transient component is notoriously difficult to include in gun tube coating tests, with the exception of live-firing tests. The pulsed-laser duration and energy were selected to best approximate the thermals associated with combustion events. Finite element simulations with only thermal boundary conditions are, therefore, analogous to the pulsed-laser heating test based on the experimental conditions. Therefore, simulations of the thermal transient, with and without the additional pressure transient, provide a unique opportunity to judge the contribution of pressure, which is unaccounted for in the pulsed-laser heating experiment. In addition, pressure transients are slightly out of phase from the peak combustion temperatures, further complicating its understanding in experiments. Finite element modeling, therefore, is ideally suited to studying the effects of the pressure transient relative to thermal loading. In order to do so, two simulations have been run for which the boundary conditions include either thermal or thermal and pressure transients. Analyzing the contributions of the pressure transient will provide a better context for analyzing the pulsed-laser heating test in terms of its ability to model gun tube firing events. The two cases described above were studied: a thermal transient alone, and in combination with pressure pulse. Additionally, interstitial pressure will be applied inside the indentation-induced blister in order to simulate a scenario in which gases have penetrated the coating. The cases analyzed were chosen in order to study the contributions of each component of the gun tube coating live firings and to compare the results to those of existing experimental techniques.
As expected, the transients had significant deleterious effects on the interface. In the simulations discussed in this section, two of the transients shown in Figure 5.3 were sequentially applied to the surface, separated by 3.5 ms, in order to study how damage and crack length are affected by the first and second transient. As shown in Figure 5.4, the presence of the pressure transient significantly reduces crack growth.

![Crack Radii as functions of Time](image)

**Figure 5.4:** Crack radius as a function of radial position for simulations with thermal transient, and thermal and pressure transients.

In fact, the only outwardly advancing crack growth observed in the simulation that included the surface pressure occurred during the first phase of the second cycle while the gas temperature and surface temperature were both increasing. The crack growth described was driven entirely by shear deformation at the interface, as the surface pressure kept the interface in a state of compression. In the absence of the pressure transient, crack growth was observed to have occurred throughout the two cycles. Early crack growth was twice observed after the temperature
had decreased to approximately half of its peak value: at between two and three microseconds and again at around seven microseconds. The next spurts of crack growth occurred during the second cycle, at approximately the same times as during the first transient, though the growth was more gradual in the second cycle. Because of the absence of the surface pressure, which restricts the normal separation of the interface, once the center of the defect had separated, the coating expanded away from the substrate, introducing significant normal loading. Because of this effect, crack growth occurs by a completely different loading mode, as the phase angle is between 1° and 4°, signifying almost no shear contribution.

The damage variable defined in Equation (4.6) also provides valuable information regarding the state of the interface and its susceptibility to further crack growth. Interfacial damage at several different stages of the transient is shown in Figure 5.5. Damage observed due to only thermal loading is shown in Figure 5.5a. It can be seen that very little damage occurred at the outer radius until approximately 10 ms, after the coating at the center of the flaw had separated from the substrate. After the short cooling period, damage increased noticeably at the outer flaw radius, though no further contributions occurred during the second cycle. Though significant crack growth occurred during the second cycle of the transient as the crack radius increased from 1.65 to 1.81 mm, it appears that the region outside the original damage is relatively unaffected by the additional thermal cycle. However, such an effect should not be expected to continue during subsequent transients, due to changing interface conditions as the crack radius increases. Damage was accumulated similarly after the addition of the pressure transient. No additional damage accumulated until the brief period between cycles when the sample was allowed to cool down. Unlike the simulation that included only the thermal transient, additional damage was observed during the second cycle of the transient.
As described above, the extent and mechanisms of crack growth are strongly affected by the presence of the surface pressure. The macroscopic view of the interface further confirmed the
beneficial effects of a surface pressure transient. The conditions at the crack tip are shown in Figure 5.6.

Figure 5.6: Interfacial damage accumulated during stages of the transient in simulations showing a magnification of the crack tip with (a) only a thermal transient and (b) thermal and pressure transients.
Advancement of the crack front in the simulation with only the thermal transient can be seen in Figure 5.6a, for which crack growth during the first cycle occurred between the peak of the pressure transient at 1.2 ms and the end of the thermal transient at 10 ms. Further crack growth occurred during the second cycle of the transient, with approximately the same amount of crack extension. A comparison between Figure 5.5a and Figure 5.6a highlights the importance of damage conditions at the crack tip. Damage accumulation at the crack tip, shown in Figure 5.6a precludes the crack growth shown in Figure 5.5a, proving to be an accurate predictor of imminent crack growth. The addition of the surface pressure transient significantly suppressed crack growth at the crack tip, despite its interstitial component. This is because the only appreciable damage accumulation occurred during the second transient, which was also when the crack extended. As expected, the damage extends further radially in the simulation without the pressure transient.

The final states of the interface, after two cycles of the thermal transient and after two cycles of the thermal and pressure transients, are shown in Figure 5.7.

![Figure 5.7: Comparison of final damage states between simulations with and without pressure transients, with the crack tip conditions inlayed on the plot.](image-url)

Again, the presence of the surface pressure noticeably improved interfacial response to the thermal transient. Despite crack opening forces resulting from interstitial pressure, the surface pressure was more than sufficient to keep the interface in a state of compression throughout the pressure transient. Such results suggest that mechanisms leading to catastrophic gun tube coating failure include effects not accounted for in the above simulations. Pressure, though it penetrated the coating, does not provide necessary crack opening forces to extend the crack. However, interstitial pressure does contain highly corrosive combustion gases, which once exposed to the steel, pose serious threats to the steel in terms of high temperatures and erosion.

The mechanisms of crack growth were significantly different after the addition of the coating and blister pressure transient. Crack growth during the simulation with both transients, though small, was dominated by the shear mode. However, in simulations without the pressure transient, the crack growth observed was driven almost entirely by Mode I loading conditions at the crack tip, as there was no longer surface pressure to keep the separated coating in close contact with the substrate. Such results suggest that tests with only thermal effects, such as the pulsed-laser heating test, possibly evaluate crack growth at different phase angles than would actually be present in gun tubes, and this could be problematic as already described in Section 2.12.8 since crack growth is a strong function of phase angle. The critical fracture energy for modes II and III is often significantly higher than for mode I, as reflected in the cohesive zone properties evaluated in Section 4.4.3. As such, loading conditions with higher proportions of shear loading are more resistant to crack growth. Results described in this section suggest that crack growth under thermal conditions with pressure, which more closely approximates gun tubes extend an already present flaw by different mechanisms under thermal loading alone.
Conductance across the interface during thermal loading also affects flaw evolution for the samples subjected to thermal and pressure transients. It is likely, though, that the relationship used for gap conductance overstates the effects of the clearance, as influence from radiation and convection would still contribute to heat transfer after the interfaces separate. Yet, the relationship does include for an important factor of crack growth, which is unaccounted for in most previous models of crack growth in gun tube coatings. Once the gap clearance increases, the coating loses most of its capacity to sink heat to the substrate. The coating then reaches even higher temperatures and eventually separates, as seen in laser-pulse heating experiments [2, 17]. As shown in the plot of the temperature at the end of the pulse (Figure 5.8) the separated coating is significantly hotter than the surrounding specimen, and lifts off the substrate; this is very similar to what was seen in the previously cited pulsed-laser heating experiments.

In addition to beneficial compressive stresses introduced because of surface pressure loading, the transient also acts to maintain contact between the coating and substrate. Improved interfacial conductance allows more heat to be transferred to the substrate, resulting in lower coating temperatures, and ultimately limiting thermal expansion. Such a tertiary effect of the pressure loading is not included in the pulsed-laser experiments and significantly improves conduction away from the coating, thus minimizing interfacial damage and crack growth.

The simulations of the damage evolution for coated plates with indentation-induced delamination flaws under thermal-pressure transients provided insight into the contribution of each component to the interface conditions. Such a simulation is similar to the pulse-laser heating tests, for which significant damage and even complete coating and adhesive failure have been shown to occur in a low number of cycles. However, the addition of the pressure transient had significant advantageous effects on the integrity of the interface. Very little crack growth was seen, despite
the presence of the thermal transient, which by itself was shown to result in significant crack growth. Additionally, even one of the worst-case scenarios in which pressure had penetrated the crack to apply loading directly on the crack surfaces, did not do away with the benefits of the surface pressure for compressive interfacial stresses.

Figure 5.8: Coating temperature after the completion of the thermal transient in simulations (a) with pressure transient and (b) without pressure transient.
5.2.2 Effects of Coating Thermal Capacity on Flaw Evolution

As observed in Section 5.2.1, thermal expansion resulting from convective heat transfer drives crack growth in the absence of a pressure transient and contributes to interfacial damage in all simulations. Significant crack growth was observed in the simulations in which the properties of nickel were applied to the coating. The importance of heat transfer for thermal expansion and resulting crack growth is a given. Therefore, the effects of a coating material’s thermal capacitance were investigated as a possible mechanism for improving coating performance. For nickel, the amount of energy required to raise the temperature of a unit of mass of the material one unit of temperature (the specific heat) is approximately 440 kJ/kg K. However, other metals have specific heats that range from between 100 to 900 kJ/kg K. An increase in the specific heat would be expected to result in less heat transfer, less thermal expansion, and, therefore, less crack growth. However, the decreased specific heat could also result in high thermal gradients, and increased stresses at the interface for different coating thickness.

The specific energy of the coating material was increased by 50 and 100% of the value of nickel in anticipation of reduced crack growth. Plots of the crack growth at different levels of specific heat are shown in Figure 5.9.
Figure 5.9: Crack radius as a function of radial position for simulations with thermal transient and with thermal and pressure transients.

Similar to the simulations performed in Section 5.2.1, two cycles of the transient were modeled to determine the effects of crack growth due to damage accumulated in the first transient. As shown in the figure and as expected, crack growth is significantly delayed and ultimately eliminated as the specific heat increases. Crack growth at the inner radius occurs during the cool-down period in the first transient when the specific heat of nickel is used, but is delayed to the ramp-up portion of the second transient, and finally well into the ramp down of the second transient as the specific heat is increased further. Similar results were observed in terms of crack growth at the outer radius. The simulation of the nickel coating with a specific heat of 440 kJ/kg K experienced crack growth through both cycles of the transient, though crack growth did not occur until the second cycle of the transient in the simulation with higher specific heat. Crack growth during the second simulation occurred during the decreasing gas temperature portion of the transient, thus coinciding with crack growth during the first simulation. The overall crack growth was observed to be less in the simulation with higher specific heat, but more growth did occur during the second transient. These results suggest that significant damage occurred during
the first transient, though not enough energy was present to drive the crack front. The respective contributions of normal and shear loading at the crack front were similar to what was seen in the simulation with only thermal boundary conditions described in baseline simulations as the phase angle during crack growth was slightly higher at $\phi=6^\circ$ as compared to $\phi=3^\circ$. In both cases, the shear contribution is almost negligible. As discussed earlier, the shear fracture energy is significantly higher than normal and provides more resistance to crack growth at higher phase angles. Finally, no outward crack growth was observed for the highest specific heat simulation. The results show that increasing specific heat has the effect of both limiting and delaying crack growth in pre-indented coated samples subjected to thermal transients on the coating surface.

A comparison of the accumulation of damage is shown in Figure 5.10, which correlates with observations from the comparison of crack growth.

As discussed previously, damage is accumulated throughout the two cycles of the simulation. Almost all the damage was accumulated in the first cycle, though crack growth occurred throughout both cycles. However, as the specific heat increases, the amount of damaged accumulated decreases, until almost no observable increment in damage is accumulated in the simulation with the specific heat of 800 kJ/kg K. More information about the conditions of the interface at the stages in the transient can be gained by observing the conditions at the crack tip, which cannot be seen in the entire interface in Figure 5.10. Plots of the damage variable closer to the crack tip are shown in Figure 5.11, which compares the damage for a coating with standard nickel properties and a simulation where the specific heat is increased to 800 kJ/kg K as shown in Figure 5.11b.
Figure 5.10: Interfacial damage accumulated during stages of the transient in simulations with specific heat of (a) 440 kJ/kg K, (b) 600 kJ/kg K, and (c) 800 kJ/kg K.
Figure 5.11: Close-up of the crack tip showing interfacial damage accumulated during stages of the thermal transient in simulations with specific heat of (a) 440, (a) 600, and (c) 800 kJ/kg K.
As shown in Figure 5.11, the higher specific heat significantly decreased the damage at the crack tip, when compared with the baseline case. No crack growth occurred in the simulations with higher specific heats, yet a slight increase in damage occurred during the decreasing temperature portion of the thermal transient. An increase in damage again precludes crack growth, shown in Figure 5.11b. During the first cycle of the transient, the damage at the crack tip increased significantly, though no crack growth at the outer radius occurred during this time. The accumulated damage during the first step directly resulted in significant and sudden crack growth during the decreasing portion of the transient in the second cycle of the transient. Such results implicate the damage in the crack tip region as a precursor to crack growth.

The final states of damage for the three simulations are shown in Figure 5.12.

![Damage as a function of Radial Position](image)

**Figure 5.12:** Comparison of state of final damage between simulations with and without pressure transients, with the crack tip conditions inlaid.

As discussed earlier, the simulations with higher specific heats accumulate significantly less damage than was observed with the “standard” thermal properties of nickel. Additionally, a
second crack was formed outside the initial region of damage. Interestingly, the second crack does not form in the original simulation, though damaged elements were observed. The near crack tip conditions show crack growth that occurred for the middle specific heat value. However, excepted for increased damage at the crack tip, and increased crack length, little difference in the state of the interface can be seen over the remainder of the interface as the specific heat was increased.

5.2.3 Effects of Phase Difference between Pressure and Thermal Transients on Flaw Evolution

The surface and blister pressure transients were adjusted in time relative to the thermal shock in order to study conditions that are possible in other barrel configurations. Effects such as riffling can influence how the gases travel through the length of the barrel, resulting in higher and lower convection coefficients, which in turn increase or decrease the rate of heat transfer into the coating, respectively. Surface conditions can also affect the speed at which the thermal and pressure transients move along the length of the tube. In addition, coating thermo-physical material properties can also increase the rate at which thermal energy is transferred into the coating and substrate, thus advancing or delaying the effects of thermal transient relative to the pressure transient. As seen in the previous section, the thermal transient has significant effects on the extent and type of crack growth. Therefore, the effects of the phase difference between the thermal and pressure transients was studied to discern how the rate of heat transfer, and other coating conditions, might affect damage and crack growth at the interface.

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In order to study such effects, the phase difference of the pressure relative to the thermal transient shown in Figure 5.3 were offset by three different increments of time in addition to the phase difference already present. The pressure transient was delayed for 0.5 ms and 1.0 ms, in addition to being advanced 0.25 ms form its original time. All three simulations of phase differences were compared to the baseline presented in Section 5.2.1 for which the thermal transient already preceded the pressure transient by 0.4 ms. Only one cycle of the transient is simulated in the following analysis. A plot of the crack radius as a function of the time for the simulations of the three amounts of phase difference is shown in Figure 5.13, in addition to the case with the measured boundary conditions.

![Crack Radii as functions of Time](image)

**Figure 5.13:** Crack radius as a function of radial position in simulations for which internal and pressure transients were offset from their original position relative to the thermal transient.

As shown in Figure 5.13, crack growth at the outer radius occurred only when the pressure was delayed for 1.0 ms from its original time relative to the thermal transient. Crack initiation
coincides with the onset of the pressure transient, and it is reinitiated at two other times during the transient: as the pressure transient approaches zero and during the ramp-down of the thermal transient. The phase angle of the crack tip during growth was between 70° and 85°, signifying significant shear contributions to crack growth, as the pressure transient kept normal loading to a minimum. However, it is likely that a 1.0 ms offset is unrealistic given the variations that might be expected in gun tube conditions. By offsetting the pressure transient, crack growth at the inner radius was delayed. Inward crack growth was most delayed in the final simulations when the pressure peak was offset at 1.0 ms later than in the baseline case, even though outward crack growth occurred throughout the transient.

The different cases of pressure offsets resulted in crack growth only in the extreme example of the pressure transient delayed by 1.0 ms. However, interfacial damage accumulated in all cases, as shown in Figure 5.15 and Figure 5.15.

As observed in the previous simulations, significant damage occurred following both the thermal and pressure transients. The area of damage increases during the cool-down phase, though accumulated damage at the edge of the region does not approach unity. Crack growth did not occur during the cool-down period after the thermal transient, yet it is apparent that the interface does experience damage as the heat is conducted through the sample. In addition, the surface was exposed to room temperatures, though at a lower convective coefficient, thus creating a more severe thermal gradient.
Figure 5.14: Interfacial damage accumulated during stages of the transient in simulations for which relative to the thermal transient the internal and external pressure transients were offset by (a) 0.25 ms before its original time, and (b) 0 ms.
Figure 5.15: Interfacial damage accumulated during stages of the transient in simulations for which relative to the thermal transient the internal and external pressure transients were offset by (a) 0.3 ms, and (b) 1.0 ms after its original time.

The state of damage at the crack tip is shown in Figure 5.16, which displays a close-up of the crack tip damage conditions.
Figure 5.16: Interfacial damage accumulated during stages of the transient in simulations for which the internal and external pressure transients relative to the thermal transient were offset by (a) 0.25 ms before its original time, (b) 0 ms, and (c) 1.0 ms after its original time.
Offsetting the pressure appears to have a greater effect on the overall interface than at the crack tip. Advancing the pressure transient ahead of the thermal resulted in a slight increase in damage, all of which was accumulated during the ramp-up portion of the pressure transient. The most significant crack tip damage was accumulated in the final simulation, for which the pressure transient was delayed 1.0 ms. As mentioned earlier, the phase angle during crack growth signified large amounts of shear contributions to which the crack tip is more resistant than normal loading. However, upon further analysis, it can be seen that significant damage occurred in the 1.0 ms before the application of the pressure transient, which was incurred in Mode I loading. The damage accumulated during the heating portion of the transient, which occurred before the pressure transient was applied is shown in Figure 5.16c. Again, the damage was incurred before the pressure transient. Thus, thermal expansion created the necessary shear displacement at the interface necessary to initiate crack growth at the weakened elements.

Some damage occurred between the start of the thermal transient and the ramp-down of the pressure. However, the remainder of damage occurred during the back end of the pressure transients; both periods coincide with crack growth.

