Finite Element Analysis of Bi-Metallic Structures with Adhesive Delamination

THESIS

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Abstract

Bi-metal structures made of aluminum and steel are increasingly used for lightweighting applications. Replacing steel parts with aluminum in the body in white can reduce the weight of a vehicle up to 30%. The coefficient of thermal expansion (CTE) of aluminum is almost twice that of steel. Due to such large CTE mismatch, thermal buckling can become a concern when the bi-metal structure is exposed to elevated temperature. When adhesive is added between the aluminum and steel, its curing process can be affected due to buckling of the dissimilar metals. Moreover, stress in the structure developed at high temperature can be permanently locked in when the adhesive fully cures. This can lead to a higher residual gap between the aluminum and the steel than in structures without adhesive.

The objective of this research is to quantitatively understand the stress and strain evolution in a bi-metallic Al / adhesive / steel structure exposed to a thermal cycle representative of that used in automotive paint bake process, including delamination of adhesive between the substrates. To achieve this objective, it is essential to first capture the behavior of the bi-metallic structure without adhesive and validating such models. Once validated, addition of cured adhesive and its delamination behavior is then incorporated. Delamination behavior relies on the fracture energy release rate of the
adhesive material, which is determined through fracture toughness testing. Specially, the research consists of the following two main tasks.

First, preliminary finite element models have been developed to capture the behavior of thermal induced buckling, including its deflection profile and stress close to the fasteners. These studies revealed that for a maximum paint bake temperature of 180°C residual stress is only found within the fastening region. This indicates that paint bake process itself does not produce enough heat to exceed elastic strain limits of the bulk structure. Several geometric factors are studied, including plate thickness, fastener pitch, and flange height. These factors reveal the effect of the geometry on the maximum deflection in buckling.

Second, adhesive fracture toughness testing is conducted to measure the mode I fracture energy release rate. The fracture toughness is then incorporated in to both lap shear and thermal buckling model. Both use cohesive zone method incorporating linear traction-separation law for modeling the adhesive damage behavior. A hybrid continuum-cohesive element is created to incorporate both delamination effects as well as cured stress locking.

The understanding established in this research is essential to optimize the design of bi-metallic structure to control distortion and residual stress in the structure, two important performance properties. Taken as a whole, the research results reported in this thesis represent a first step toward improving the quantitative understanding of adhesive deformation and failure behaviors in Al-steel bi-metallic structure. Future work includes (1) incorporation of non-linear traction-separation behavior in the cohesive elements, and
(2) testing of adhesive fracture toughness as a function of temperature. Addressing the future work can further improve the accuracy of the computational model.
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Chapter 1: Introduction

The automotive industry is continually maintaining its competition and demand for an increasingly fuel efficient, economic vehicle. Addition of high strength aluminum to car frames is a desirable option for its lighter weight and sufficient mechanical properties to meet industry standards. The addition of high strength aluminum to the, originally, full steel body in white (BIW) provides the potential for a vehicle to reduce its weight up to 34% [1]. Complications arise when joining aluminum to steel in terms of corrosion, and thermal expansion during the paint bake process.

Figure 1- Aluminum (grey) to steel (blue) body in white
Aluminum and steel that make physical contact are subject to galvanic corrosion. Particularly aluminum acts as the anode and steel as the cathode, meaning the aluminum will preferentially corrode. In order to prevent corrosion, an epoxy adhesive can be applied as an intermediate layer, acting as an insulator between the two. This thermosetting polymer is applied in its thick fluid state then undergoes curing through heating at or above the glass transition temperature.

Epoxy adhesives are widely used as high-performance structural adhesives, particularly in automotive and aerospace applications. Epoxy adhesives are used in the automotive industry because its low shrinkage during curing and its low shrinkage upon being cured. Epoxy adhesives are known to have a good affinity for aluminum alloy surfaces and the oxide layers. [2]

During the paint baking process, the epoxy adhesive and e-coat paint are cured at a held temperature of about 180°C. A significant mismatch in the coefficient of thermal expansion (CTE) between aluminum and steel can lead to thermally induced mechanical buckling, or thermo-mechanical buckling. This buckling effect leads to an interaction between aluminum and the weld or bolt, causing local plastic strain to occur. Once cooled, a residual gap remains due to this interaction, which negatively impacts the integrity of the structure.

At 180°C, the adhesive is well above its glass transition temperature and begins to crosslink or cure. The curing occurs while the structure is buckled, thus gaining its mechanical strength without bridging the two metals. At the final stage of the cycle, when
When the temperature returns to 25°C, the adhesive is already cured which locks the residual stress and acts as a barrier for the aluminum to return to its original form. This effect was confirmed by Zhu et al. who assembled flat sheets of SAE 1004 or DP590 with AA6061-T6 using adhesive and either rivets or bolts. They compared the effects of maximum gap between specimen with and without adhesive. It was found that a larger gap occurs in the specimens with adhesive, thus indicating that curing plays a role in the final vertical deflection. [3]

The objective of this research is to quantitatively understand the stress and strain evolution in a bi-metallic Al / adhesive / steel structure exposed to a thermal cycle representative of that used in automotive paint bake process. The overall approach is summarized in the following. First, thermal-mechanical buckling behavior of a bi-metallic structure without adhesive is modeled and validated. Then the adhesive is introduced into the bi-metallic structure which takes into account its damage initiation, damage evolution and failure or delamination. Fracture toughness testing is conducted to obtain the fracture energy release rate, which plays an essential role in adhesive delamination. Specially, the research consists of the following two main tasks.

In Task 1, finite element analysis (FEA) using Abaqus is performed to predict geometry and stresses that occur during thermally induced buckling. Linear buckling analysis is performed to evaluate the overall buckling geometry which is then used in further models to ensure consistent buckling behavior, particularly for simple geometries. A full analysis can then be performed utilizing the paint bake thermal cycle as the
thermal load. Deflection profiles and maximum gap between the aluminum and steel are analyzed for different scenarios and compared to experimental data.

Once an experimentally confirmed model is developed, adhesive, represented as cohesive elements, are added between the aluminum and steel. These cohesive elements are designed to improve computational efficiency, due to the significantly smaller size of the adhesive when compared to the adherends. Two approaches are considered when using cohesive elements. The first is the traction vs separation approach, which is used when the adhesive is considered indiscernibly thin and the elastic-plastic mechanical properties are unknown. Continuum cohesive elements on the other hand are used for known thickness and they also use the same conventional mechanical properties as ductile metals. The accuracy of each approach strongly depends on the thickness of adhesive being model, as well as adhesive material’s ductility.

The most common approach applied for this research is the traction vs separation approach. This modeling method requires parameters for damage evolution and failure, such as: maximum nominal stress fracture energy release rate. Since fracture energy release rate varies significantly depending on the particular formulation of adhesive material, fracture toughness testing of the particular adhesive is performed in Task 2. Tapered double cantilever beams were tensile tested. Using the recorded loads and corresponding crack length, the fracture energy release rate was determined.

The thesis is organized as follows. Chapter 2 contains a literature review of aluminum to steel dissimilar metal joining, solid state welds, epoxy adhesives, and modeling adhesives. The specific objectives of the research are described in Chapter 3.
Chapter 4 includes thermo-mechanical buckling modeling which includes several parametric studies in terms of plate thickness, fastener pitch and geometric imperfections. Chapter 4 then continues into contact interaction between rivet/bolt and plates as well as adhesive delamination. Chapter 5 is dedicated to study of epoxy adhesive, fracture mechanics testing and cohesive element modeling. Finally, summary and conclusions of this research and the future work are provided in Chapters 6 and 7, respectively. The understanding established in this research is essential to optimize the design of bi-metallic structure to control distortion and residual stress in the structure, two important performance properties.
Chapter 2: Literature Review

2.1 Aluminum to Steel Dissimilar Metal Joining

Aluminum and steel have significantly different melting points, different electrical and thermal conductivities as well as different coefficients of thermal expansion.

The largest prevailing issue of aluminum to steel joining is the formation of intermetallic compounds (IMCs). Intermetallic compounds, as defined by Schulze in 1967 are solid phases containing two or more metallic elements, with optionally one or more non-metallic elements, whose crystal structure differs from that of other constituents. [4] Homogenous and heterogeneous solid solutions for metals and interstitial compounds such as carbides and nitrides are excluded under this definition. [5]

In intermetallic compounds, the binding is metallic between the primary configuration and ionic in the secondary configuration leading to the different properties of IMCs compared to alloys. The ionic bond is due to a greater difference in electronegativities between the solute metal and the solvent metal. Because of this partial ionic bonding, the IMCs are always very brittle, having similar properties to that of ceramic
materials. Properties of IMCs include: high strength, good machinability, good creep at high temperatures and high toughness at cryogenic temperatures.