The final extent and nature of the damage to the interface is shown in Figure 5.17. Interestingly, the final state of the damage was relatively unaffected by the pressure offset. Advancing the pressure ahead of the thermal transient increased the damage at the edge of the region, though delaying the pressure by 1.0 ms created a slightly larger area of damage. Offsetting the pressure by an additional 1.0 ms behind the thermal transient resulted in crack growth, and moving the pressure ahead of the thermal produced similar damage. The results suggest that at the crack tip, delaying the pressure transient by an additional 0.3 ms, or advancing it 0.25 ms relative to the
thermal transient, actually resulted in less damage at the crack tip. However, delaying the transient by 0.3 ms performed the best over the rest of the interface in terms of limiting damage. A possible means of improving coating performance therefore could include tuning coating thermal properties in order to offset the thermal and pressure effects further.

Figure 5.17: Comparison of state of final damage between which the pressure transient was shifted relative to the thermal transient, with the crack tip conditions inlayed on the plot.

5.2.4 Effects of Phase Difference between Internal Flaw Pressure and Surface Boundary Conditions on Flaw Evolution

The results in Section 5.2.1 suggest that internal blister pressure is not sufficient to propagate a crack by itself. However, researchers [14, 25, 165] maintain that such pressure is the major cause of final coating failure. Such conflicting observations suggest another contributing factor; because it is assumed that blister pressure penetrated the coating by way of a through-coating
crack, the ensuing effects of the thermal transient are likely to cause expansion and fully or partially close the crack through which pressure entered. The effects of through-coating crack closure are, therefore, likely to provide resistance to the release of the blister pressure after crack closure. Such an effect was investigated using the same thermal-structural model analyzed in the previous sections, but with the internal blister pressure out of phase with respect to surface pressure. These conditions were designed to simulate crack growth in a gun tube, with surface convection and pressure measured from actual firings. It should be noted, however, that the internal blister pressure was offset behind the surface pressure transient, providing crack opening forces after the surface conditions had subsided.

Crack growth was found to be a function of the amount of time that blister conditions were offset from the surface pressure transient. The simulations in which such a pressure offset was less than 0.15 ms showed no crack growth. In the simulations in which the blister pressure was offset to 0.25 ms after the surface resulted in slight crack growth after the surface pressure transient had completely subsided and before the completion of the blister loading. A plot of the crack growth for the four different simulations is shown in Figure 5.18.

![Crack growth simulation](image)

Figure 5.18: Crack radius as a function of radial position in simulations for which internal and pressure transients were offset from their original positions relative to the thermal conditions.
Crack growth at the inner radius was initiated earlier in the simulation as the phase difference between the two pressure transients increases. Outer radius crack growth only occurred when the phase difference was 0.25 ms or greater. Results indicate that the amount of crack growth was a strong function of the time that the blister pressure was offset. A small crack extension was observed after the two pressures were offset at 0.25 ms, though catastrophic crack growth resulted only after the offset was increased to 0.275 ms. Crack growth resulting from the blister pressure differential was predominantly Mode I, with a phase angle of $\phi=1.7^\circ$; this was to be expected as the interstitial pressure is separating the coating surface from the substrate, thus opening the crack. Such crack growth occurs when pressure is applying crack opening forces inside the blister and surface pressure has almost completely subsided. When the blister pressure was delayed by 0.25 ms, slight crack growth occurred as the outer radius increased from 1.45 to 1.58 ms. However, when the blister pressure was further delayed by 0.025 ms to 0.275 ms, catastrophic crack growth occurred. This was because the increase in crack size created a larger area for crack opening pressure, resulting in a positive feedback loop that led to catastrophic failure at the interface. Initiation of crack growth occurred in the final two simulations at approximately the same time from the start of the simulation: the 0.25 ms offset crack initiation occurred slightly later. Crack growth in the case of the 0.275 ms offset also occurred at a faster rate. Blister pressure was slightly higher in the simulation with the longer offset time, resulting in larger crack opening forces that were unopposed by surface pressures for longer—a combination that produced catastrophic crack growth. Based on these results, coating failure is highly sensitive to the conditions inside the blister, namely the differential between surface and blister pressure magnitudes. According to researchers [14, 25, 165], failure by blister pressure is the accepted mode of final failure. Furthermore, the results described herein confirm the experimental observations that the coating fails suddenly and catastrophically when the difference between the blister pressure phases exceeds a critical value.
In order to assess the damage that accumulates during the transients, damage at several different times was plotted as a function of radial position, as shown in Figure 5.20 and Figure 5.20.

![Damage as a function of Radial Position](image)

**Figure 5.19:** Interfacial damage accumulated during stages of the transient in simulations for which the internal and external pressure transients were offset relative to the thermal transient by (a) 0 ms, (b) 0.15 ms after its original time.

With the exception of the 0.275 ms offset shown Figure 5.20d, which failed before the end of the blister pressure transient, all plots show damage at the completion of the pressure transient, at the end of the thermal transient, and at the completion of the cycle. In all cases, most of the damage accumulated after the end of both thermal and pressure cycles while the sample was exposed to room temperature air on the surface and the heat was being conducted heat through its thickness.
The damage accumulation plot in Figure 5.20d is incomplete, as the sample failed before the end of the blister pressure transient.

Figure 5.20: Interfacial damage accumulated during stages of the transient in simulations for which the internal and external pressure transients were offset relative to the thermal transient by (a) 0.25 ms, and (b) 0.275ms after its original time.

Damage accumulation on the entire interface occurred primarily during the cool-down phase of the transient. However, observations at the crack tip show that significant damage occurred in the time between the completion of the surface and interstitial pressure transients, as shown in Figure 5.21.
Figure 5.21: Interfacial damage accumulated during stages of the transient in simulations for which the internal and external pressure were offset after its original time relative to the thermal by (a) 0.15 ms and (b) 0.25 ms.

Significant damage accumulated when the blister offset time increased from 0.15 ms to 0.25 ms. The majority of damage accumulation in both plots occurs between the time the surface pressure is removed and the end of the blister pressure transient, which is also the time in which crack growth occurs during the simulation described in Figure 5.21b. The above figure is also useful in showing the importance of the offset time for the conditions at the crack tip. As the blister offset increases to 0.25 ms, unopposed crack opening forces are present for more time, in addition to being at a higher magnitude, resulting in significantly more damage after the interstitial pressure
recedes. Following the completion of the blister pressure transient, almost no further damage is accumulated.

Further insight into the contribution of the extent of the phase difference between the blister and surface pressures is shown in Figure 5.22, which plots the final state of damage as a function of radial position.

![Figure 5.22: Comparison of state of final damage between which the pressure transient was shifted relative to the thermal transient, with the crack tip conditions inlayed on the plot.](image)

As shown in the plot, the sample with the highest blister offset accumulates the largest amount of damage in terms of both area and magnitude. Additionally, at the crack tip, substantial increases in damage can be seen as the blister offset times are increased. Such results should be expected, as the crack opening forces associated with the blister pressure would have greater effects when it acts longer and at a higher magnitude without the confining surface pressure.
5.2.5 Discussion of Thermal-Structural Simulation of Flaw Evolution Results

The simulations were conducted in order to ascertain the mechanisms and conditions for the evolution of an indentation-induced interfacial flaw under severe thermal and pressure surface transients. Complete boundary conditions, including surface pressure, were applied to the finite element model, and the responses of the system without the pressure transient were compared. By comparing the respective solutions of the simulations with and without the pressure transient, the contribution to interfacial flaw evolution can be determined, thus gaining insight into the pulsed-laser heating experiment. Additionally, blister pressure was included in the indentation-induced flaw in a situation in which it was assumed that the surface pressure had penetrated the coating through a crack, thus applying crack opening forces from inside the blister. Thermal and pressure transients were also further offset, in order to examine the effects of the phase difference between thermal and pressure transients, and by extension, factors that might expedite or impede their effects. Finally, internal blister pressure has often been cited as a likely initiator of final failure, though it has never been proven; therefore, blister pressure was delayed relative to the surface pressure transient in order to simulate a gun tube in which combustion gases (and pressure) penetrate the coating and subsequently cannot easily escape because thermal expansion has caused the crack to close.

The plots of the damage variable are instructive in highlighting the effects of thermal and/or pressure transients on the state of the interface away from the crack tip. Plots that show the entire interface are slightly misleading, as it appears that the region for which the damage variable is equal to one is much larger than it actually is. Upon closer inspection, the area of complete damage is not as large as it first appears, as shown by the images of the damage variable at the crack tip. Additionally, as can be seen in the relation between the damage variable and the cohesive normal and shear stresses (Equations (4.2) and (4.4)) the damage variable is multiplied
by the cohesive stiffnesses, which are orders of magnitude larger than the strength of the interface. Therefore, interfacial elements can still be in a state of high stress with magnitudes on the order of megaPascals, despite a damage variable exceeding 0.999. Though the entire scope of the interfacial damage provides insight into the distance from the crack that the damage reaches, the near-crack tip damage offers more insight in terms of predicting the effects of the thermal transient on future crack growth.

As discussed previously in Section 5.2.1 the contributions of surface thermal and pressure transients have significant effects on the interface, specifically on flaw evolution. Two cycles of the transient were simulated, in order to better quantify the effects of damage introduced during the first cycle of the transient. In the absence of the pressure transient, crack growth occurred during both cycles of the transient. Crack extension occurred during the ramp-down period of the thermal transient, and again during a relatively steady portion during which combustion gases were approximately 1300 K in both cycles. The coating had absorbed enough heat that it separated at the center of the indentation, which allowed it to be detached from the interface. Once the coating was free to expand from the substrate, heat conduction across the interface decreased drastically as a function of separation distance before stopping completely, resulting in higher coating temperatures and even greater thermal expansion. Clearly, the effects of the increased coating temperature contribute to crack growth. However, they can also melt the coating, as temperatures in excess of the melting point of nickel were observed at the edges of the indentation. Given the effect of gap conductance, an added benefit of the pressure transient is that it keeps the coating in contact with the interface should an interfacial flaw be present, which aids heat transfer through the coating and limits thermal expansion. Additionally, because of the thermal expansion of the coating and the absence of any confining pressure, the coating bows upward, creating normal separation at the interface. In the absence of a pre-existing interfacial flaw, the coating would remain in contact with the interface, resulting in radial expansion, and
higher shear stresses at the interface, to which the interface is more resistive. However, when a flaw is present, and there is no confining surface pressure, crack tip loading is predominately Mode I. It is likely that such conditions are similar to the pulsed-laser heating experiment, where similar amounts of energy are introduced to the sample at approximately the same rate. Based on observations from the simulation, such an experiment can be expected to initiate a flaw due to shear forces. However, the contribution of Mode I loading will increase as the flaw grows until the loading on the crack tip is entirely Mode I, as was the case in the simulations. Such loading are not analogous to gun tube conditions, as demonstrated in Section 5.2.1. Simulations performed with both thermal and pressure transients illustrated the beneficial effects of the absence of Mode I crack tip loading conditions. Crack growth did not occur in the first cycle of the transient that included pressure, but did manifest itself in the period of increasing combustion gas temperatures during the second cycle due to shear interfacial loading. When the pressure transients were applied, crack tip loading was entirely Mode II, but otherwise it was still predominately Mode II, with a small Mode I component. Surface pressure, therefore, has a significant effect on interfacial conditions and the resulting crack growth. Such effects must be taken into consideration when analyzing pulsed-laser heating experimental results.

The addition of surface and blister pressure without additional offsets, significantly impeded crack growth, which did not occur during the pressure transient due to the resulting interfacial compressive stresses. However, crack growth did occur during the second cycle of the transient as the convective gases were again heating up the coating surface. Interestingly, the interface was in compression at this time due to the surface pressure, though the shear stresses at the interface were enough to drive the crack a short length by entirely Mode II/III loading. Such results confirm the significance of shear crack growth in the presence of surface pressure transients.
The importance of the conditions of blister pressure should not be overlooked in terms of crack interfacial crack growth and final coating failure. An analysis in which the pressure was assumed to have penetrated the coating, but was in phase with the surface proved inconsequential in terms of crack growth. Such results were not unexpected, as the entire surface experiences pressure equal in magnitude and applied to the entire coating, providing more than sufficient force to maintain compressive stresses ahead of the crack tip. Offsetting the entire pressure transient did not have significant effects on the state of the interface, with the exception of the 1.0 ms offset, which is larger than any anticipated variation in actual conditions. In this case, the crack extension occurred by almost exclusively Mode I loading, which occurred after the pressure transient was complete. Based on subsequent simulations, the effects of delaying the internal blister pressure transient were evaluated in order to simulate the resistance interstitial pressures might face after the coating expanded and the through-coating crack had completely or partially closed. It was observed in the simulations that when pressures were offset by 0.25 ms, crack growth began and that further offsets lead to catastrophic failure. Such results suggest the sensitivity of the blister to gas pressures. Offsets in the phase between surface and interstitial pressure have compounding effects: pressure is applied from inside the blister for a longer period of time absent confining surface pressure, in addition to being larger in magnitude. Further compounding the effects, the interstitial pressure application area increases with the crack front. Once crack growth is initiated, the pressure area and resulting crack opening forces also increase, further driving the crack. Interstitial pressure also opens the crack in its weakest mode, as interfaces are significantly more resistant in the shear mode to fracture. According to this analysis, it is highly likely that blister pressure is the instigator of final coating failure.

The thermal capacitance of the coating materials was analyzed in terms of its effects on crack growth. Based on the expectations due to the role of specific heat in heat transfer, the property was identified as possibly having a strong effect on crack growth. Higher specific heat requires
more energy to change the temperature of the coating. Because the loading conditions, and the energy associated with them, are constant the resulting effect of higher specific heat is lower coating temperatures, less heating, and reduced thermal strains, despite increased thermal gradients. Such effects can be seen in Figure 5.23, where after the thermal transient, the temperature of the coating is significantly lower in the simulation with the lower coating specific heat.

Figure 5.23: Temperature distribution in the coating after the completion of the thermal transient, t=10 ms, for a coating with specific heat of (a) 440 kJ/kg K, and (b) 800 kJ/kg K.
Because of decreased heat transfer and lower coating temperatures, the coating experienced significantly less thermal expansion. Recognizing the effects of specific heat, it was confirmed as a viable consideration in coating design as a means of improving performance and minimizing interfacial crack growth.

Several different combinations of gun tube boundary conditions were applied to the surface of a coated sample with an indentation-induced interfacial flaw. Flaw evolution under the thermal transients was studied with and without the surface pressure transient, which was accompanied by an interstitial pressure inside the flaw that was in phase with the surface pressure transient. A comparison of the results with and without the pressure transient illustrated the beneficial effects in terms of suppressing crack growth. Additionally, the results revealed the different mechanisms of crack growth resulting from the addition of the surface pressure transient, which has the beneficial effect of keeping the interface in compression, and reducing crack growth. Thermal capacitance was also studied and found to have beneficial effects in terms of reducing heat transfer and thermal expansion, leading to suppressed crack growth. The surface and interstitial pressure transients were further offset relative to the thermal transient. In addition, though interfacial damage increased, crack growth was found to be largely unaffected, except in the extreme case of the 1.0 ms offset. Finally, the blister pressure was offset from the surface pressure transient, which resulted in the occurrence of blister pressure after the surface pressure had subsided. The pressure differential at the end of the blister transient was sufficiently significant to extend the crack. Observations from simulations at different blister offset values suggest that the crack growth is highly sensitive to blister pressure conditions. In cases in which the blister pressure was offset by 0.25 ms, a small amount of outward crack growth was observed. However, increasing the blister offset time to 0.275 ms resulted in catastrophic crack growth such that coating completely separated from the substrate surface. These results provide valuable insight into the crack growth process of interfacial, delamination-induced flaws, useful
information for designing future experiments, and a relevant context for interpreting test results based on current gun tube coating evaluation methods.

5.2.6 Discussion in Context of Existing Gun Tube Coatings

Because it was used in the study of cohesive zone property measurement, nickel, which is not a gun tube coating material, was used in the study in place of tantalum. Like current gun tube coating application techniques, the nickel coating was applied by thermal spray. While it is not used or even considered in gun tubes, it can provide insight into current treatments. Tantalum, which is the presumptive next generation coating material after chromium, has some similar material properties to nickel, and was applied using similar deposition techniques to tantalum in gun tubes. A summary of the best available material properties for both the investigated and actual gun tube material is shown in Table 5.1.

The similarities in both elastic moduli can be seen in the Table. Nickel has significantly higher room temperature conductivity, but quickly approaches that of tantalum as the temperature increases. The coefficient of thermal expansion is similar, but slightly higher for nickel; such a difference could result in a small contribution to crack growth as the coating expansion is likely to be higher.
Table 5.1. Comparison of nickel and tantalum temperature dependent material properties.

<table>
<thead>
<tr>
<th>Temperature (K)</th>
<th>Nickel Yield Strength (MPa)</th>
<th>Nickel Ultimate Strength (MPa)</th>
<th>Tantalum Temperature (K)</th>
<th>Tantalum Yield Strength (MPa)</th>
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<th>Elastic Modulus (GPa)</th>
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<table>
<thead>
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<th>Thermal Conductivity (W/mK)</th>
<th>Temperature (K)</th>
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<table>
<thead>
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<th>Coefficient of Thermal Expansion (1/K)</th>
<th>Temperature (K)</th>
<th>Coefficient of Thermal Expansion (1/K)</th>
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<td>1200</td>
<td>1.86E-05</td>
<td>1200</td>
<td>2.37E-05</td>
</tr>
</tbody>
</table>

The most significant affects will likely be due to the difference in specific heat. Specific heat of nickel is almost four times that of tantalum. As discussed in Section 5.2.2, increasing the thermal capacitance by 50 percent had the effect of significantly retarding crack growth, while doubling it completely eliminated crack growth. Given the specific heat value for tantalum, a negative effect on the suppression of crack growth would be expected.
A complete analysis of a prospective gun tube coating would require the determination of cohesive zone properties using the technique described earlier. Once the properties have been measured, they can be applied to simulate the evolution of a flaw, resulting from indentation or other means, under combinations of thermal and pressure transients. The usefulness and applicability of the technique have been demonstrated with nickel thermally sprayed onto steel. A nickel-coated steel material set was applied using similar techniques for a test case of the method. Affects of boundary conditions were investigated, in addition to mechanisms or coating failure and current experimental techniques, such as the pulsed-laser experiment. The relevancy of the method in terms of current investigations into new coating materials was commented on. While findings can be extrapolated to understand the mechanisms that occur during flaw evolution, caution must be applied when attempting to extrapolate findings for specific results.
CHAPTER 6. CONCLUSIONS AND RECOMMENDATIONS

The main objective of this thesis was to develop a method to evaluate the interfacial properties of gun tube coatings and to use those properties to investigate the evolution of an indentation-induced flaw under thermal and pressure transients. The idea of using the spherical indentation test in conjunction with finite element models was hypothesized to be a reasonable solution. As such, numerical models were developed to simulate the mechanical process whereby interfacial flaws are introduced. The resulting delamination was then modeled numerically in order to evaluate the cohesive zone properties. Finally, the numerical results from the indentation modeling were applied as initial conditions to a second simulation of the evolution of the flaw in response to severe thermal and pressure transients applied to the surface. These models were then used to evaluate the effects of several variables on interfacial damage and crack extension. The numerical and experimental studies performed led to the following conclusions.

1. Preliminary studies suggest that through-coating cracks and interfacial delaminations are detrimental to the survivability of gun tubes. A preliminary model with collapsed stress concentration elements at the crack tip provided a basis for an analysis of the resulting stress state under severe thermal and pressure transients in a cylindrical geometry.

2. Though it provides an analytical solution for the critical fracture energy at interfaces in the coated specimens, the four-point bend test was found to be an unreliable method for interfacial critical fracture energy evaluation due to the burdensome requirements necessary to achieve steady-state crack growth. Of these, the most notable are steady-state crack growth before plastic deformation, and load-independent crack growth, once initiated. Aluminum cold sprayed on aluminum produced catastrophic crack growth,
whereas nickel-coated steel specimens produced significant deformation in the substrate before crack growth occurred.

3. The aluminum-coated aluminum samples and the nickel-coated steel specimens were indented with a spherical indenter to produce an interfacial delamination, the presence and size of which were quantified using ultrasonic measurement techniques. The results of the experiments were applied to numerical indentation simulations. Cohesive zone properties, critical fracture energy, and cohesive strength were chosen based on knowledge of the interface, the fracture characteristics, and the combination that would best reproduce the initial experimental results.

4. The described method was verified by evaluating the properties during four-point bend testing and successfully using the properties to predict the radius of indentation-induced delaminations at several different load levels, thereby providing validation for the procedure. Reasonable agreement was observed between numerical predictions and experimental results in terms of the prediction of the presence of a delamination, and the size of the resulting flaw. The four-point bend tests application to flaw evaluation was also explored, and found to be limited to a narrow set of materials.

5. The numerically/experimentally determined properties described in the previous statement were applied to a sequential, implicit-to-explicit simulation in order to study the evolution of indentation-induced interfacial flaws. Simulations included analysis of the effects of the surface pressure transient compared to only thermal convective heating in order to understand their relative contributions and thus, provide insight into the effects unaccounted for in the pulsed-laser heating test. Both the surface and blister pressure transients were offset in order to make general observations about the phase difference between the two transients, and thus to understand the out-of-phase thermal effects. Finally, blister pressure was offset, as gas penetration is often cited as a coating
failure mode and the previous analysis showed little contribution to crack growth when surface and interstitial pressure were in phase. Additionally, once the gas penetrates the coating delayed blister pressure should be expected, due to impeded gas exit after the thermal transient.

It is recommended that the following changes or improvements be incorporated into existing experimental setups and numerical models for a deeper understanding of the flaw evolution in gun tubes and for more accurate predictions of the coating responses.

1. Though spherical indentations produced interfacial delaminations in the coatings studied, both stronger or thinner coatings than those studied are likely to require higher indentation loads if penetration and damage are possible. Other ways of introducing flaws may be necessary to evaluate cohesive zone properties and to study the evolution of pre-existing flaws experimentally. A possible area of future research includes cohesive zone property evaluation using conical indenters. Additionally, techniques to introduce flaws for experimentally studying flaw evolution could include masking the interface before spraying.

2. Thermal-structural simulations were conducted with an axisymmetric model, showing flaw evolution in two-dimensional space on a plate. Such conditions model predominantly one-dimensional heat flow, though the presence of a flaw can affect heat transfer. However, gun tube applications have two complicating factors not included in the present research study because they could not be included in an axisymmetric model and would almost certainly have required significantly more computational power. In gun tubes, the heat moves along the gun tube as a thermal front. Such an effect is likely to create further thermal stress caused by the non-planar thermal gradient and resulting
thermal stresses. It is likely that the impingement of this transient on a defect would have different effects than it would on a planar thermal gradient. Additionally, the asymmetric circumferential geometry of the gun tube is significantly different than that of the plate. Effects such as the circumferential expansion of the tube moving along the axis are, therefore, not accounted for in the present study.

3. It is likely that the cohesive zone properties are affected by increasing temperatures, resulting in unpredictable effects on both the strength and critical fracture energy of the interface. Higher temperatures can be expected to decrease the strength of the interface, but the effects on fracture energy are more difficult to predict. Recognizing that these effects could be significant in gun tube applications, especially in circumstances involving repeated firings and the ensuing build-up of heat in the gun tube, they should be investigated. Indentation tests could be done in a high-temperature furnace to evaluate elevated temperature interfacial properties, though the heating would be more gradual and uniform than present in gun tubes.

4. Finite element simulations reveal strain rates on the order of 1.5/s, which is significant for most metals. High strain rates result in higher elastic modulus and yield strength, but also decreased ductility. However, high strain rates occur during high surface temperature transients, which increase the temperature of the material under high rates of strain. High temperatures have the opposite effects, resulting in lower modulus and strength. Such competing effects make the contribution of strain rate hardening difficult to predict, and thus require significant material characterization work involving both high strain rates and high temperatures.

5. The experiments in this research were conducted at room temperature, and they were extrapolated so that we could analyze the evolution of a flaw at elevated temperatures. Experimentally re-creating such temperatures over a sample surface would be a difficult
undertaking. Introducing high temperature in microseconds is certainly impossible, as lasers can introduce such amounts of energy on only a small concentrated area. However, experimental analysis of high-temperature transients on indentation-induced defects is something that should be explored, even if only over a small area.

6. The described approach, while providing a comprehensive and thorough analysis of the interface, does neglect possibly significant effects such as strain rate and temperature dependent material properties. Hardening due to high strain rates will likely be partially offset by high temperature softening. Additionally, strain rate and temperature effects were ignored in the cohesive zone, as the properties were measured at room temperature quasistatically.

7. Given the challenges and uncertainties associated with the assumption of negligible strain rate effects, a different method of cohesive zone property analysis should be explored. A laser produced stress wave experiment would therefore be recommended to accommodate any high strain rate effects on the cohesive zone properties. In the experiment a laser is used to rapidly heat the back of the substrate, resulting in a stress wave that induces delaminations. The laser induced thermal waves can also be used to study mode mixity by varying the angle of incidence. Computational requirements associated with the method, though necessitating an accurate description of the energy introduced by the laser, would be no more burdensome than those described herein. In addition, as mentioned above, an accurate rate-dependent description of the materials would improve results in both the current and proposed work.

The present research offers experimental and numerical techniques to evaluate interfacial properties, before applying the properties to analyze the evolution of an interfacial flaw. This concept as such could be used in a number of different situations such as:
1. Quantification of coating adhesion properties has applications in all fields where coating separation is of concern. Applications could include coatings of a variety of materials, such as ceramics, polymers, and metals. Quantifying the properties of the interfaces would be helpful for evaluating coating adhesion. In addition, it would enable the properties to be applied for further modeling in order to understand and mitigate the mechanisms that lead to coating failure.

2. Flaw evolution under several thermal and pressure transients is a concern for a variety of coating applications. Such concerns are especially relevant on turbine blades, which experience both severe thermal and pressure loading. Coating flaw initiation exposes the substrate high temperature gases, leading to many of the same thermal effects that, in turn, lead to steel gun tube failure, such as phase transformation and melting.
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APPENDIX A. NICKEL COATED STEEL TEST RESULTS

A. PRELIMINARY TEST RESULTS ON NICKEL-COATED STEEL

![Load as a Function of Displacement Graph](image1)
Figure A.1: Load as a function of displacement for preliminary displacement indentation tests.

![AE Events as a Function of Load Graph](image2)
Figure A.2: Acoustic emission events as a function of displacement for 0.2 mm displacement indentation test.
Figure A.3: C-scan showing dimension of delamination for 0.2 mm displacement indentation test.

Figure A.4: Acoustic emission events as a function of displacement for 0.3 mm displacement indentation test.
Figure A.5: C-scan showing dimension of delamination for 0.3 mm displacement indentation test.
Figure A.6: Acoustic emission events as a function of displacement for 0.4 mm displacement indentation test.

Figure A.7: C-scan showing dimension of delamination for 0.4 mm displacement indentation test.
Figure A.8: Acoustic emission events as a function of displacement for 0.5 mm displacement indentation test.

Figure A.9: C-scan showing dimension of delamination for 0.5 mm displacement indentation test.
B. INDENTATION RESULTS LOADED 0.5 mm OF NICKEL-COATED STEEL

![Graph showing load as a function of displacement for 0.5 mm displacement indentation test for samples 3A, 3B, and 3C.](image1)

Figure B.1: Load as a function of displacement for 0.5 mm displacement indentation test for samples 3A, 3B, and 3C.

![Graph showing acoustic emission events as a function of load for 0.5 mm displacement indentation test for sample 3A.](image2)

Figure B.2: Acoustic emission events as a function of load for 0.5 mm displacement indentation test for sample 3A.
Figure B.3: C-scan showing dimension of delamination for 0.5 mm displacement indentation test for sample 3A.

Figure B.4: Acoustic emission events as a function of load for 0.5 mm displacement indentation test for sample 3B.
Figure B.5: C-scan showing dimension of delamination for 0.5 mm displacement indentation test for sample 3B.

Figure B.6: C-scan showing dimension of delamination for 0.5 mm displacement indentation test for sample 3D.
Figure B.7: Load as a function of displacement for 0.5 mm displacement indentation test for sample 4A.

Figure B.8: C-scan showing dimension of delamination for 0.5 mm displacement indentation test for sample 4A.
Figure B.9: C-scan showing dimension of delamination for 0.5 mm displacement indentation test for sample 4B.

Figure B.10: C-scan showing dimension of delamination for 0.5 mm displacement indentation test for sample 4D.
Figure B.11: C-scan showing dimension of delamination for 0.5 mm displacement indentation test for sample 4D.
C. INDENTATION RESULTS LOADED TO INTERMEDIATE LEVELS OF NICKEL-COATED STEEL

![Graph](image)

**Figure C.1:** Load as a function of displacements for 1.5 kN, and 2.0 kN load indentation tests.

![Graph](image)

**Figure C.2:** Acoustic emission events as a function load of 1.5 kN load indentation test, run 1.
Figure C.3: C-scan showing dimension of delamination for 1.5 kN load indentation test, run 1.

Figure C.4: Acoustic emission events as a function load of 1.5 kN load indentation test, run 2.
Figure C.5: C-scan showing dimension of delamination of 1.5 kN load indentation test, run 2.

Figure C.6: Acoustic emission events as a function load of 2.0 kN load indentation test, run 1.
Figure C.7: C-scan showing dimension of delamination for 2.0 kN load indentation test, run 1.

Figure C.8: Acoustic emission events as a function load of 2.0 kN load indentation test, run 2.
Figure C.9: C-scan showing dimension of delamination of 2.0 kN load indentation test, run 2.

Figure C.10: Load as a function of displacement for 2.6 kN load indentation test, run 1.
Figure C.11: Acoustic emission events as a function load of 2.6 kN load indentation test, run 1.

Figure C.12: C-scan showing dimension of delamination for 2.6 kN load indentation test, run 1.
Figure C.13: Acoustic emission events as a function load of 2.6 kN load indentation test, run 2.

Figure C.14: C-scan showing dimension of delamination of 2.6 kN load indentation test, run 2.
Figure C.15: Acoustic emission events as a function load of 3.2 kN load indentation test, run 1.

Figure C.16: C-scan showing dimension of delamination for 3.2 kN load indentation test, run 1.
Figure C.17: Acoustic emission events as a function of load of 3.2 kN load indentation test, run 2.

Figure C.18: C-scan showing dimension of delamination of 3.2 kN load indentation test, run 2.
D. ALUMINUM-COATED ALUMINUM INDENTATION TEST RESULTS

Figure D.1: Load as a function of displacement for indentation with 4.8 mm (3/16”) diameter indenter into 0.37 mm average thickness coating for Sample Al1.

Figure D.2: Acoustic emission events as a function of load for indentation with 4.8 mm (3/16”) diameter indenter into 0.37 mm average thickness coating for sample Al1.
Figure D.3: Load as a function of displacement for indentation with 2.0 mm diameter indenter into 0.58 mm average thickness coating, loaded to 1.02 kN for sample Al2.

Figure D.4: Acoustic emission events as a function of load for indentation with 2.0 mm diameter indenter into 0.58 mm average thickness coating, loaded to 1.02 kN for sample Al2.
Figure D.5: Load as a function of displacement for indentation with 2.0 mm diameter indenter into 0.58 mm average thickness coating, loaded to 4.0 kN for sample Al3.

Figure D.6: Acoustic emission events as a function of load for indentation with 2.0 mm diameter indenter into 0.58 mm average thickness coating, loaded to 4.0 kN for sample Al3.
Figure D.7: Load as a function of displacement for indentation with 2.0 mm diameter indenter into 0.37 mm average thickness coating for sample Al4.

Figure D.8: Acoustic emission events as a function of load for indentation with 2.0 mm diameter indenter into 0.37 mm average thickness coating for sample Al4.
Figure D.9: Load as a function of displacement for indentation with 2.5 mm diameter indenter into 0.37 mm average thickness coating for sample Al5.

Figure D.10: Acoustic emission events as a function of load for indentation with 2.5 mm diameter indenter into 0.37 mm average thickness coating for sample Al5.
Figure D.11: Load as a function of displacement for indentation with 2.5 mm diameter indenter into 0.5 mm average thickness coating for sample Al6.

Figure D.12: Acoustic emission events as a function of load for indentation with 2.5 mm diameter indenter into 0.5 mm average thickness coating for sample Al6.
Figure D.13: Load as a function of displacement for indentation with 2.0 mm diameter indenter into 0.9 mm average thickness coating for samples B1, A2, and A3.

Figure D.14: C-scan of the (a) amplitude (b) and time of flight of the first wall reflection of delamination following an indentation with 2.0 mm diameter indenter into 0.9 mm average thickness coating for sample B1.
Figure D.15: Acoustic emission events as a function of displacement for indentation with 2.0 mm diameter indenter into 0.9 mm average thickness coating for sample A2.
Figure D.16: C-scan of the (a) amplitude (b) and time of flight of the first wall reflection of delamination following an indentation with 2.0 mm diameter indenter into 0.9 mm average thickness coating for sample A2.
Figure D.17: Acoustic emission events as a function of displacement for indentation with 2.0 mm diameter indenter into 0.9 mm average thickness coating for sample A3.
Figure D.18: C-scan of the (a) amplitude (b) and time of flight of the first wall reflection of delamination following an indentation with 2.0 mm diameter indenter into 0.9 mm average thickness coating for sample A3.
APPENDIX B. COMPUTER CODE

A. ANSYS AXISYMMETRIC INDENTATION MODEL OF NICKEL THERMALLY SPRAYED ONTO STEEL

```plaintext
#axisymmetric model of an indentation test
#nickel coated steel
#can be used in a sequential analysis with file that imports results
#and applies them as initial conditions
# coding: mbcs
#
#the following sets and surfaces need to be defined and copied to the import file
#coating sets: Coat (entire coating), CoatOut (outside edge), CoatSym (inside edge)
#coating surfaces: CoatBot, CoatSurf, FluxReg
#plate sets: Plate (entire plate), PlateBot, PlateOut, PlateSym
#plate surfaces: PlateTop
#indenter sets: IndSym,
#cohesive zone surfaces: CZBot, CZTop
#
#to include residual stresses insert the command:
##*INITIAL CONDITIONS, type=STRESS, USER
#right before the first *STEP command in the input file and select
##user subroutine file
from part import *
from material import *
from section import *
from assembly import *
from step import *
from interaction import *
from load import *
from mesh import *
from job import *
from sketch import *
from visualization import *
from connectorBehavior import *

#units: m (length), Pa (Pressure), J (energy), kg (mass), K (temperature)

#Geometry Parameters
#disk radius
r=0.008
#disk height
h=0.0026
#coating thickness
coat=.0006
#cohesive zone thickness
czt=7e-06
#define precrack
precrack=0
#indenter rad
rad=0.001

#indenter displacement
disp=0.00039

#Cohesive zone properties
#mode I critical energy release rate
G1c=150
```
#modes II and III critical energy release rates
GIIc=475
#mode I cohesive strength
SigmaMax=70e6
#modes II and III cohesive strengths
TauMax=70e6

#mesh parameters
xrat=30 # ratio that elements get bigger in x direction
yrat=10 # substrate ratio elements get bigger going down in y direction
xnum=10 # number of elements
ynum=2 # number of elements in substrate in y direction
coatthick=1 # number of elements -1? should be 2, trying 4

#Define material properties
#coating
mdb.models['Model-1'].Material(name='Top')
mdb.models['Model-1'].materials['Top'].Density(table=((6000.0, ), )) #density
used to be 16654
mdb.models['Model-1'].materials['Top'].Elastic(table=((225E9, 0.32, 0.0),
(225E9, 0.32, 973.0)), temperatureDependency=OFF) #110GPa
mdb.models['Model-1'].materials['Top'].Plastic(table=((300e6, 0.000), (405e6, 0.0953)) #1400 MPa #500 MPaS
mdb.models['Model-1'].materials['Top'].Conductivity(table=((55.13, 0.0),
(51.62, 900.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Top'].SpecificHeat(table=((0.1338, 0.0),
(0.131, 900.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Top'].Expansion(table=((6.3e-06, ), ))

#indenter
mdb.models['Model-1'].Material(name='Ind')
mdb.models['Model-1'].materials['Ind'].Density(table=((16654.0, ), ))
mdb.models['Model-1'].materials['Ind'].Elastic(table=((650000000000.0, 0.22,
0.0), (650000000000.0, 0.22, 973.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Ind'].Conductivity(table=((55.13, 0.0),
(51.62, 900.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Ind'].SpecificHeat(table=((0.1338, 0.0),
(0.131, 900.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Ind'].Expansion(table=((6.3e-06, ), ))

#substrate
mdb.models['Model-1'].Material(name='Bottom')
mdb.models['Model-1'].materials['Bottom'].Density(table=((7850.0, ), ))
mdb.models['Model-1'].materials['Bottom'].Elastic(table=((190E9, 0.30,
0.0), (190E9, 0.30, 900.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Bottom'].Plastic(table=((550000000.0,
0.000), (800000000.1, 0.9)))
#(500000000.0, 0.002), (750000000.0, 0.006), (1000000000.0, 0.011),
(1300000000.0, 0.016), (1500000000.0, 0.026), (1600000000.0, 0.04),
(1750000000.0, 0.5), (2000000000.0, 0.99)) #reduce all by factor of 10
steel was 260MPa
mdb.models['Model-1'].materials['Bottom'].Expansion(table=((1.35e-05, ), ))
mdb.models['Model-1'].materials['Bottom'].Conductivity(table=((40.69, 0.0),
(31.69, 900.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Bottom'].SpecificHeat(table=((512.7, 0.0),
(755.84, 900.0)), temperatureDependency=ON)