2.1.1 Fusion Welding of Aluminum to Steel

Dating as far back as the 1950s, several attempts have been made by researchers to fusion weld aluminum to steel, only to find them ineffective. [6]

Although it is impractical to fusion weld aluminum and steel, there are other joining methods that reduce the amount of heat transmitted to the part. Typically, this requires the introduction of some type of brazing or solid state welding process.

Brazing can be performed as a gas tungsten arc butt welding-brazing, gas metal arc welding-brazing, and several similar processes. Song et al. joined 5A06 aluminum alloy and AISI 321 stainless steel by TIG welding–brazing with 1100 pure Al, 4043 AlSi5 and 4047 AlSi12 filler metals. All the butt joints fractured at the 5–10 μm thick IMC layers at the Fe–Al interfaces in tensile tests. The butt joint with 4043 AlSi5 filler metal had the largest tensile strength of 125.2 MPa.

In another study, Lu et al. braze-welded the aluminum side and arc-welded the steel side of an airplane component leading to an intermetallic layer of 3-4 μm. Upon tensile testing, the joint strength was higher than 70% of the tensile strength of the aluminum [7].
Borriusthekul et al. used gas tungsten arc welding to join AA 1100 alloy to low carbon steel by welding bead-on-sheet with successful joint penetration in 1 mm thick sheet metal. [8]

2.1.2 Bonded Aluminum/Steel Microstructure

The aluminum-iron phase diagram can be used as a method for preliminary judgment concerning the structures developed during welding of steel/aluminum joints, the properties of aluminum containing iron, and steels containing aluminum.

As seen in the phase diagram (Figure 2), the solubility of iron in solid aluminum is very small; the boundary of the solid solution of iron in aluminum at 225-600 °C is found at an iron content of 0.01-0.022%. [6] Iron will not dissolve in aluminum at room temperature.

Iron in aluminum alloys are always present in the form of IMCs; this determines the workability and mechanical properties of the alloys. Introducing iron will increase the ultimate tensile strength and hardness with the consequential drastic drop in ductility. Iron-aluminum alloys containing 8-10% iron have a low amount of ductility, with those having a higher iron content being extremely brittle.
As stated, a main challenge with any type of aluminum-steel joining is formation of layers of IMCs. The most common IMCs formed between aluminum and steel are FeAl$_3$, Fe$_2$Al$_5$, and Fe$_3$Al [9]. Figure 2 is the aluminum steel binary phase diagram. In a typical weld, the type of intermetallic heavily relies on the fraction of aluminum to steel. Close to the aluminum side of a weld, IMCs formed are heavily composed of aluminum, i.e. FeAl$_3$ (59.18 % Al) and Fe$_2$Al$_5$. Within the region close to steel, this phase diagram would predict IMCs such as FeAl and Fe$_3$Al.

Figure 2 - The Al-Fe phase diagram.
These intermetallic compounds are formed at elevated temperatures. The higher the available thermal energy, the thicker the layer of IMCs has been known to grow between aluminum and steel. An IMC layer of 2μm or greater can lead to a decrease in strength and ductility. [9] The brittleness of these IMCs makes the site susceptible to failure.

2.2 Solid State Welding

Solid state welding involves a group of welding processes that produce a coalescence at temperatures below the melting point of the base metal/filler metal. Adhesion of these surfaces occur by inter-atomic forces. For bonding to occur, surfaces must be brought together approximately 10 angstroms for most metals. [10] The main barriers to solid state welding include asperities, oxides, and surface contamination. Therefore, solid state welding involves removal of these barriers through a combination of pressure, heat, and time.

The advantages of solid state welding include: a weld free from microstructural defects, mechanical properties which are similar to those of the parent metals, no need for filler metals, and the ability to join dissimilar metals. The main disadvantages are that equipment is typically expensive, and welding takes more time compared to fusion welding.
Solid state welding processes can produce significantly thinner intermetallic layers. Because it is a lower temperature process, there is less available thermal energy or IMCs to form. [8] [9]

### 2.2.1 Friction Stir Welding

Friction stir welding is a solid state welding processes which combines two joining workpieces without any melting [11]. Instead of a molten pool of metal, FSW

![Friction stir welding process and welding temperature profile](image)

Figure 3 - Friction stir welding process (above) and welding temperature profile (below)
uses heat and kinetic energy to soften the metals and “stir” them together; analogous to mixing dough [12].