#cohesive zone
mdb.models['Model-1'].Material(name='Coh')
mdb.models['Model-1'].materials['Coh'].Density(table=((7850, ), ))
mdb.models['Model-1'].materials['Coh'].Elastic(table=((5e16,
5e16, 5e16), ), type=TRACTION)
# Define geometry
# substrate
mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.1)
mdb.models['Model-1'].sketches['__profile__'].sketchOptions.setValues(
decimalPlaces=3)
mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -0.05), point2=(0.0, 0.05))
mdb.models['Model-1'].sketches['__profile__'].rectangle(point1=(0.0, 0.0), point2=(r, (-h)))

mdb.models['Model-1'].Part(dimensionality=AXISYMMETRIC, name='Plate', type=DEFORMABLE_BODY)

mdb.models['Model-1'].parts['Plate'].BaseShell(sketch= mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

# define coating
mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.1)
 mdb.models['Model-1'].sketches['__profile__'].sketchOptions.setValues(
decimalPlaces=3, viewStyle=AXISYM)
 mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -0.05), point2=(0.0, 0.05))
 mdb.models['Model-1'].sketches['__profile__'].rectangle(point1=(0.0, 0.00), point2=(r, coat))

mdb.models['Model-1'].Part(dimensionality=AXISYMMETRIC, name='Coating', type=DEFORMABLE_BODY)

mdb.models['Model-1'].parts['Coating'].BaseShell(sketch= mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

# Define cohesive zone rectangle
mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.1)
 mdb.models['Model-1'].sketches['__profile__'].sketchOptions.setValues(
decimalPlaces=3)
 mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -czt/2), point2=(0.0, czt/2))
 mdb.models['Model-1'].sketches['__profile__'].rectangle(point1=(precrack, 0.0), point2=(r, czt))

mdb.models['Model-1'].Part(dimensionality=AXISYMMETRIC, name='CZ', type=DEFORMABLE_BODY)

mdb.models['Model-1'].parts['CZ'].BaseShell(sketch= mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

# indenter
mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.02)
 mdb.models['Model-1'].sketches['__profile__'].sketchOptions.setValues(
decimalPlaces=4, viewStyle=AXISYM)
 mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -0.01), point2=(0.0, 0.01))
 mdb.models['Model-1'].sketches['__profile__'].FixedConstraint(entity= mdb.models['Model-1'].sketches['__profile__'].geometry[2])
 mdb.models['Model-1'].sketches['__profile__'].ArcByCenterEnds(center=(0.0,
rad+coat), direction=COUNTERCLOCKWISE, point1=(0.0, coat),
point2=(rad+coat,
rad+coat))
mdb.models['Model-1'].sketches['__profile__'].CoincidentConstraint(entity1=
mdb.models['Model-1'].sketches['__profile__'].vertices[2], entity2=
mdb.models['Model-1'].sketches['__profile__'].geometry[2])
mdb.models['Model-1'].sketches['__profile__'].CoincidentConstraint(entity1=
mdb.models['Model-1'].sketches['__profile__'].vertices[0], entity2=
mdb.models['Model-1'].sketches['__profile__'].geometry[2])
mdb.models['Model-1'].Part(dimensionality=AXISYMMETRIC, name='Indenter',
type=
ANALYTIC_RIGID_SURFACE)
mdb.models['Model-1'].parts['Indenter'].AnalyticRigidSurf2DPlanar(sketch= mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

mdb.models['Model-1'].parts['Indenter'].ReferencePoint(point=(0.0, rad+coat, 0.0))
mdb.models['Model-1'].rootAssembly.DatumCsysByThreePoints(coordSysType=
CYLINDRICAL, origin=(0.0, 0.0, 0.0), point1=(1.0, 0.0, 0.0), point2=(0.0, 0.0, -1.0))
mdb.models['Model-1'].rootAssembly.Instance(dependent=ON, name='Indenter-1',
part=mdb.models['Model-1'].parts['Indenter'])
mdb.models['Model-1'].RigidBody(name='IndConstr', refPointRegion=Region(
referencePoints=
(mdb.models['Model-1'].rootAssembly.instances['Indenter-1'].referencePoints[2],),
surfaceRegion=Region(
side2Edges=mdb.models['Model-1'].rootAssembly.instances['Indenter-1'].edges.getSequenceFromMask(
mask=('[#1 ]', ), ))))

#define sections
mdb.models['Model-1'].CohesiveSection(initialThickness=1.0,
initialThicknessType=SPECIFY, material='Coh', name='CohSect',
outOfPlaneThickness=None, response=TRACTION_SEPARATION)
mdb.models['Model-1'].HomogeneousSolidSection(material='Top', name='TopSect',
thickness=None)
mdb.models['Model-1'].HomogeneousSolidSection(material='Bottom', name=
'BotSect', thickness=None)

#Assign sections
mdb.models['Model-1'].parts['CZ'].SectionAssignment(offset=0.0,
offsetField='',
offsetType=MIDDLE_SURFACE, region=Region(
faces=mdb.models['Model-1'].parts['CZ'].faces.getSequenceFromMask(mask=(
'#[1 ]', ), ), ), sectionName='CohSect')
mdb.models['Model-1'].parts['Coating'].SectionAssignment(offset=0.0,
offsetField='', offsetType=MIDDLE_SURFACE, region=Region(
faces=mdb.models['Model-1'].parts['Coating'].faces.getSequenceFromMask(
mask=('#[1 ]', ), ), ), sectionName='TopSect')
mdb.models['Model-1'].parts['Plate'].SectionAssignment(offset=0.0,
offsetField='', offsetType=MIDDLE_SURFACE, region=Region(
faces=mdb.models['Model-1'].parts['Plate'].faces.getSequenceFromMask(mask=(
'#[1 ]', ), ), ), sectionName='BotSect')

#create instances
mdb.models['Model-1'].rootAssembly.DatumCsysByThreePoints(coordSysType=
CYLINDRICAL, origin=(0.0, 0.0, 0.0), point1=(1.0, 0.0, 0.0), point2=(0.0, 0.0, -1.0))
mdb.models['Model-1'].rootAssembly.Instance(dependent=ON, name='CZ-1', part=
```
mdb.models['Model-1'].parts['CZ'])
mdb.models['Model-1'].rootAssembly.Instance(dependent=ON, name='Coating-1',
part= mdb.models['Model-1'].parts['Coating'])
mdb.models['Model-1'].rootAssembly.Instance(dependent=ON, name='Plate-1',
part= mdb.models['Model-1'].parts['Plate'])
#mesh cohesive
mdb.models['Model-1'].parts['CZ'].setMeshControls(elemShape=QUAD, regions=
 mdb.models['Model-1'].parts['CZ'].faces.getSequenceFromMask(('[#1 ]', ),
),
technique=SWEEP)
mdb.models['Model-1'].parts['CZ'].seedEdgeByBias(end1Edges=
 mdb.models['Model-1'].parts['CZ'].edges.getSequenceFromMask(('[#4 ]', ),
),
end2Edges=mdb.models['Model-1'].parts['CZ'].edges.getSequenceFromMask((
'#[1 ]', ), ), number=650, ratio=1.0)
mdb.models['Model-1'].parts['CZ'].seedEdgeByNumber(edges=
 mdb.models['Model-1'].parts['CZ'].edges.getSequenceFromMask(('[#a ]', ),
),
number=1)
#mesh coating
mdb.models['Model-1'].parts['Coating'].seedEdgeByBias(biasMethod=SINGLE,
constraint=FINER, end1Edges=
 mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(('[#2 ]',
),
), end2Edges=
 mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(('[#8 ]',
), ), number=9, ratio=1.0)
mdb.models['Model-1'].parts['Coating'].seedEdgeByNumber(edges=
 mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(('[#4 ]',
),
), end2Edges=
 mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(('[#1 ]',
),
), number=150, ratio=1.5)
mdb.models['Model-1'].parts['Coating'].setMeshControls(elemShape=QUAD, regions=
 mdb.models['Model-1'].parts['Coating'].faces.getSequenceFromMask(('[#1 ]',
),
), technique=STRUCTURED)
#mesh plate
#in radial direction
mdb.models['Model-1'].parts['Plate'].seedEdgeByBias(end1Edges=
 mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask(('[#4 ]',
),
), end2Edges=
 mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask(('[#1 ]',
),
), number=150, ratio=1.5)
#through thickness
mdb.models['Model-1'].parts['Plate'].seedEdgeByBias(end1Edges=
 mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask(('[#2 ]',
),
), end2Edges=
 mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask(('[#8 ]',
),
), number=40, ratio=4.0)
mdb.models['Model-1'].parts['Plate'].setMeshControls(elemShape=QUAD, regions=
```

mdb.models['Model-1'].parts['Plate'].faces.getSequenceFromMask(('[#1 ]', ), technique=STRUCTURED)

# assign element types
mdb.models['Model-1'].parts['CZ'].setElementType(elemTypes=(ElemType(
    elemCode=COHAX4, elemLibrary=STANDARD, elemDeletion=ON,
    viscosity=0.0005),
    ElemType(elemCode=UNKNOWN_TRI, elemLibrary=STANDARD)), regions=(
    mdb.models['Model-1'].parts['CZ'].faces.getSequenceFromMask(('[#1 ]', ), ),))
mdb.models['Model-1'].parts['Coating'].setElementType(elemTypes=(ElemType(
    elemCode=CAX4RT, elemLibrary=STANDARD, secondOrderAccuracy=OFF,
    hourglassControl=ENHANCED, distortionControl=DEFAULT),
    ElemType(elemCode=CAX3T, elemLibrary=STANDARD)), regions=(
    mdb.models['Model-1'].parts['Coating'].faces.getSequenceFromMask(('[#1 ]', ), ),))

mdb.models['Model-1'].parts['Plate'].setElementType(elemTypes=(ElemType(
    elemCode=CAX4RT, elemLibrary=STANDARD), ElemType(
    elemCode=CAX3T, elemLibrary=STANDARD, secondOrderAccuracy=OFF,
    hourglassControl=ENHANCED, distortionControl=DEFAULT)), regions=(
    mdb.models['Model-1'].parts['Plate'].faces.getSequenceFromMask(('[#1 ]', ), ),))

# generate meshes
mdb.models['Model-1'].parts['CZ'].generateMesh()
mdb.models['Model-1'].parts['Coating'].generateMesh()
mdb.models['Model-1'].parts['Plate'].generateMesh()

# collapse cohesive zone
mdb.meshEditOptions.setValues(enableUndo=True, maxUndoCacheElements=0.5)
mdb.models['Model-1'].parts['CZ'].editNode(coordinate2=0.0, nodes=mdb.models['Model-1'].parts['CZ'].nodes.getSequenceFromMask(mask=('ffffffff:40 #3fffff ', ), ))

# define necessary surfaces
mdb.models['Model-1'].parts['Coating'].set(name='CoatSym', nodes=mdb.models['Model-1'].parts['Coating'].nodes.getSequenceFromMask(mask=(' [#1 0:3 #800000 #0:4 #4000 #0:4 #20', '#0:3 #1000000 #0:4 #80000 #0:4 #400 #0:4', '#2 #0:3 #10000000 #0:4 #8000 ', ), ))

mdb.models['Model-1'].parts['Plate'].set(name='PlateSym', nodes=mdb.models['Model-1'].parts['Plate'].nodes.getSequenceFromMask(mask=(' [#0:4 #400000 #0:4 #200 #0:4 #10 #0:3', '#8000000 #0:4 #40000 #0:4 #200 #0:4 #1', '#0:3 #8000000 #0:4 #4000 #0:4 #20 #0:3', '#10000000 #0:4 #80000 #0:4 #400 #0:4 #2', '#0:3 #10000000 #0:4 #8000 #0:4 #40 #0:3', '#20000000 #0:4 #10000000 #0:4 #800 #0:4 #4', '#0:3 #20000000 #0:4 #10000 #0:4 #80 #0:3', '#40000000 #0:4 #20000000 #0:4 #100 #0:4 #8', '#0:3 #40000000 #0:4 #20000 #0:4 #100 #0:3', '#80000000 #0:4 #40000000 #0:4 #200 #0:4 #10', '#0:3 #80000000 #0:4 #40000000 #0:4 #200 #0:4', '#1 #0:3 #80000000 #0:4 #40000000 #0:4 #200 #0:4', '#0:188 #ff000000 ffffffff:4 #7fff ', ), ))
mdb.models['Model-1'].rootAssembly.regenerate()
mdb.models['Model-1'].rootAssembly.Set(name='Outside', nodes=
mdb.models['Model-1'].rootAssembly.instances['Coating1'].nodes.getSequenceFromMask(
mask=('[#0:9 #2000 #0:4 #10 #0:3 #8000000 #0:4',
' #40000 #0:4 #200 #0:4 #1 #0:3 #800000', ' #0:4 #4000 ]', ), )+\
mdb.models['Model-1'].rootAssembly.instances['Plate1'].nodes.getSequenceFromMask(
mask=('[#0:4 #800000 #0:4 #4000 #0:4 #20 #0:3',
' #10000000 #0:4 #80000 #0:4 #400 #0:4 #2',
' #0:3 #1000000 #0:4 #8000 #0:4 #40 #0:3',
' #20000000 #0:4 #100000 #0:4 #800 #0:4 #4',
' #0:3 #2000000 #0:4 #10000 #0:4 #80 #0:3',
' #40000000 #0:4 #200000 #0:4 #1000 #0:4 #8',
' #0:3 #4000000 #0:4 #20000 #0:4 #100 #0:3',
' #80000000 #0:4 #400000 #0:4 #2000 #0:4 #10',
' #0:3 #8000000 #0:4 #40000 #0:4 #200 #0:4',
' #1 #0:3 #800000 #0:4 #4000 #0:4 #20',
' #0:3 #10000000 #0:4 #80000 #0:4 #400 #0:4', ' #2 #0:3 #1000000 ]', ),
))
mdb.models['Model-1'].rootAssembly.Set(name='Corner', nodes=
mdb.models['Model-1'].rootAssembly.instances['Coating1'].nodes.getSequenceFromMask(
mask=('[#0:47 #20 ]', ), ))
mdb.models['Model-1'].parts['CZ'].Set(elements=
mdb.models['Model-1'].parts['CZ'].elements.getSequenceFromMask(mask=(
'[#ffffffff:20 #3ff ]', ), ), name='CZElements')
mdb.models['Model-1'].parts['CZ'].Surface(face3Elements=
mdb.models['Model-1'].parts['CZ'].elements.getSequenceFromMask(mask=(
'[#ffffffff:20 #3ff ]', ), ), name='CZTop')
mdb.models['Model-1'].parts['CZ'].Surface(face1Elements=
mdb.models['Model-1'].parts['CZ'].elements.getSequenceFromMask(mask=(
'[#ffffffff:20 #3ff ]', ), ), name='CZBot')
mdb.models['Model-1'].parts['Coating'].Surface(name='CoatSurf', side1Edges=
mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(('[#4
]',
), ))
mdb.models['Model-1'].parts['Coating'].Surface(name='CoatBot', side1Edges=
mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(('[#1
]',
), ))
mdb.models['Model-1'].parts['Plate'].Surface(name='PlateTop', side1Edges=
mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask(('[#1 ]',
),
))
mdb.models['Model-1'].parts['Indenter'].Set(name='IndLoad', referencePoints=(
mdb.models['Model-1'].parts['Indenter'].referencePoints[2], ))
#define contact interactions
mdb.models['Model-1'].ContactProperty('InterfaceContact')
mdb.models['Model1'].interactionProperties['InterfaceContact'].TangentialBehavior(
formulation=FRICTIONLESS)
mdb.models['Model1'].interactionProperties['InterfaceContact'].NormalBehavior(
allowSeparation=ON, constraintEnforcementMethod=DEFAULT,
pressureOverclosure=HARD)
mdb.models['Model1'].interactionProperties['InterfaceContact'].tangentialBehavior.setValues(

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dependencies=0, directionality=ISOTROPIC, elasticSlipStiffness=None,
formulation= PENALTY, fraction=0.005, maximumElasticSlip=FRACTION,
pressureDependency=OFF, shearStressLimit=None, slipRateDependency=OFF,
temperatureDependency=OFF)
mdb.models['Model-1'].interactionProperties['InterfaceContact'].normalBehavior.setValues(
allowSeparation=ON, clearanceAtZeroContactPressure=0.0,
constraintEnforcementMethod= PENALTY, contactStiffness=DEFAULT,
contactStiffnessScaleFactor=1.0, pressureOverclosure=HARD,
stiffnessBehavior=LINEAR)
mdb.models['Model-1'].interactionProperties['InterfaceContact'].tangentialBehavior.setValues(
dependencies=0, directionality=ISOTROPIC, elasticSlipStiffness=None,
formulation= PENALTY, fraction=0.005, maximumElasticSlip=FRACTION,
pressureDependency=OFF, shearStressLimit=None, slipRateDependency=OFF,
temperatureDependency=OFF)
mdb.models['Model-1'].SurfaceToSurfaceContactStd(adjustMethod=NONE,
clearanceRegion=None, createStepName='Initial', datumAxis=None,
enforcement=SURFACE_TO_SURFACE, initialClearance=omit,
interactionProperty='InterfaceContact', master=
mdb.models['Model-1'].rootAssembly.instances['Plate-1'].surfaces['PlateTop'],
  name='Int-1', slave=
mdb.models['Model-1'].rootAssembly.instances['Coating-1'].surfaces['CoatBot'],
  sliding=FINITE, surfaceSmoothing=NONE, thickness=ON)
mdb.models['Model-1'].Tie(adjust=ON, master=
mdb.models['Model-1'].rootAssembly.instances['Coating-1'].surfaces['CoatBot'],
  name='Top', positionToleranceMethod=COMPUTED, slave=
mdb.models['Model-1'].rootAssembly.instances['CZ-1'].surfaces['CZTop'],
  thickness=ON, tieRotations=ON)
mdb.models['Model-1'].Tie(adjust=ON, master=
mdb.models['Model-1'].rootAssembly.instances['Plate-1'].surfaces['PlateTop'],
  name='Bottom', positionToleranceMethod=COMPUTED, slave=
mdb.models['Model-1'].rootAssembly.instances['CZ-1'].surfaces['CZBot'],
  thickness=ON, tieRotations=ON)
mdb.models['Model-1'].SurfaceToSurfaceContactStd(adjustMethod=NONE,
clearanceRegion=none, createStepName='Initial', datumAxis=None,
enforcement=SURFACE_TO_SURFACE, initialClearance=omIT,
interactionProperty='InterfaceContact', master=
Region(side2Edges=mdb.models['Model-1'].rootAssembly.instances['Indenter-1'].edges.getSequenceFromMask(
  mask=('[#1 ]', )), name='IndenterCont', slave=
mdb.models['Model-1'].rootAssembly.instances['Coating-1'].surfaces['CoatSurf'],
  sliding=FINITE, thickness=ON)

#define boundary conditions
mdb.models['Model-1'].DisplacementBC(amplitude=UNSET,
createStepName='Initial',
distributionType=UNIFORM, fieldName='', localCsys=None, name='SymCoat',
region=
mdb.models['Model-1'].rootAssembly.instances['Coating-1'].sets['CoatSym'],
u1=SET, u2=UNSET, ur3=UNSET)
mdb.models['Model-1'].DisplacementBC(amplitude=UNSET,
createStepName='Initial',
distributionType=UNIFORM, fieldName='', localCsys=None, name='SymPlate',
region= mdb.models['Model-1'].rootAssembly.instances['Plate-1'].sets['PlateSym'],
u1=SET, u2=UNSET, ur3=UNSET)
mdb.models['Model-1'].DisplacementBC(amplitude=UNSET,
createStepName='Initial',
distributionType=UNIFORM, fieldName='', localCsys=None, name='PlateBot',
region= mdb.models['Model-1'].rootAssembly.instances['Plate-1'].sets['PlateBot'],
u1=UNSET, u2=SET, ur3=UNSET)

#define load and remove load step
mdb.models['Model-1'].StaticStep(initialInc=1e-06, maxNumInc=100000, minInc=1e-21, name='LoadStep', nleom=ON, previous='Initial')
mdb.models['Model-1'].StaticStep(initialInc=1e-06, maxNumInc=75000, minInc=1e-21, name='RemoveLoad', previous='LoadStep')

mdb.models['Model-1'].steps['LoadStep'].setValues(adaptiveDampingRatio=0.05,
continueDampingFactors=True, stabilizationMethod=DISSIPATED_ENERGY_FRACTION)

mdb.models['Model-1'].steps['RemoveLoad'].setValues(adaptiveDampingRatio=0.05,
continueDampingFactors=True, stabilizationMethod=DISSIPATED_ENERGY_FRACTION)

#define load boundary conditions
mdb.models['Model-1'].DisplacementBC(amplitude=UNSET,
createStepName='LoadStep',
distributionType=UNIFORM, fieldName='', fixed=OFF, localCsys=None, name='IndenterLoad',
region= mdb.models['Model-1'].rootAssembly.instances['Indenter-1'].sets['IndLoad'],
u1=0, u2=-disp, ur3=0)

mdb.models['Model-1'].boundaryConditions['IndenterLoad'].setValuesInStep(stepName='RemoveLoad', u2=0.0)

#request restart files for explicit portion of sequential
#analysis
mdb.models['Model-1'].steps['RemoveLoad'].Restart(frequency=1, numberIntervals=0, overlay=ON, timeMarks=OFF)

#request field output
mdb.models['Model-1'].fieldOutputRequests['F-Output-1'].setValues(variables=('S', 'PE', 'PEEQ', 'PEMAG', 'LE', 'U', 'RF', 'CF', 'CSTRESS', 'CDISP', 'STATUS'))

mdb.models['Model-1'].FieldOutputRequest(createStepName='LoadStep', name='F-Output-2', rebar=EXCLUDE, region= mdb.models['Model-1'].rootAssembly.instances['CZ-1'].sets['CZElements'], sectionPoints=DEFAULT, variables=('SDEG', 'CFAILURE', 'DMICRT'))

#couple outside surface
mdb.models['Model-1'].Equation(name='CouplingEqn', terms=((1.0, 'Outside', 1),
(-1.0, 'Corner', 1)))

mdb.models['Model-1'].HistoryOutputRequest(createStepName='LoadStep', name='H-Output-2', rebar=EXCLUDE, region= mdb.models['Model-1'].rootAssembly.instances['Indenter-1'].sets['IndLoad'],
sectionPoints=DEFAULT, variables=('RF1', 'RF2'))
#define job
mdb.models['Model-1'].rootAssembly.regenerate()

mdb.Job(atTime=None, contactPrint=OFF, description='', echoPrint=OFF,
    explicitPrecision=DOUBLE, getMemoryFromAnalysis=True, historyPrint=OFF,
    memory=90, memoryUnits=PERCENTAGE, model='Model-1', modelPrint=OFF,
    multiprocessingMode=DEFAULT, name='Nickel8', nodalOutputPrecision=FULL,
    numCpus=1, numDomains=1, parallelizationMethodExplicit=DOMAIN,
    queue=None,
    scratch='', type=ANALYSIS, userSubroutine='', waitHours=0, waitMinutes=0)
B. ABAQUS EXPLICIT THERMAL-STRUCTURAL MODEL SIMULATION FLAW EVOLUTION UNDER SEVERE COATING SURFACE THERMAL AND PRESSURE TRANSIENTS

*Heading
** Job name: Job-1 Model name: Model-1
** Generated by: Abaqus/CAE 6.9-2
** Imports cohesive zone, coating and substrate, including
** mesh, material state, and properties.
** will automatically import last available increment
** of second step (after the indenter load has been removed)
** of job "Nickel"
** units: m (length), Pa (Pressure), J (energy), kg (mass), K
** (temperature)
** *Preprint, echo=NO, model=NO, history=NO, contact=NO
** ** ASSEMBLY
** *
** Assembly, name=Assembly
** **
** **Cohesive zone
*Instance, Instance=CZ-1, Library=Nickel
**-----------------Cohesive zone sets-----------------
*Nset, nset=_PickedSet2, internal, generate
  1,  1302,     1
*Elset, elset=_PickedSet2, internal, generate
  1,  650,    1
*Elset, elset=CZElements, generate
  1,  650,    1
*Elset, elset=_CZTop_S3, internal, generate
  1,  650,    1
*Surface, type=ELEMENT, name=CZTop
  _CZTop_S3, S3
*Elset, elset=_CZBot_S1, internal, generate
  1,  650,    1
*Surface, type=ELEMENT, name=CZBot
  _CZBot_S1, S1
*Import, Step=2, State=Yes, Update=No
*End Instance
**
**Coating
*Instance, Instance=Coating-1, Library=Nickel
**-----------------Coating sets----------------------
*Nset, nset=_PickedSet2, internal, generate
  1,  3381,     1
*Elset, elset=_PickedSet2, internal, generate
  1,  3200,     1
*Nset, nset=CoatSym, generate
  1,  3200,   161
*Elset, elset=CoatSurf_S3, internal, generate
  3041,  3200,    1
*Surface, type=ELEMENT, name=CoatSurf
  _CoatSurf_S3, S3
*Elset, elset=_CoatBot_S1, internal, generate
  1, 160, 1
*Surface, type=ELEMENT, name=CoatBot
  _CoatBot_S1, S1
*Elset, elset=_FluxReg_S3, internal, generate
  3041, 3200, 1
*Surface, type=ELEMENT, name=FluxReg
  _FluxReg_S3, S3
*Import, Step=2, State=Yes, Update=no
*End Instance

**Substrate
*Instance, Instance=Plate-1, Library=Nickel
**-------------------Plate sets-------------------
*Nset, nset=_PickedSet2, internal, generate
  1, 6601, 1
*Elset, elset=_PickedSet2, internal, generate
  1, 6400, 1
*Nset, nset=PlateBot, generate
  6441, 6601, 1
*Nset, nset=PlateSym, generate
  161, 6601, 161
*Elset, elset=_PlateTop_S1, internal, generate
  1, 160, 1
*Surface, type=ELEMENT, name=PlateTop
  _PlateTop_S1, S1
** Section: BotSect
*Import, Step=2, State=Yes, Update=No
*End Instance

**-------------------Constraints-------------------
*Nset, nset=Corner, instance=Coating-1
  3381,
*Nset, nset=Outside, instance=Coating-1, generate
  322, 3220, 161
*Nset, nset=Outside, instance=Plate-1, generate
  162, 6441, 161
*Elset, elset=MassScale, instance=Coating-1
  1, 2, 3, 4, 161, 162, 163, 164, 321, 322, 323, 324,
  481, 482, 483, 484
  641, 642, 643, 644, 801, 802, 803, 804, 961, 962, 963, 964,
  1121, 1122, 1123, 1124
  1281, 1282, 1283, 1284, 1441, 1442, 1443, 1444, 1601, 1602, 1603, 1604,
  1761, 1762, 1763, 1764
  1921, 1922, 1923, 1924, 2081, 2082, 2083, 2084, 2241, 2242, 2243, 2244,
  2401, 2402, 2403, 2404
  2561, 2562, 2563, 2564, 2721, 2722, 2723, 2724, 2881, 2882, 2883, 2884,
  3041, 3042, 3043, 3044
** Constraint: Bottom
*Tie, name=Bottom, adjust=yes
  CZ-1.CZBot, Plate-1.PlateTop
** Constraint: CouplingEqn
*Equation
  2
  Outside, 1, 1.
  Corner, 1, -1.
** Constraint: Top
*Tie, name=Top, adjust=yes
  CZ-1.CZTop, Coating-1.CoatBot
*End Assembly
*Amplitude, name=Amp-1, definition=SMOOTH STEP
  0., 0., 0.0008, 1.
*Amplitude, name=Amp-2, definition=SMOOTH STEP
0., 1, 0.0004, 0.902
*Amplitude, name=Amp-3, definition=SMOOTH STEP
0., 0.902, 0.002, 0
*Amplitude, name=Amp-4, definition=SMOOTH STEP
0., 0, 0.0043, 0
*Amplitude, name=Amp-5, definition=SMOOTH STEP
0., 0., 0.0008, 0.913
*Amplitude, name=Amp-6, definition=SMOOTH STEP
0., 0.913, 0.0004, 1.
*Amplitude, name=Amp-7, definition=SMOOTH STEP
0., 1, 0.002, 0
*Amplitude, name=Amp-8, definition=SMOOTH STEP
0., 0, 0.0043, 0

**-----------------------Material Properties-----------------------
** MATERIALS
**
*Material, name=Bottom
*Conductivity
40.69, 0.
31.69, 900.
*Density
7850.,
*Elastic
1.9e+11, 0.3, 0.
1.446e+11, 0.3, 900.
*Expansion
1.35e-05,
*Plastic
5.5e+08, 0.
8e+08, 0.9
*Specific Heat
512.7, 0.
755.84, 900.
*Material, name=Coh
*Damage Initiation, criterion=QUADS
7e+07, 7e+07, 7e+07
*Damage Evolution, type=ENERGY, mixed mode behavior=POWER LAW, power=1.
175., 475., 475.
*Density
7850.,
*Elastic, type=TRACTION
5e+16, 5e+15, 5e+16
*Material, name=Top
*Conductivity
83., 0.
55.5, 267.
40., 1200.
*Density
6000.,
*Elastic
2.25e+11, 0.32, 0.
183.e9, 0.32, 625.
147.e9, 0.32, 900.
147.e9, 0.32, 1200.
*Expansion
1.34e-05, 0.
1.86e-05, 1700.
*Plastic
3e+08, 0., 0.
4.05e+08, 0.0953, 0.
281.e06, 0., 625.
365.e06, 0.2, 625.
37.5e06, 0., 950.
**Specific Heat**
0.105, 0.
0.13, 1000.
0.13, 1200.
**
**-----------------Interaction Properties------------------**
**
*Surface Interaction, name=InterfaceContact
1.,
*Friction
0.6,
*Surface Behavior, pressure-overclosure=HARD
*Gap Conductance
1.5e7, 0.
1.45e7, 5e-18
1e7, 5e-15
0.9e7, 1e-13
0.8e7, 5e-11.
68000, 1e-8
55000., 1e-07
18936., 1e-06
4800., 5e-6
2506, 0.00001
26, 0.001
2.6, 0.01
** 100., 1e-06
** 100., 0.0001
** 0., 1.
**
**----------------Boundary conditions------------------**
**
** Name: PlateBot Type: Displacement/Rotation
*Boundary
Plate-1.PlateBot, 2, 2
** Name: SymCoat Type: Displacement/Rotation
*Boundary
Coating-1.CoatSym, 1, 1
** Name: SymPlate Type: Displacement/Rotation
*Boundary
Plate-1.PlateSym, 1, 1
**
** ----------------------------------------------**
**
** STEP: Step-1
**
*Step, name=Step-1
*Dynamic Temperature-displacement, Explicit
, 0.0008
*Bulk Viscosity
0.24, 4.8
*FIXED MASS SCALING, FACTOR=10., ELSET=MassScale
**
** INTERACTIONS
**
** Interaction: Interface
*Contact Pair, interaction=InterfaceContact, mechanical constraint=PENALTY,
cpset=Interface
Coating-1.Coatbot, Plate-1.Platetop
** Interaction: Convection
*Sfilm, amplitude=Amp-1
Coating-1.FluxReg, F, 2800., 1.59e5
**Coating-1.FluxReg, F, 0., 193000.
** Interaction: Convection
**
** LOADS
**
** Name: PressCoatBot Type: Pressure
*Dsload, amplitude=Amp-5
Coating-1.CoatBot, PNU, 3.9e8
** Name: PressCoatSurf Type: Pressure
*Dsload, amplitude=Amp-5
Coating-1.CoatSurf, P, 3.9e8
** Name: PressCoatSurf Type: Pressure
*Dsload, amplitude=Amp-5
Plate-1.PlateTop, PNU, 3.9e8
** OUTPUT REQUESTS
**
*Restart, write, number interval=1, time marks=NO
**
** FIELD OUTPUT: F-Output-1
**
*Output, field
*Node Output
A, NT, RF, RFL, U, V
*Element Output, directions=YES
EVF, HFL, LE, PE, PEEQ, PEEQVAVG, PEVAVG, S, STATUS, SVAVG
*Contact Output
CSTRESS,
**
** FIELD OUTPUT: F-Output-2
**
*Element Output, elset=CZ-1.CZEElements, directions=YES
CFAILURE, DMICRT, SDEG
**
** HISTORY OUTPUT: H-Output-1
**
*Output, history, variable=PRESELECT
*End Step
** ---------------------------------------------------------------
**
** STEP: Step-2
**
*Step, name=Step-2
*Dynamic Temperature-displacement, Explicit
0.0004
*Bulk Viscosity
0.06, 1.2
*FIXED MASS SCALING, FACTOR=10., ELSET=MassScale
**
** INTERACTIONS
**
** Interaction: Convection
*Sfilm, amplitude=Amp-2
Coating-1.FluxReg, F, 2800., 2.75e5
**Coating-1.FluxReg, F, 300., 193000.
** Interaction: Convection
**
** LOADS
**
** Name: PressCoatBot Type: Pressure
*Dsload, amplitude=Amp-6
Coating-1.CoatBot, PNU, 3.9e8
** Name: PressCoatSurf Type: Pressure
*Dsload, amplitude=Amp-6
Coating-1.CoatSurf, P, 3.9e8
** Name: PressCoatSurf Type: Pressure
*Dsload, amplitude=Amp-6
Plate-1.PlateTop, PNU, 3.9e8
**
** OUTPUT REQUESTS
**
*Restart, write, number interval=1, time marks=NO
**
** FIELD OUTPUT: F-Output-1
**
*Output, field
*Node Output
A, NT, RF, RFL, U, V
*Element Output, directions=YES
EVF, HFL, LE, PE, PEEQ, PEEQVAVG, PEVAVG, S, STATUS, SVAVG
*Contact Output
CSTRESS,
**
** FIELD OUTPUT: F-Output-2
**
*Element Output, elset=CZ-1.CZEmentes, directions=YES
CFAILURE, DMICRT, SDEG
**
** HISTORY OUTPUT: H-Output-1
**
*Output, history, variable=PRESELECT
*End Step
**
** --- ----- ----- ----- ----- ----- ----- ----- ----- ----- ----- ----- ----- ----- ----
**
** STEP: Step-3
**
*Step, name=Step-3
*Dynamic Temperature-displacement, Explicit
  , 0.002
*Bulk Viscosity
  0.06, 1.2
*FIXED MASS SCALING, FACTOR=10., ELSET=MassScale
**
** INTERACTIONS
**
** Interaction: Convection
*Sfilm, amplitude=Amp-3
Coating-1.FluxReg, F, 2800., 22500.
**
** LOADS
**
** Name: PressCoatBot Type: Pressure
*Dsload, amplitude=Amp-7
Coating-1.CoatBot, PNU, 3.9e8
** Name: PressCoatSurf Type: Pressure
*Dsload, amplitude=Amp-7
Coating-1.CoatSurf, P, 3.9e8
** Name: PressCoatSurf Type: Pressure
*Dsload, amplitude=Amp-7
Plate-1.PlateTop, PNU, 3.9e8
**
** OUTPUT REQUESTS
**
*Restart, write, number interval=1, time marks=NO
**
** FIELD OUTPUT: F-Output-1

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**
*Output, field
*Node Output
A, NT, RF, RFL, U, V
*Element Output, directions=YES
EVF, HFL, LE, PE, PEEQ, PEEQVAVG, PEVAVG, S, STATUS, SVAVG
*Contact Output
CSTRESS,
**
** FIELD OUTPUT: F-Output-2
**
*Element Output, elset=CZ-1.CZElements, directions=YES
CFAILURE, DMICRT, SDEG
**
** HISTORY OUTPUT: H-Output-1
**
*Output, history, variable=PRESELECT
*End Step
C. ABAQUS ALUMINUM COLD-SPRAYED ONTO ALUMINUM FOUR-POINT BEND TEST SIMULATION

#Simulation of the four-point bend test of aluminum cold-sprayed
#onto aluminum
#
#this file, written in Python script must be run as an input
#file to include residual stresses. To include residual stresses #insert the
command: *INITIAL CONDITIONS, type=STRESS, USER
#right before the first *STEP command in the input file
#When submitting file,
#run with command abaqus job=jobname user=sigini interactive
#
from part import *
from material import *
from section import *
from assembly import *
from step import *
from interaction import *
from load import *
from mesh import *
from job import *
from sketch import *
from visualization import *
from connectorBehavior import *

#units: m (length), Pa (Pressure), J (energy), kg (mass), K (temperature)
#Geometry Parameters
#sample half-length
r=0.05715
#coating thickness
c=0.001524
#substrate thickness
h=0.003048
#sample width
width=0.02
#cohesive zone thickness
czt=0.0002
#precrack length
precrack=0.000

#cohesive zone material properties
#Normal cohesive strength (mode I)
SigmaMax=140e06
#Shear cohesive strength (modes II and III)
TauMax=20000e06
#Critical energy release rates
Glc=0
GIIc=Glc
#define material stiffness
stiff=7.5e+15
opening=0