There are three steps in friction stir welding: plunging, dwell time and retraction. Each step is associated with its signature thermal profile as shown in Figure 4. Friction stir welding is commonly applied to aluminum and dissimilar metal welds. This solid-state welding process produces a fine grain structure with increased strength and toughness. This is due to the process of dynamic recrystallization, where there is enough plastic deformation energy for nucleation sites to occur. There exists no weld solidification cracking susceptibility because there is no liquid phase to solidify from. The heat affected zones (HAZs) in friction stir welds are relatively insignificant compared to the HAZs of fusion welding. Moreover, it is a low distortion as well as low shrinkage process without possibility for lack of fusion or change in material composition.

With all the benefits of friction stir welding, there are still limitations with this welding process. Friction stir welding is limited to welding uniform thicknesses compared to other welding processes. Another limitation is, that given an environment with limited space for repairs, friction stir welding is not practical; thus, limiting it to a production tool.

Just as in fusion welding, friction stir welding is also known to form IMC layers, although they are significantly smaller. Using the proper processing parameters, these
layers will remain minimal in solid state welds and not affect overall strength of the joint. It has been found that intermetallic layer thickness is depended on weld offset (in butt joints), plunge depth, rotation speed, weld velocity and dwell time (spot welds). As seen in Figure 4, the weld which was performed closer to the aluminum side had a thicker layer of IMCs; the closer the pin is to the aluminum side the more heat is delivered to the
part before joining occurs. This increase in heat causes more energy to be used in forming IMCs. Also seen is that increasing the rotational speed will boost the overall temperature around the weld and assist in the development of IMCs [13].

A friction stir welder will withdraw its tool at the end of a weld. Once the tool is removed, a hole where the tool was previously located remains. This poses a challenge, because for many applications, the hole must either be filled up or removed from the final part. This adds an additional processing step compared to fusion welding. Liu et al. came up with a solution to filling the hole. [13]

In order to fill the whole, a two-step process was performed. The first step involves a typical friction stir weld. The second step involves removing the non-consumable bit and polishing off the whole using the shoulder. The result of this procedure can be seen in Figure 5. This additional step led to higher shear strengths. It is also a practical design because there is no longer a hole region where stress concentrations can occur [13].

Figure 5 - Two step method for sealing holes using FSW [13]
2.2.2 Friction Bit Joining

Friction bit joining has some advantages to friction stir spot welding, particularly for the automotive industry. Friction bit joining is known to achieve higher levels of joint strength than self-piercing riveting (SPR). The reason for such strength is due to the bit’s ability to match steel base metals in Al/steel dissimilar metal bonding. This leads to a similar metal diffusion bond which closely resembles an inertia friction weld. [14]

![Figure 6 - Process of friction bit joining in three phases](image)

The process of friction bit joining is very similar to that of friction stir welding. This includes its plunging phase, joining phase, and pull-out or stopping phase as illustrated in Figure 6. The bit in this case is consumable and joined with the surrounding metal, compared to FSW which does not fuse its pin and pulls it out at the last step. The
advantage of this method is that it does not leave a hole and takes less time than friction stir refill spot welding.

Friction bit joining is commonly studied for dissimilar metal welds. Miles et al used FBJ to produce dissimilar alloy joints of 1.4 mm DP980 and 1.8mm AA5754-O. Lap-shear shear fracture loads averaged 6.3 kN, while cross tension fracture loads averaged 2.5 kN. A comparison of these results with self-piercing rivet data revealed that joints of 1.6 mm HSLA 350 and 2.0 mm AA 5754-O supported more than twice the lap shear fracture stress of the self-piercing rivet joints, where failures in both types of joints occurred in the weaker AA 5754-O component of the joint assembly. [14]

In another study, Miles et al. successfully friction bit joined aluminum alloy A356 to grey cast iron, with lap shear fracture loads approaching 7 kN (Figure 7). In the case of a 6-mm-thick aluminum coupon welded to an 8 mm thick cast iron coupon, bonding was enhanced by interstitial free steel in the tool bit, which promoted greater heating than was possible between the D2 steel joining bit and cast iron. [16]
2.3 Epoxy Adhesives

Epoxy adhesives are chemical compounds used to join components by providing a bond between two surfaces. Epoxy adhesives were first commercially available in 1946. Epoxies are known for being the most versatile because they bond to many substrates and can be modified with little effort to achieve widely varying properties [17].