#support geometry
#outer half load span and indenter radius
a=0.0535
r1=0.003175
#inner half load span and indenter radius
b=0.0265
r2=r1
# Fixture displacement

displacement=.0012

# Mesh parameters

xrat=10
yrat=10
xnum=25
ynum=20
coatthick=3

# Define material properties

# for coating

mdb.models['Model-1'].Material(name='Top')

mdb.models['Model-1'].materials['Top'].Density(table=((2810.0, ), ))

mdb.models['Model-1'].materials['Top'].Elastic(table=((71.7e9, 0.33), ))

mdb.models['Model-1'].Material(name='Fix')

mdb.models['Model-1'].materials['Fix'].Density(table=((7850.0, ), ))

mdb.models['Model-1'].materials['Fix'].Elastic(table=((300e9, 0.3), ))

# for substrate

mdb.models['Model-1'].Material(name='Bottom')

mdb.models['Model-1'].materials['Bottom'].Density(table=((2810, ), ))

mdb.models['Model-1'].materials['Bottom'].Elastic(table=((205e9, 0.33), ))

mdb.models['Model-1'].materials['Bottom'].Plastic(table=((1200e6, 0.0), ))

mdb.models['Model-1'].materials['Bottom'].Elastic(table=((71.7e9, 0.33), ))

mdb.models['Model-1'].materials['Bottom'].Plastic(table=((50300e6, 0.0), ))

# for cohesive zone

mdb.models['Model-1'].Material(name='Coh')

mdb.models['Model-1'].materials['Coh'].Density(table=((7850, ), ))

mdb.models['Model-1'].materials['Coh'].Elastic(table=((stiff, stiff, stiff), type=TRACTION))

mdb.models['Model-1'].materials['Coh'].QuadsDamageInitiation(table=((SigmaMax, TauMax, TauMax), ))

mdb.models['Model-1'].materials['Coh'].quadsDamageInitiation.DamageEvolution(table=((opening, ), ), type=DISPLACEMENT)

mdb.models['Model-1'].materials['Coh'].quadsDamageInitiation.damageEvolution.setValues(table=((GIC, ), ), type=ENERGY)

mdb.models['Model-1'].materials['Coh'].Regularization()

# Define geometry

# Define Substrate Part

mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.1)

mdb.models['Model-1'].sketches['__profile__'].sketchOptions.setValues(decimalPlaces=3)

mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -0.05), point2=(0.0, 0.05))

mdb.models['Model-1'].sketches['__profile__'].rectangle(point1=(0.0, 0.0), point2=(r, (-h)))

mdb.models['Model-1'].Part(dimensionality=TWO_D_PLANAR, name='Plate', type=DEFORMABLE_BODY)

mdb.models['Model-1'].parts['Plate'].BaseShell(sketch=

mdb.models['Model-1'].sketches['__profile__'])

de1 mdb.models['Model-1'].sketches['__profile__'].rectangle(point1=(0.0, 0.00),

coating

mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.1)

mdb.models['Model-1'].sketches['__profile__'].sketchOptions.setValues(decimalPlaces=3)

mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -0.05), point2=(0.0, 0.05))

mdb.models['Model-1'].sketches['__profile__'].rectangle(point1=(0.0, 0.00),
point2=(r, coat))
mdb.models['Model-1'].Part(dimensionality=TWO_D_PLANAR, name='Coating', type=DEFORMABLE_BODY)

mdb.models['Model-1'].parts['Coating'].BaseShell(sketch=mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

#Define cohesive zone rectangle
mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.1)
mdb.models['Model-1'].sketches['__profile__'].sketchOptions.setValues(decimalPlaces=3)
mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -czt/2), point2=(0.0, czt/2))
mdb.models['Model-1'].sketches['__profile__'].rectangle(point1=(precrack, 0.0), point2=(r, czt))
mdb.models['Model-1'].Part(dimensionality=TWO_D_PLANAR, name='CZ', type=DEFORMABLE_BODY)

mdb.models['Model-1'].parts['CZ'].BaseShell(sketch=mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

#Define top loader
mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.001)
mdb.models['Model-1'].sketches['__profile__'].sketchOptions.setValues(decimalPlaces=5)
mdb.models['Model-1'].sketches['__profile__'].ArcByCenterEnds(center=(a, coat+r1), direction=COUNTERCLOCKWISE, point1=(a-r1, coat+r1), point2=(a+r1, coat+r1))
mdb.models['Model-1'].sketches['__profile__'].Line(point1=(a-r1, coat+r1), point2=(a+r1, coat+r1))
mdb.models['Model-1'].sketches['__profile__'].HorizontalConstraint(entity=mdb.models['Model-1'].sketches['__profile__'].geometry[3])
mdb.models['Model-1'].sketches['__profile__'].PerpendicularConstraint(entity1=mdb.models['Model-1'].sketches['__profile__'].geometry[2], entity2=mdb.models['Model-1'].sketches['__profile__'].geometry[3])
mdb.models['Model-1'].Part(dimensionality=TWO_D_PLANAR, name='Outter', type=DEFORMABLE_BODY)

mdb.models['Model-1'].parts['Outter'].BaseShell(sketch=mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

#Define bottom loader
mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.001)
mdb.models['Model-1'].sketches['__profile__'].sketchOptions.setValues(decimalPlaces=5)
mdb.models['Model-1'].sketches['__profile__'].ArcByCenterEnds(center=(b, -h-r2), direction=CLOCKWISE, point1=(b-r2, -h-r2), point2=(b+r2, -h-r2))
mdb.models['Model-1'].sketches['__profile__'].Line(point1=(b-r2, -h-r2), point2=(b+r2, -h-r2))
mdb.models['Model-1'].sketches['__profile__'].HorizontalConstraint(entity=mdb.models['Model-1'].sketches['__profile__'].geometry[3])
mdb.models['Model-1'].sketches['__profile__'].PerpendicularConstraint(entity1=mdb.models['Model-1'].sketches['__profile__'].geometry[2], entity2=mdb.models['Model-1'].sketches['__profile__'].geometry[3])
mdb.models['Model-1'].Part(dimensionality=TWO_D_PLANAR, name='Inner', type=DEFORMABLE_BODY)

mdb.models['Model-1'].parts['Inner'].BaseShell(sketch=mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

#Define sections
mdb.models['Model-1'].CohesiveSection(initialThickness=1.0, initialThicknessType=SPECIFY, material='Coh', name='CohSect', outOfPlaneThickness=None, response=TRACTION SEPARATION)
mdb.models['Model-1'].HomogeneousSolidSection(material='Top', name='TopSect', thickness=None)
mdb.models['Model-1'].HomogeneousSolidSection(material='Bottom', name='BotSect', thickness=None)
mdb.models['Model-1'].HomogeneousSolidSection(material='Top', name='InnerSect', thickness=None)
mdb.models['Model-1'].HomogeneousSolidSection(material='Top', name='OutterSect', thickness=None)
mdb.models['Model-1'].sections['BotSect'].setValues(material='Bottom', thickness=width)
mdb.models['Model-1'].sections['CohSect'].setValues(material='Coh', outOfPlaneThickness=width, response=TRACTION SEPARATION)
mdb.models['Model-1'].sections['InnerSect'].setValues(material='Fix', thickness=width)
`mdb.models['Model-1'].sections['OutterSect'].setValues(material='Fix', thickness=width)`

`mdb.models['Model-1'].sections['TopSect'].setValues(material='Top', thickness=width)`

`#Assign sections
mdb.models['Model-1'].parts['CZ'].SectionAssignment(offset=0.0, offsetField='', offsetType=MIDDLE_SURFACE, region=Region(faces=mdb.models['Model-1'].parts['CZ'].faces.getSequenceFromMask(mask=('[#1 ]', ))), sectionName='CohSect')

mdb.models['Model-1'].parts['Coating'].SectionAssignment(offset=0.0, offsetField='', offsetType=MIDDLE_SURFACE, region=Region(faces=mdb.models['Model-1'].parts['Coating'].faces.getSequenceFromMask(mask=('[#1 ]', ))), sectionName='InnerSect')

mdb.models['Model-1'].parts['Outter'].SectionAssignment(offset=0.0, offsetField='', offsetType=MIDDLE_SURFACE, region=Region(faces=mdb.models['Model-1'].parts['Outter'].faces.getSequenceFromMask(mask=('[#1 ]', ))), sectionName='OutterSect')`
elemCode=CPS4R, elemLibrary=STANDARD, secondOrderAccuracy=OFF, hourglassControl=DEFAULT, distortionControl=DEFAULT), ElemType(
    elemCode=CPS3, elemLibrary=STANDARD)), regions=(
    mdb.models['Model-1'].parts['Outter'].faces.getSequenceFromMask((['#1 ', ]), ),
    mdb.models['Model-1'].parts['Inner'].faces.getSequenceFromMask((['#1'], ), ),
)

#Seed parts
#cohesive
mdb.models['Model-1'].parts['CZ'].seedEdgeByBias(end1Edges=
    mdb.models['Model-1'].parts['CZ'].edges.getSequenceFromMask(('[#4 ]', ), ),
    end2Edges=mdb.models['Model-1'].parts['CZ'].edges.getSequenceFromMask((
        '[#1 ]', ), ), number=800, ratio=2.0)

mdb.models['Model-1'].parts['CZ'].seedEdgeByNumber(edges=
    mdb.models['Model-1'].parts['CZ'].edges.getSequenceFromMask((['#a ', ]), ),
    number=1)

#coating
mdb.models['Model-1'].parts['Coating'].seedEdgeByBias(end1Edges=
    mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask((['#4 '], ), ),
    end2Edges=mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask((
        '[#1 ]', ), ), number=400, ratio=2.0)

mdb.models['Model-1'].parts['Coating'].seedEdgeByNumber(edges=
    mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask((['#a '], ), ),
    number=15)

#plate
#along height
mdb.models['Model-1'].parts['Plate'].seedEdgeByBias(end1Edges=
    mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask((['#2'], ), ),
    end2Edges=mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask((
        '[#8 ]', ), ), number=25, ratio=1.0)

mdb.models['Model-1'].parts['Plate'].seedEdgeByBias(end1Edges=
    mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask((['#4'], ), ),
    end2Edges=mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask((
        '[#1 ]', ), ), number=400, ratio=2.0)

#outer
mdb.models['Model-1'].parts['Outter'].seedPart(deviationFactor=0.1, size=r1/15)

#inner
mdb.models['Model-1'].parts['Inner'].seedPart(deviationFactor=0.1, size=r2/15)

#mesh
mdb.models['Model-1'].parts['CZ'].generateMesh()

mdb.models['Model-1'].parts['Coating'].generateMesh()

mdb.models['Model-1'].parts['Plate'].generateMesh()

mdb.models['Model-1'].parts['Outter'].generateMesh()

mdb.models['Model-1'].parts['Inner'].generateMesh()

#collapse cohesive zone
mdb.meshEditOptions.setValues(enableUndo=True, maxUndoCacheElements=0.5)

mdb.models['Model-1'].parts['CZ'].editNode(coordinate2=0.0, nodes=
    mdb.models['Model-1'].parts['CZ'].nodes.getSequenceFromMask(mask=(' 
        [ffffffff:50 #3 ]', ), )
)

#surfaces
mdb.models['Model-1'].parts['Coating'].Surface(name='CoatBot', side1Edges=
    mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask((['#1'], ), ))

mdb.models['Model-1'].parts['Plate'].Surface(name='PlateTop', side1Edges=
    mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask(('[ #1 ]', ), ))

mdb.models['Model-1'].parts['CZ'].Surface(name='CZTop', side1Edges=
    mdb.models['Model-1'].parts['CZ'].edges.getSequenceFromMask((['#4'], ), ))

mdb.models['Model-1'].parts['CZ'].Surface(name='CZBot', side1Edges=
    mdb.models['Model-1'].parts['CZ'].edges.getSequenceFromMask((['#1'], ), ))

mdb.models['Model-1'].parts['Coating'].Surface(name='CoatSurf', side1Edges=
    mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask((['#4'], ), ))

mdb.models['Model-1'].rootAssembly.Surface(name='CoupSurf', side1Edges=
mdb.models['Model-1'].rootAssembly.instances['Plate-1'].edges.getSequenceFromMask(mask=('', '#8 '), )
mdb.models['Model-1'].rootAssembly.instances['Coating-1'].edges.getSequenceFromMask(mask=('', '#2 '), )

mdb.models['Model-1'].parts['Inner'].Surface(name='InnerCont', side1Edges=
    mdb.models['Model-1'].parts['Inner'].edges.getSequenceFromMask('([#2 ]), '), )

mdb.models['Model-1'].parts['Outter'].Surface(name='OuterBC', side1Edges=
    mdb.models['Model-1'].parts['Outter'].edges.getSequenceFromMask('([#1 ]), '), )

mdb.models['Model-1'].parts['Plate'].Surface(name='PlateBot', side1Edges=
    mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask('([#4 ]), '), )

mdb.models['Model-1'].parts['Outter'].nodes.getSequenceFromMask(mask=('[#1 ]), ')

mdb.models['Model-1'].parts['Outter'].Set(name='LoadPoint', nodes=
    mdb.models['Model-1'].parts['Outter'].nodes.getSequenceFromMask(mask=('[#1 ]), '), )

mdb.models['Model-1'].ContactProperty('InterfaceContact')

mdb.models['Model-1'].interactionProperties['InterfaceContact'].TangentialBehavior(  
    formulation=FRICTIONLESS)

mdb.models['Model-1'].interactionProperties['InterfaceContact'].NormalBehavior(  
    allowSeparation=ON, constraintEnforcementMethod=DEFAULT,
    pressureOverclosure=HARD)

mdb.models['Model-1'].ContactProperty('TopLoadCont')

mdb.models['Model-1'].interactionProperties['TopLoadCont'].TangentialBehavior(  
    formulation=FRICTIONLESS)

mdb.models['Model-1'].interactionProperties['TopLoadCont'].NormalBehavior(  
    allowSeparation=ON, constraintEnforcementMethod=DEFAULT,
    pressureOverclosure=HARD)

mdb.models['Model-1'].ContactProperty('BotLoadCont')

mdb.models['Model-1'].interactionProperties['BotLoadCont'].TangentialBehavior(  
    formulation=FRICTIONLESS)

mdb.models['Model-1'].interactionProperties['BotLoadCont'].NormalBehavior(  
    allowSeparation=ON, constraintEnforcementMethod=DEFAULT,
    pressureOverclosure=HARD)

mdb.models['Model-1'].SurfaceToSurfaceContactStd(adjustMethod=NONE,  
    clearanceRegion=None, createStepName='Initial', datumAxis=None,  
    enforcement=SURFACE_TO_SURFACE, initialClearance=OMIT, interactionProperty=  
    'InterfaceContact', master=
    mdb.models['Model-1'].rootAssembly.instances['Plate-1'].surfaces['PlateTop']  
    , name='Int-1', slave=
    mdb.models['Model-1'].rootAssembly.instances['Coating-1'].surfaces['CoatBot']  
    , sliding=FINITE, surfaceSmoothing=NONE, thickness=ON)

mdb.models['Model-1'].SurfaceToSurfaceContactStd(adjustMethod=NONE,  
    clearanceRegion=None, createStepName='Initial', datumAxis=None,  
    enforcement=SURFACE_TO_SURFACE, initialClearance=OMIT, interactionProperty=  
    'TopLoadCont', master=  
    mdb.models['Model-1'].rootAssembly.instances['Coating-1'].surfaces['CoatSurf']  
    , name='TopLoader', slave=  
    mdb.models['Model-1'].rootAssemblyinstances['Outter-1'].surfaces['OutterCont']  
    , sliding=FINITE, surfaceSmoothing=NONE, thickness=ON)

mdb.models['Model-1'].SurfaceToSurfaceContactStd(adjustMethod=NONE,  
    clearanceRegion=None, createStepName='Initial', datumAxis=None,  
    enforcement=SURFACE_TO_SURFACE, initialClearance=OMIT, interactionProperty=  
    'BotLoadCont', master=  
    mdb.models['Model-1'].rootAssembly.instances['Plate-1'].surfaces['PlateBot']  
    , name='BotCont', slave=  
    mdb.models['Model-1'].rootAssembly.instances['Inner-1'].surfaces['InnerCont']  
    , sliding=FINITE, smooth=0.2)

#constraints
mdb.models['Model-1'].rootAssembly.regenerate()

mdb.models['Model-1'].Tie(adjust=ON, master=  
    mdb.models['Model-1'].rootAssembly.instances['Coating-1'].surfaces['CoatBot']  
    , name='Top', positionToleranceMethod=COMPUTED, slave=  
    mdb.models['Model-1'].rootAssembly.instances['CZ-1'].surfaces['CZTop'],
    )
#Boundary conditions

mdb.models['Model-1'].DisplacementBC(amplitude=UNSET, createStepName='Initial', distributionType=UNIFORM, fieldName='', localCsys=None, name='Symmetry', region=Region(edges=mdb.models['Model-1'].rootAssembly.instances['Plate-1'].edges.getSequenceFromMask(mask=('[#2 ]', ), ), ul=SET, u2=UNSET, ur3=UNSET))

mdb.models['Model-1'].DisplacementBC(amplitude=UNSET, createStepName='Initial', distributionType=UNIFORM, fieldName='', localCsys=None, name='Inner', region=Region(edges=mdb.models['Model-1'].rootAssembly.instances['Inner-1'].edges.getSequenceFromMask(mask=('[#1 ]', ), ), ul=UNSET, u2=SET, ur3=UNSET))

mdb.models['Model-1'].DisplacementBC(amplitude=UNSET, createStepName='Initial', distributionType=UNIFORM, fieldName='', localCsys=None, name='Outter', region=Region(edges=mdb.models['Model-1'].rootAssembly.instances['Outter-1'].edges.getSequenceFromMask(mask=('[#1 ]', ), ), ul=OFF, u2=ON, ur3=ON))

mdb.models['Model-1'].boundaryConditions['Outter'].setValues(u1=OFF, u2=ON, ur3=ON)

#load step

mdb.models['Model-1'].ImplicitDynamicsStep(description='Load Step', name='Load', nlgeom=ON, previous='Initial')

mdb.models['Model-1'].steps['Load'].setValues(initialInc=1e-13, maxNumInc=4000, minInc=1e-15)

mdb.models['Model-1'].steps['Load'].setValues(alpha=DEFAULT, amplitude=RAMP, application=QUASI_STATIC, initialConditions=OFF, nohaf=OFF)

mdb.models['Model-1'].steps['Load'].setValues(alpha=DEFAULT, amplitude=STEP, application=MODERATE_DISSIPATION, nohaf=OFF)

#couple nodes on top of load point

mdb.models['Model-1'].Coupling(controlPoint=Region(vertices=mdb.models['Model-1'].rootAssembly.instances['Outter-1'].vertices.getSequenceFromMask(mask=('[#2 ]', ), ), couplingType=KINEMATIC, influenceRadius=WHOLE_SURFACE, localCsys=None, name='LoadConst', surface=Region(side1Edges=mdb.models['Model-1'].rootAssembly.instances['Outter-1'].edges.getSequenceFromMask(mask=('[#1 ]', ), ), ul=OFF, u2=ON, ur3=ON))

#apply load

mdb.models['Model-1'].TabularAmplitude(data=((0.0, 0.0), (1.0, 1.0)), name='Amp-1', smooth=SOLVER_DEFAULT, timeSpan=STEP)

mdb.models['Model-1'].DisplacementBC(amplitude=UNSET, createStepName='Load', distributionType=UNIFORM, fieldName='', fixed=OFF, localCsys=None, name='Load', region=Region(edges=mdb.models['Model-1'].rootAssembly.instances['Outter-1'].edges.getSequenceFromMask(mask=('[#1 ]', ), ), ul=UNSET, u2=displacement, ur3=UNSET))

mdb.models['Model-1'].boundaryConditions['Load'].setValues(amplitude='Amp-1')

#include output

mdb.models['Model-1'].parts['CZ'].Set(elements=mdb.models['Model-1'].parts['CZ'].elements.getSequenceFromMask(mask=('[#ffffffff:25 ]', ), ), name='CZ elements')

mdb.models['Model-1'].rootAssembly.regenerate()

mdb.models['Model-1'].FieldOutputRequest(createStepName='Load', name='F-Output-2', rebar=EXCLUDE, region=mdb.models['Model-1'].rootAssembly.instances['CZ-1'].sets['CZ elements'], sectionPoints=DEFAULT, variables=('S', 'SDEG', 'CFAILURE', 'LE', 'U', 'RF', 'CF', 'CSTRESS', 'CDISP', 'STATUS'))

#include reaction force in history output

mdb.models['Model-1'].historyOutputRequests['H-Output-1'].setValues(region=mdb.models['Model-1'].rootAssembly.instances['Outter-1'].sets['LoadPoint'], rebar=EXCLUDE, variables=('RF2', 'ALLAE', 'ALLCD', 'ALLMD', 'ALLME', 'ALLF', 'ALLIE', 'ALLJ', 'ALLKE', 'ALLKL', 'ALLPD', 'ALLQ', 'ALLSE', 'ALLSD', 'ALLVD', 'ALLWK', 'ETOTAL'))

#name job
mdb.Job(atTime=None, contactPrint=OFF, description='', echoPrint=OFF, explicitPrecision=DOUBLE, getMemoryFromAnalysis=True, historyPrint=OFF, memory=90, memoryUnits=PERCENTAGE, model='Model-1', modelPrint=OFF, multiprocessingMode=DEFAULT, name='4ptbend', nodalOutputPrecision=FULL, numCpus=1, numDomains=1, parallelizationMethodExplicit=DOMAIN, queue=None, scratch='', type=ANALYSIS, userSubroutine='', waitHours=0, waitMinutes=0)
D. ABAQUS ALUMINUM COLD-SPRAYED ONTO ALUMINUM INDENTATION TEST SIMULATION

Simulation of the indentation of a ball indenter into a coated specimen to simulate indentation-induced interfacial crack growth. Axisymmetric model with cohesive zone elements at interface. Used for verification of method.

To include residual stresses insert the command: *INITIAL CONDITIONS, type=STRESS, USER right before the first *STEP command in the input file. When submitting file, run with command abaqus job=jobname user=sigini interactive.

```python
from part import *
from material import *
from section import *
from assembly import *
from step import *
from interaction import *
from load import *
from mesh import *
from job import *
from sketch import *
from visualization import *
from connectorBehavior import *

# Geometry Parameters
# Disk radius
r=0.006*1.75
# Disk height
h=0.00489#14e-5
# Coating thickness
coat=0.001
# Cohesive zone thickness
czt=7e-05#7e-7
# Define precrack
precrack=0.00025
# Indenter rad
rad=0.001#0.002 0.0024#
disp=.54e-3 #0.001524/7
disp=.67e-3 #0.001524/7
# disp=0.000

# Critical energy release rates
GIC=0#GIC=0.5*SigmaMax*SigmaMax/(5.7e15)#1325
GIIc=GIC
# Define material stiffness
stiff=7.5e+15
opening=0

# Mesh parameters
xrat=10
yrat=10
xnum=25
ynum=20
```
coatthick=3

# Define material properties
# for coating
mdb.models['Model-1'].Material(name='Top')
mdb.models['Model-1'].materials['Top'].Density(table=((2810.0, ), ))
mdb.models['Model-1'].materials['Top'].Elastic(table=((20e9, 0.33), ))
mdb.models['Model-1'].materials['Top'].Plastic(table=((300e6, 0.0), (629e6, 0.30)))
mdb.models['Model-1'].materials['Top'].Conductivity(table=((55.13, 0.0), (51.62, 900.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Top'].SpecificHeat(table=((0.1338, 0.0), (0.131, 900.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Top'].Expansion(table=((6.3e06, ), ))

# for substrate
mdb.models['Model-1'].Material(name='Bottom')
mdb.models['Model-1'].materials['Bottom'].Density(table=((2810.0, ), ))
mdb.models['Model-1'].materials['Bottom'].Elastic(table=((72e9, 0.33), ))
mdb.models['Model-1'].materials['Bottom'].Plastic(table=((462e6, 0.0), (524e6, 0.50)))
mdb.models['Model-1'].materials['Bottom'].Expansion(table=((1.35e-05, ), ))
mdb.models['Model-1'].materials['Bottom'].Conductivity(table=((40.69, 0.0), (31.69, 900.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Bottom'].SpecificHeat(table=((755.84, 0.0), (755.84, 900.0)), temperatureDependency=ON)

# for cohesive zone
mdb.models['Model-1'].Material(name='Ind')
mdb.models['Model-1'].materials['Ind'].Density(table=((16654.0, ), ))
mdb.models['Model-1'].materials['Ind'].Elastic(table=((1000000000000.0, 0.3, 0.0), (1670000000000.0, 0.3, 973.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Ind'].Conductivity(table=((55.13, 0.0), (51.62, 900.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Ind'].SpecificHeat(table=((0.1338, 0.0), (0.131, 900.0)), temperatureDependency=ON)
mdb.models['Model-1'].materials['Ind'].Expansion(table=((6.3e-06, ), ))

mdb.models['Model-1'].Material(name='Coh')
mdb.models['Model-1'].materials['Coh'].Density(table=((7850, ), ))
mdb.models['Model-1'].materials['Coh'].Elastic(table=((stiff, stiff, stiff), ), type=TRACTION)
mdb.models['Model-1'].materials['Coh'].QuadsDamageInitiation(table=((SigmaMax, TauMax, TauMax), ))
mdb.models['Model-1'].materials['Coh'].quadsDamageInitiation.DamageEvolution(table=(opening, ), ), type=DISPLACEMENT)

# Define geometry
# Define Steel Part
mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.1)
mdb.models['Model-1'].sketches['__profile__'].sketchOptions.setValues(decimalPlaces=3)
mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -0.05), point2=(0.0, 0.05))
mdb.models['Model-1'].sketches['__profile__'].rectangle(point1=(0.0, 0), point2=(r, (-h)))
mdb.models['Model-1'].Part(dimensionality=AXISYMMETRIC, name='Plate', type=DEFORMABLE_BODY)
del mdb.models['Model-1'].sketches['__profile__']

mdb.models['Model-1'].parts['Plate'].BaseShell(sketch=mdb.models['Model-1'].sketches['__profile__'])

del mdb.models['Model-1'].sketches['__profile__']

mdb.models['Model-1'].parts['Plate'].BaseShell(sketch=mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.1)

mdb.models['Model-1'].sketches['__profile__'].sketchOptions.setValues(
decimalPlaces=3, viewStyle=AXISYM)

mdb.models['Model-1'].parts['Plate'].BaseShell(sketch=mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

# Define cohesive zone rectangle

mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.1)

mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -0.05), point2=(0.0, 0.05))

mdb.models['Model-1'].parts['Coating'].BaseShell(sketch=mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.1)

mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -czt/2), point2=(0.0, czt/2))

mdb.models['Model-1'].parts['CZ'].BaseShell(sketch=mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.02)

mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -0.01), point2=(0.0, 0.01))

mdb.models['Model-1'].parts['CZ'].BaseShell(sketch=mdb.models['Model-1'].sketches['__profile__'])
del mdb.models['Model-1'].sketches['__profile__']

# indenter

mdb.models['Model-1'].ConstrainedSketch(name='__profile__', sheetSize=0.02)

mdb.models['Model-1'].sketches['__profile__'].ConstructionLine(point1=(0.0, -0.01), point2=(0.0, 0.01))

mdb.models['Model-1'].parts['Indenter'].ReferencePoint(point=(0.0, rad+coat, 0.0))

mdb.models['Model-1'].rootAssembly.DatumCsysByThreePoints(coordSysType=ANALYTIC_RIGID_SURFACE)
 CYLINDRICAL, origin=(0.0, 0.0, 0.0), point1=(1.0, 0.0, 0.0), point2=(0.0, 0.0, -1.0))

mdb.models['Model-1'].rootAssembly.Instance(dependent=ON, name='Indenter-1',
part= mdb.models['Model-1'].parts['Indenter'])

mdb.models['Model-1'].RigidBody(name='IndConstr', refPointRegion=Region(
referencePoints=(
    mdb.models['Model-1'].rootAssembly.instances['Indenter-1'].referencePoints[2],
)), surfaceRegion=Region(
    side2Edges=mdb.models['Model-1'].rootAssembly.instances['Indenter-1'].edges.getSequenceFromMask(
        mask=('[#1 ]', ), )))

# define sections
mdb.models['Model-1'].CohesiveSection(initialThickness=1.0,
    initialThicknessType=SPECIFY, material='Coh', name='CohSect',
    outOfPlaneThickness=None, response=TRACTION_SEPARATION)

mdb.models['Model-1'].HomogeneousSolidSection(material='Top', name='TopSect',
    thickness=None)

mdb.models['Model-1'].HomogeneousSolidSection(material='Bottom', name='BotSect',
    thickness=None)

# Assign sections
mdb.models['Model-1'].parts['CZ'].SectionAssignment(offset=0.0, offsetField='',
    offsetType=MIDDLE_SURFACE, region=Region(
        faces=mdb.models['Model-1'].parts['CZ'].faces.getSequenceFromMask(mask=(
            '#1 ]', ), ), ), sectionName='CohSect')

mdb.models['Model-1'].parts['Coating'].SectionAssignment(offset=0.0,
    offsetField='', offsetType=MIDDLE_SURFACE, region=Region(
        faces=mdb.models['Model-1'].parts['Coating'].faces.getSequenceFromMask(
            mask=('[#1 ]', ), ), ), sectionName='TopSect')

mdb.models['Model-1'].parts['Plate'].SectionAssignment(offset=0.0,
    offsetField='', offsetType=MIDDLE_SURFACE, region=Region(
        faces=mdb.models['Model-1'].parts['Plate'].faces.getSequenceFromMask(mask=(
            '#1 ]', ), ), ), sectionName='BotSect')

# create instances
mdb.models['Model-1'].rootAssembly.DatumCsysByThreePoints(coordSysType=
    CYLINDRICAL, origin=(0.0, 0.0, 0.0), point1=(1.0, 0.0, 0.0), point2=(0.0,
    0.0, -1.0))

mdb.models['Model-1'].rootAssembly.Instance(dependent=ON, name='CZ-1', part=
    mdb.models['Model-1'].parts['CZ'])

mdb.models['Model-1'].rootAssembly.Instance(dependent=ON, name='Coating-1',
    part= mdb.models['Model-1'].parts['Coating'])

mdb.models['Model-1'].rootAssembly.Instance(dependent=ON, name='Plate-1',
    part= mdb.models['Model-1'].parts['Plate'])

# mesh cohesive
mdb.models['Model-1'].parts['CZ'].setMeshControls(elemShape=QUAD, regions=
    mdb.models['Model-1'].parts['CZ'].faces.getSequenceFromMask((['#1 ]', ], ),
),
    technique=SWEEEP)

mdb.models['Model-1'].parts['CZ'].seedEdgeByBias(end1Edges=
    mdb.models['Model-1'].parts['CZ'].edges.getSequenceFromMask((['#4 ]', ], ),
),
    end2Edges=mdb.models['Model-1'].parts['CZ'].edges.getSequenceFromMask((
        ['#1 ]', ), ), number=650, ratio=1.0)

mdb.models['Model-1'].parts['CZ'].seedEdgeByNumber(edges=
    mdb.models['Model-1'].parts['CZ'].edges.getSequenceFromMask((['#a ]', ], ),
),
#mesh coating
mdb.models['Model-1'].parts['Coating'].seedEdgeByBias(biasMethod=SINGLE, constraint=FINER, end1Edges=
    mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(('[#2 ]', '),
    end2Edges=
    mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(('[#8 ]', '),
    number=13, ratio=1.0)
mdb.models['Model-1'].parts['Coating'].seedEdgeByBias(end1Edges=
    mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(('[#4 ]', '),
    end2Edges=
    mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(('[#1 ]', '),
    number=200, ratio=3.0)
mdb.models['Model-1'].parts['Coating'].setMeshControls(elemShape=QUAD, regions=
    mdb.models['Model-1'].parts['Coating'].faces.getSequenceFromMask(('[#1 ]', '),
    technique=STRUCTURED)

#mesh plate
# in radial direction
mdb.models['Model-1'].parts['Plate'].seedEdgeByBias(end1Edges=
    mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask(('[#4 ]', '),
    end2Edges=
    mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask(('[#1 ]', '),
    number=200, ratio=3.0)
# through thickness
mdb.models['Model-1'].parts['Plate'].seedEdgeByBias(end1Edges=
    mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask(('[#2 ]', '),
    end2Edges=
    mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask(('[#8 ]', '),
    number=45, ratio=7.0)
mdb.models['Model-1'].parts['Plate'].setMeshControls(elemShape=QUAD, regions=
    mdb.models['Model-1'].parts['Plate'].faces.getSequenceFromMask(('[#1 ]', '),
    technique=STRUCTURED)

# assign element types
mdb.models['Model-1'].parts['CZ'].setElementType(elemTypes=(ElemType(
    elemCode=COHAX4, elemLibrary=STANDARD, elemDeletion=ON, viscosity=0.05),
    ElemType(elemCode=UNKNOWN_TRI, elemLibrary=STANDARD)), regions=
    mdb.models['Model-1'].parts['CZ'].faces.getSequenceFromMask(('[#1 ]', '),
    )
mdb.models['Model-1'].parts['Coating'].setElementType(elemTypes=(ElemType(
    elemCode=CAX4RT, elemLibrary=STANDARD, secondOrderAccuracy=OFF, hourglassControl=ENHANCED, distortionControl=DEFAULT),
    ElemType(elemCode=CAX3T, elemLibrary=STANDARD)), regions=
    mdb.models['Model-1'].parts['Coating'].faces.getSequenceFromMask(('[#1 ]', '),
    )
mdb.models['Model-1'].parts['Plate'].setElementType(elemTypes=(ElemType(
elemCode=CAX4RT, elemLibrary=STANDARD), ElemType(elemCode=CAX3T, elemLibrary=STANDARD, secondOrderAccuracy=OFF, hourglassControl=ENHANCED, distortionControl=DEFAULT)), regions=(
    mdb.models['Model-1'].parts['Plate'].faces.getSequenceFromMask(("[#1]", ), ),
)

#generate meshes
mdb.models['Model-1'].parts['CZ'].generateMesh()
mdb.models['Model-1'].parts['Coating'].generateMesh()
mdb.models['Model-1'].parts['Plate'].generateMesh()

#collapse cohesive zone
mdb.meshEditOptions.setValues(enableUndo=True, maxUndoCacheElements=0.5)
mdb.models['Model-1'].parts['CZ'].editNode(coordinate2=0.0, nodes=
    mdb.models['Model-1'].parts['CZ'].nodes.getSequenceFromMask(mask=(
        '[ffffffff:40 #3ffffff ]', ), ),
)