Due to good wetting characteristics, epoxy adhesives can adhere to a wide variety of substrates with the exception of low-surface-energy, untreated plastics and elastomers.

Figure 7- Friction bit weld between AA7075 and grey cast iron [16]
Low shrinkage on curing and high resistance to creep are a quality that serves notably well for automotive vehicles. Epoxy adhesives have an excellent resistance to oil, moisture, and many solvents.
Industries using epoxy adhesives include aerospace, civil engineering, automotive, chemical, electrical, marine, leisure and many others Table 1.

The decision to use adhesive bonding over other joining methods (i.e., screws, rivets, and spot welds) depends on the specific application. Certain plastics may require...
extensive surface preparation to allow adhesive to properly wet the surface. On the other hand, some applications could not exist without adhesive bonding. This includes joining of ceramic or elastomeric materials, joining of significantly thin substrates, joining of surface skin to honeycomb, as well as many other applications could not be successfully performed without these adhesives. A list of applications is provided in Table 2.

<table>
<thead>
<tr>
<th>Application areas for adhesives</th>
<th>Examples</th>
</tr>
</thead>
<tbody>
<tr>
<td>Dissimilar materials</td>
<td>Combinations of metals, rubbers, plastics, foamed materials, fabrics, wood, ceramics, glass, etc.</td>
</tr>
<tr>
<td>Dissimilar materials that constitute a corrosion couple</td>
<td>Iron to copper or brass</td>
</tr>
<tr>
<td>Heat-sensitive materials</td>
<td>Thermoplastics, magnetic materials, glass</td>
</tr>
<tr>
<td>Laminated structures</td>
<td>Sandwich construction based on honeycomb materials; heat exchangers; sheet laminates, core laminates</td>
</tr>
<tr>
<td>Reinforced structures</td>
<td>Stiffeners for wall paneling, boxes and containers, partitions, automobile chassis parts, aircraft body parts</td>
</tr>
<tr>
<td>Structural applications</td>
<td>Load-bearing structures in the aircraft fuselage, automotive and civil engineering industries</td>
</tr>
<tr>
<td>Bonded inserts</td>
<td>Plug inserts, studs, rivets, concentric shafts; tubes, frame construction; shaft-rotor joints; tools; reinforced plastics with metal inserts; paintbrush bristles</td>
</tr>
<tr>
<td>Sealed joints and units</td>
<td>Pipe joining, encapsulation, container seams, lid seals</td>
</tr>
<tr>
<td>Fragile components</td>
<td>Instrumentation, thin films and foils, microelectronic components and others where precise location of parts is required</td>
</tr>
<tr>
<td>Components of particular dimensions</td>
<td>Where bonding areas are large or there is a need for shape conformity between bonded parts</td>
</tr>
<tr>
<td>Temporary fastening</td>
<td>Where the intention is to dismantle the bond later, the use of various labels, surgical and pressure-sensitive tapes, adhesives for positioning and locating parts, in lieu of jigs, prior to assembly by other means</td>
</tr>
</tbody>
</table>

Table 2 - List of applications for epoxy adhesives [17]

2.3.1 Thermosetting Polymers
There are two classes of polymers: thermoplastic and thermoset. Each are distinguished by their primary response. A thermoplastic polymer behaves like a liquid above a specific temperature while the heating of a thermoset leads to degradation without achieving a liquid state; i.e. thermoplastics are reversible and thermosets are not. The state in which a polymer becomes a liquid occurs when individual macromolecules separate and produce flow. This is only possible for polymers containing linear or branched chains, held together through secondary forces such as Van der Waal, dipole-dipole, and hydrogen bonding. A comparison of the structure between thermoplastics and thermosets are shown in Figure 8.

Thermoplastic polymers can be either amorphous or semi-crystalline. Amorphous polymers adapt fluid-like behavior once above the glass transition temperature. The glass transition temperature, $T_g$, is a threshold temperature for polymers that transfer from hard glassy material to soft rubbery material. Semi-crystalline polymers must exceed their $T_g$ and reach melting to break the crystalline structure.

Thermoset polymers cannot achieve a liquidus state once in final form because they are heavily crosslinked.
A distinctive characteristic of a thermosetting polymer is that it consists of one giant macromolecule consisting of covalently bonded repeating units. Because these polymer networks are produced in an irreversible way, the synthesis of thermosetting polymer produces a final material with the desired shape. Subsequently, polymerization (defined as the reaction of monomers to form polymers) and final shaping are performed within the same process. Processing thermoset polymers under incorrect conditions leads to irreversible losses of the material. Thermoplastics have an advantage over thermosets for circumstances where parts must be reused and recycled.