#create surfaces
mdb.models['Model-1'].parts['CZ'].Surface(face3Elements=
    mdb.models['Model-1'].parts['CZ'].elements.getSequenceFromMask(mask=(
        '[ffffffff:20 #3ff ]', ), ), name='CZTop'
)
mdb.models['Model-1'].parts['CZ'].Surface(face1Elements=
    mdb.models['Model-1'].parts['CZ'].elements.getSequenceFromMask(mask=(
        '[ffffffff:20 #3ff ]', ), ), name='CZBot'
)

mdb.models['Model-1'].parts['Coating'].Surface(name='CoatSurf', side1Edges=
    mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(("[#4]", ), ),
)

mdb.models['Model-1'].parts['Coating'].Surface(name='CoatBot', side1Edges=
    mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(("[#1]", ), ),
)

mdb.models['Model-1'].parts['Coating'].Surface(name='FluxReg', side1Edges=
    mdb.models['Model-1'].parts['Coating'].edges.getSequenceFromMask(("[#4]", ), ),
)

mdb.models['Model-1'].parts['Plate'].Surface(name='PlateTop', side1Edges=
    mdb.models['Model-1'].parts['Plate'].edges.getSequenceFromMask(("[#1]", ), ),
)

mdb.models['Model-1'].parts['Plate'].Set(name='PlateBot', nodes=
    mdb.models['Model-1'].parts['Plate'].nodes.getSequenceFromMask(("[#0:282 #ffe0000 #ffffffff:5 #3ffffff ]", ), ),
)

mdb.models['Model-1'].parts['Plate'].Set(name='PlateSym', nodes=
    mdb.models['Model-1'].parts['Plate'].nodes.getSequenceFromMask(("[#0:6 #100 #0:5 #20000 #0:5 #40000000 #0:6]", 
        '#8 #0:5 #1000 #0:5 #2000000 #0:5 #4000000000', 
        '#0:6 #80 #0:5 #10000 #0:5 #20000000 #0:6', 
        '#4 #0:5 #800 #0:5 #100000 #0:5 #2000000000', 
        '#0:6 #40 #0:5 #8000 #0:5 #1000000 #0:6', 
        '#2 #0:5 #400 #0:5 #80000 #0:5 #100000000', 
        '#0:6 #20 #0:5 #4000 #0:5 #800000 #0:6', 
        '#1 #0:5 #200 #0:5 #40000 #0:5 #80000000', 
        '#0:6 #10 #0:5 #2000 #0:5 #400000 #0:5', 
        '#80000000 #0:6 #100 #0:5 #20000000 #0:5', 
        '#0:6 #8 #0:5 #1000 #0:5 #200000 #0:5', 
        '#40000000 #0:6 #80 #0:5 #100000 #0:5 #20000000', 
        '#0:6 #4 #0:5 #800 #0:5 #1000000 #0:5', ' #2000000000 ), ),
)

mdb.models['Model-1'].parts['CZ'].Set(elements=
mdb.models['Model-1'].parts['CZ'].elements.getSequenceFromMask(mask=('[ffffffff:20 #3ff]', ), ), name='CZElements')

mdb.models['Model-1'].rootAssembly.regenerate()

mdb.models['Model-1'].parts['Coating'].Set(name='CoatSym', nodes=mdb.models['Model-1'].parts['Coating'].nodes.getSequenceFromMask(mask=('[#1 0:5 #200 0:5 #40000 0:5 #8000000', '
  #0:6 #10 0:5 #2000 0:5 #4000000 #0:5',
  #80000000 #0:6 #100 0:5 #20000 0:5 #40000000',
  #0:6 #8 0:5 #1000 0:5 #200000 ]', ), ))

mdb.models['Model-1'].rootAssembly.regenerate()

mdb.models['Model-1'].rootAssembly.Set(name='Outside', nodes=
  mdb.models['Model-1'].rootAssembly.instances['Coating-1'].nodes.getSequenceFromMask(
    mask=('[#0:12 #20000 #0:5 #40000000 #0:6 #8 0:5',
    ' #1000 0:5 #2000000 0:5 #4000000 #0:6 #80',
    ' #0:5 #10000 #0:5 #200000 #0:6 #4 0:5', ' #800 0:5 #100000 ]', ), )+
  mdb.models['Model-1'].rootAssembly.instances['Plate-1'].nodes.getSequenceFromMask(
    mask=('[#0:6 #200 0:5 #40000 0:5 #8000000 #0:6',
    ' #10 0:5 #20000 0:5 #4000000 #0:5 #80000000',
    ' #0:6 #100 0:5 #200000 0:5 #4000000 #0:6',
    ' #8 0:5 #1000 0:5 #200000 0:5 #40000000',
    ' #0:6 #80 0:5 #10000 0:5 #2000000 0:6',
    ' #4 0:5 #800 0:5 #100000 0:5 #20000000',
    ' #0:6 #40 0:5 #8000 0:5 #1000000 0:6',
    ' #2 0:5 #400 0:5 #80000 0:5 #10000000',
    ' #0:6 #20 0:5 #4000 0:5 #800000 0:6',
    ' #1 0:5 #200 0:5 #40000 0:5 #8000000',
    ' #0:6 #10 0:5 #2000 0:5 #4000000 #0:5',
    ' #80000000 #0:6 #100 0:5 #20000000 #0:5 #40000000',
    ' #0:6 #8 0:5 #1000 0:5 #2000000 ', ), ))

mdb.models['Model-1'].rootAssembly.Set(name='Corner', nodes=
  mdb.models['Model-1'].rootAssembly.instances['Coating-1'].nodes.getSequenceFromMask(
    mask=('[#0:87 #20000000 ]', ), ))

mdb.models['Model-1'].parts['Indenter'].Set(name='IndLoad', referencePoints=(
    mdb.models['Model-1'].parts['Indenter'].referencePoints[2], ))

#define contact interactions
mdb.models['Model-1'].ContactProperty('InterfaceContact')

mdb.models['Model-1'].interactionProperties['InterfaceContact'].TangentialBehavior(  formulation=FRICTIONLESS)

mdb.models['Model-1'].interactionProperties['InterfaceContact'].NormalBehavior(
    allowSeparation=ON, constraintEnforcementMethod=DEFAULT,  
    pressureOverclosure=HARD)

mdb.models['Model-1'].interactionProperties['InterfaceContact'].tangentialBehavior.setValues(  dependencies=0, directionality=ISOTROPIC, elasticSlipStiffness=None,  
    formulation=PENALTY, fraction=0.005, maximumElasticSlip=FRACTION, 
    pressureDependency=OFF, shearStressLimit=None, slipRateDependency=OFF,  
    table=((0.3, ), ), temperatureDependency=OFF)

mdb.models['Model-1'].interactionProperties['InterfaceContact'].normalBehavior.setValues(  allowSeparation=ON, clearanceAtZeroContactPressure=0.0,  
    constraintEnforcementMethod=PENALTY, contactStiffness=DEFAULT,  
    contactStiffnessScaleFactor=1.0, pressureOverclosure=HARD, 
    stiffnessBehavior=LINEAR)

mdb.models['Model-1'].interactionProperties['InterfaceContact'].tangentialBehavior.setValues(  ...
dependencies=0, directionality=ISOTROPIC, elasticSlipStiffness=None,
formulation=PENALTY, fraction=0.005, maximumElasticSlip=FRACTION,
pressureDependency=OFF, shearStressLimit=None, slipRateDependency=OFF,
table=((0.6, ), ), temperatureDependency=OFF)

mdb.models['Model-1'].SurfaceToSurfaceContactStd(adjustMethod=NONE,
clearanceRegion=None, createStepName='Initial', datumAxis=None,
enforcement=SURFACE_TO_SURFACE, initialClearance=OMIT,
interactionProperty= 'InterfaceContact', master= 
  mdb.models['Model-1'].rootAssembly.instances['Plate-1'].surfaces['PlateTop'],
  name='Int-1', slave= 
  mdb.models['Model-1'].rootAssembly.instances['Coating-1'].surfaces['CoatBot'],
  sliding=FINITE, surfaceSmoothing=NONE, thickness=ON)

mdb.models['Model-1'].Tie(adjust=ON, master= 
  mdb.models['Model-1'].rootAssembly.instances['Coating-1'].surfaces['CoatBot'],
  name='Top', positionToleranceMethod=COMPUTED, slave= 
  mdb.models['Model-1'].rootAssembly.instances['CZ-1'].surfaces['CZTop'],
  thickness=ON, tieRotations=ON)

mdb.models['Model-1'].Tie(adjust=ON, master= 
  mdb.models['Model-1'].rootAssembly.instances['Plate-1'].surfaces['PlateTop'],
  name='Bottom', positionToleranceMethod=COMPUTED, slave= 
  mdb.models['Model-1'].rootAssembly.instances['CZ-1'].surfaces['CZBot'],
  thickness=ON, tieRotations=ON)

mdb.models['Model-1'].SurfaceToSurfaceContactStd(adjustMethod=NONE,
clearanceRegion=None, createStepName='Initial', datumAxis=None,
initialClearance=OMIT, interactionProperty='InterfaceContact', master= 
  Region(
    side2Edges=mdb.models['Model-1'].rootAssembly.instances['Indenter-1'].edges.getSequenceFromMask(
      mask=('[#1 ]', ), ), ), name='IndenterCont', slave= 
  mdb.models['Model-1'].rootAssembly.instances['Coating-1'].surfaces['CoatSurf'],
  sliding=FINITE, thickness=ON)

#bcs

mdb.models['Model-1'].DisplacementBC(amplitude=UNSET,
createStepName='Initial',
distributionType=UNIFORM, fieldName='', localCsys=None, name='SymCoat',
region= 
  mdb.models['Model-1'].rootAssembly.instances['Coating-1'].sets['CoatSym'],
  u1=SET, u2=UNSET, ur3=UNSET)

mdb.models['Model-1'].DisplacementBC(amplitude=UNSET,
createStepName='Initial',
distributionType=UNIFORM, fieldName='', localCsys=None, name='SymPlate',
region= 
  mdb.models['Model-1'].rootAssembly.instances['Plate-1'].sets['PlateSym'],
  u1=SET, u2=UNSET, ur3=UNSET)

mdb.models['Model-1'].DisplacementBC(amplitude=UNSET,
createStepName='Initial',
distributionType=UNIFORM, fieldName='', localCsys=None, name='PlateBot',
region= 
  mdb.models['Model-1'].rootAssembly.instances['Plate-1'].sets['PlateBot'],
  u1=UNSET, u2=SET, ur3=UNSET)
mdb.models['Model-1'].StaticStep(initialInc=1e-06, maxNumInc=100000, minInc=1e-21, name='LoadStep', nlgeom=ON, previous='Initial')
mdb.models['Model-1'].StaticStep(initialInc=1e-06, maxNumInc=75000, minInc=1e-21, name='RemoveLoad', previous='LoadStep')
mdb.models['Model-1'].steps['LoadStep'].setValues(adaptiveDampingRatio=0.05, continueDampingFactors=True, stabilizationMethod=DISSIPATED_ENERGY_FRACTION)
mdb.models['Model-1'].steps['RemoveLoad'].setValues(adaptiveDampingRatio=0.05, continueDampingFactors=True, stabilizationMethod=DISSIPATED_ENERGY_FRACTION)

mdb.models['Model-1'].DisplacementBC(amplitude=UNSET, createStepName='LoadStep', distributionType=UNIFORM, fieldName='', fixed=OFF, localCsys=None, name='IndenterLoad', region=mdb.models['Model-1'].rootAssembly.instances['Indenter-1'].sets['IndLoad'], u1=0, u2=-disp, ur3=0)

mdb.models['Model-1'].boundaryConditions['IndenterLoad'].setValuesInStep(stepName='RemoveLoad', u2=0.0)

mdb.models['Model-1'].steps['RemoveLoad'].Restart(frequency=1, numberIntervals=0, overlay=ON, timeMarks=OFF)

mdb.models['Model-1'].fieldOutputRequests['F-Output-1'].setValues(variables=('S', 'PE', 'PEEQ', 'PEMAG', 'LE', 'U', 'RF', 'CF', 'CSTRESS', 'CDISP', 'STATUS'))

mdb.models['Model-1'].FieldOutputRequest(createStepName='LoadStep', name='F-Output-2', rebar=EXCLUDE, region=mdb.models['Model-1'].rootAssembly.instances['CZ-1'].sets['CZElements'], sectionPoints=DEFAULT, variables=('SDEG', 'CFAILURE', 'DMICRT'))

mdb.models['Model-1'].Equation(name='CouplingEqn', terms=((1.0, 'Outside', 1), (-1.0, 'Corner', 1)))

mdb.models['Model-1'].HistoryOutputRequest(createStepName='LoadStep', name='H-Output-2', rebar=EXCLUDE, region=mdb.models['Model-1'].rootAssembly.instances['Indenter-1'].sets['IndLoad'], sectionPoints=DEFAULT, variables=('RF1', 'RF2'))

#define job
mdb.models['Model-1'].rootAssembly.regenerate()

mdb.Job(atTime=None, contactPrint=OFF, description='', echoPrint=OFF, explicitPrecision=DOUBLE, getMemoryFromAnalysis=True, historyPrint=OFF, memory=90, memoryUnits=PERCENTAGE, model='Model-1', modelPrint=OFF, multiprocessingMode=DEFAULT, name='Implicit3', nodalOutputPrecision=FULL, numCpus=1, numDomains=1, parallelizationMethodExplicit=DOMEIN, queue=None, scratch='''', type=ANALYSIS, userSubroutine='', waitHours=0, waitMinutes=0)
E. USER SUB-ROUTINE TO APPLY PRESSURE AS A FUNCTION OF CRACK SIZE IN EXPLICIT THERMAL-STURUCTURAL SIMULATION

C
C User subroutine VDLOAD
subroutine vdload ( 
C Read only -
*     nblock, ndim, stepTime, totalTime, amplitude, 
*     curCoords, velocity, dircos, jltyp, sname, 
C Write only -
*     value )
C
include 'vaba_param.inc'
parameter ( const = -4.d2 )
C
dimension curCoords(nblock,ndim), velocity(nblock,ndim), 
*     dircos(nblock,ndim,ndim), value(nblock)
*
character*80 sname
*
do k = 1, nblock
C
C For interface
C Before crack growth, to only apply pressure in ring delamination
C
C Initial Outer Radius
X=1.44D-3
C
C Initial Inner Radius
Y=3.94D-4
IF (CURCOORDS(k,1).LT.Y) THEN
VALUE(k)=0
END IF
IF (CURCOORDS(k,1).GT.X) THEN
VALUE(k)=0
END IF
IF (CURCOORDS(k,1).LT.X) THEN
IF (CURCOORDS(k,1).GT.Y) THEN
C         Max load multiplied by the amplitude,
C         amplitude=f(t), amplitude<=1
VALUE(k)=3.9D8*amplitude
END IF
END IF
C
C first phase of crack growth
C Define crack radii as functions of time
C
C Time interval for which phase of crack growth is valid
C
C In terms of Total Time (model starts at t=2)
IF (totaltime.GT.2.003393) THEN
IF (totalTime.LT.2.003475) THEN
C Define Inner radius as a function of total time
X=4.6*(totalTime-2.0032)+0.00006
C
C Define Outer Radius as a function of total time
Y=0
IF (CURCOORDS(k,1).LT.Y) THEN
VALUE(k)=0
END IF
IF (CURCOORDS(k,1).GT.X) THEN
VALUE(k)=0
END IF
IF (CURCOORDS(k,1).LT.X) THEN
IF (CURCOORDS(k,1).GT.Y) THEN
C         Max load multiplied by the amplitude,
C         amplitude=f(t), amplitude<=1
VALUE(k)=3.9D8*amplitude
END IF
END IF
C
Max load multiplied by the amplitude,
amplitude=f(t), amplitude=<1
VALUE(k)=3.9D8*amplitude
END IF
END IF
END IF
C second phase of crack growth
C Define crack radii as functions of time
C Time interval for which phase of crack growth is valid
C In terms of Total Time (model starts at t=2)
IF (totaltime.GT.2.003475) THEN
IF (totalTime.LT.2.003614) THEN
C OUTER RADIUS
X=2.9104*(totalTime-2.0032)+0.0011
C INNER RADIUS
Y=0
IF (CURCOORDS(k,1).LT.Y) THEN
VALUE(k)=0
END IF
IF (CURCOORDS(k,1).GT.X) THEN
VALUE(k)=0
END IF
IF (CURCOORDS(k,1).LT.X) THEN
IF (CURCOORDS(k,1).GT.Y) THEN
C Max load multiplied by the amplitude,
amplitude=f(t), amplitude=<1
VALUE(k)=3.9D8*amplitude
END IF
END IF
END IF
C After crack growth completion
C Time interval for which phase of crack growth is valid
C In terms of Total Time (model starts at t=2)
IF (totalTime.GT.2.003614) THEN
C OUTER RADIUS
X=2.32D-3
C INNER RADIUS
Y=0
IF (CURCOORDS(k,1).LT.Y) THEN
VALUE(k)=0
END IF
IF (CURCOORDS(k,1).GT.X) THEN
VALUE(k)=0
END IF
IF (CURCOORDS(k,1).LT.X) THEN
IF (CURCOORDS(k,1).GT.Y) THEN
C Max load multiplied by the amplitude,
amplitude=f(t), amplitude=<1
VALUE(k)=3.9D8*amplitude
END IF
END IF
END IF
end do
end do
*
return
end
F. USER SUB-ROUTINE TO RESIDUAL STRESS IN FOUR-POINT BEND SIMULATION

SUBROUTINE SIGINI(SIGMA,COORDS,NTENS,NCRDS,NOEL,NPT,LAYER,
  1  KSPT,LREBAR,NAMES)
C
INCLUDE 'ABA_PARAM.INC'
C
DIMENSION SIGMA(NTENS), COORDS(NCRDS)
CHARACTER NAMES(2)*80
C
substrate residual stress distribution
IF (COORDS(2).LT.0.0) THEN
  C x-dir substrate residual stress
  C Defined as a function of y (thickness) direction
  SIGMA(1)=(7.34+16737*(COORDS(2)+0.00305/2))*1000000
  C y-dir substrate residual stress
  SIGMA(2)=0
  C z-dir substrate residual stress
  SIGMA(3)=0
END IF
C
coating residual stress distribution
IF (COORDS(2).GT.0.0) THEN
  C x-dir substrate residual stress
  C Defined as a function of y (thickness) direction
  SIGMA(1)=(-13.39+20566*(COORDS(2)-0.00152/2))*1000000
  C y-dir substrate residual stress
  SIGMA(2)=0
  C z-dir substrate residual stress
  SIGMA(3)=0
END IF
C
RETURN
END
APPENDIX C. NON-TECHNICAL ABSTRACT

Coatings are vital to the performance of gun tubes, as they protect the structural steel substrate from both the thermal and chemical affects of the hot, erosive, combustion gases. Current tests to evaluate gun tube coatings are limited to live-firing experiments, and the pulsed-laser heating experiment. Live firing tests are expensive and cumbersome, while laser tests do not include many affects including in firing experiments, such as gas pressure. Recognizing the shortcomings of current evaluation techniques, a method was developed to evaluate interfacial properties. A flaw was first introduced by indenting the coating surface to create a flaw at the interface. The evolution of the flaw can then be studied numerically, easily accounting for effects not including in conventional experiments.

The effects of severe thermal- and pressure-transients on coated substrates with indentation-induced, and blister defects were then analyzed using experimental and finite element methods. Numerical modeling was used to assess the transient thermal- and stress-states and the propensity for fracture related damage and evolution, while undergoing surface loading to simulate actual internal gun tube conditions. Spherical indentations were evaluated based on the size and loading force in which they were introduced. Indentation results were then compared to FEA simulations in order to evaluate the strength and critical fracture energy of the interface. Measured properties were then used to evaluate the growth of an interfacial indentation-induced flaw to various combinations of surface thermal and pressure loading. Results indicated complex interactions between the boundary conditions and their timing and the resulting damage initiation and evolution. Given the need for robust coatings, the experimental and modeling procedures
explored by this study will have important ramifications for coated tube designs and the evaluation of candidate materials.
VITA

Jason Thomas Harris was born in State College, Pennsylvania, the son of Janet Lee and Thomas James Harris on July 30th, 1983. After completing high school at Bellefonte Area High School, Bellefonte, Pennsylvania in 2002, he attended the Pennsylvania State University in University Park, Pennsylvania from 2002 to 2006, receiving a Bachelor of Science degree in Mechanical Engineering, and a Minor in Engineering Mechanics. Under the supervision of Dr. Albert Segall, he pursued his Masters of Science in Engineering Mechanics at the same institution, studying finite element modeling of particle failure in stressed particle beds. In May of 2008, he successfully defended his thesis and graduated, before enrolling in the doctoral program in Engineering Science and Mechanics, again under the guidance of Dr. Albert Segall. In his research, he developed a method to characterize coating interfaces and model the evolution of flaws that are subjected to severe thermal and pressure transients. He will be graduating with Doctor of Philosophy from Pennsylvania State University in August of 2012.