One of the most important parameters for characterizing a thermosetting polymer is its glass transition temperature with respect to the temperature at which it is used. Most
thermosetting polymers are formulated so that their glass transition temperature is higher than its used temperature so that they behave like glass while in use. Polymers with a use temperature higher than glass transition are classified as rubbers, although regarded as thermosetting polymers operating in a rubbery state. A cure required at higher temperatures will lead to a thermoset with continued mechanical performance at higher temperatures. [17]

Some typical examples of thermosetting polymers are phenolic and urea-formaldehyde resins, unsaturated polyesters, and epoxy resins.

The epoxy used in the automotive industry requires temperatures of up to 180°C to fully cure. This is advantageous in the automotive industry because each BIW must go through paint bake cycles of up to 180°C. By incorporating this type of epoxy, adhesive curing can take place simultaneously with paint curing.

2.3.2 Fracture Mechanics Theory and Adhesive

Fracture mechanics is the study of mechanical behavior of cracked materials subjected to an applies load. Irwin [18] developed the field of fracture mechanics from studying the early work of Inglis [19], Griffith [20], and Westgaard [21]. The core of fracture mechanics lies in the imperfections which lead to voids and rupture due to nucleation and growth of cracks. The behavior of the crack growth is heavily dependent on the microstructure. For metals this includes crystal phases, inclusions, grain size, precipitates, and dislocation motion. These constituents are what creates the potential for
voids, and therefore cracking. In adhesive, these potential crack initiators are contributed to regions of unbonded material or a manufacturing defect [22]. Brittle failure is a low energy dissipation process with a high crack velocity. Ductile fracture on the other hand is a high energy dissipation process involving low crack velocity. Brittle fracture will result in little plastic deformation when compared to ductile fracture.

In early work of studying fracture, cracks were modeled in terms of stress concentrations. The systemic flaw in this approach is that it would have to be assumed that there is infinite stress at the crack tip. This is physically impossible, so Griffith came up with the energy balance approach. In 1921, he postulated that for unit crack extension to occur under the influence of applied stresses, the decrease in potential energy of the system must equal the increase in surface energy through the means of displacement of the outer boundaries and the change in stored elastic energy.

Based on the results of Inglis, Griffith discovered that cracking caused a net change in potential energy of the plate, and from this he derived the fracture stress:

$$\sigma_f = \sqrt{\frac{EG_c}{\pi a}}$$  \hspace{1cm} (1)

where $\sigma_f$ is the stress when fracture occurs, $E$ is the elastic modulus, $G_c$ is the critical fracture energy release rate, and $a$ is the crack length. This expression defined the interaction between the material ($G_c$), the stress level ($\sigma_f$), and size of flaw ($a$). This equation is useful for determining the fracture stress when defining a minimum visible flaw ($a$).
For brittle materials, the stress intensity approach examines the stress state near the tip of a sharp crack. There are three types of cracks: mode I, mode II, and mode III.

Figure 9 - Fracture modes

Figure 9 shows the three opening modes. Mode I is the normal opening mode, while modes II and III are two shear sliding modes. The stress intensity factor, $K_I$, is the central concept for this stress intensity approach. The stress intensity factor describes the stress state at a crack tip, is related to the rate of crack growth, and is used to establish failure criteria due to fracture. The stress intensity factor is a theoretical construct which is typically applied to a homogeneous, linear elastic material and is useful for providing failure criterion for brittle materials. Table 3 shows the stress intensity factors for common geometries.
Table 3 - Stress intensity factors for several geometries [23]

<table>
<thead>
<tr>
<th>Type of Crack</th>
<th>Stress Intensity Factor, $K_I$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Center crack, length 2a, in an infinite plate</td>
<td>$\sigma_\infty \sqrt{\pi a}$</td>
</tr>
<tr>
<td>Edge crack, length a, in a semi-infinite plate</td>
<td>$1.12 \sigma_\infty \sqrt{\pi a}$</td>
</tr>
<tr>
<td>Central penny-shaped crack, radius a, in in infinite body</td>
<td>$2 \sigma_\infty \sqrt{\frac{a}{\pi}}$</td>
</tr>
<tr>
<td>Center crack, length 2a in plate of width W</td>
<td>$\sigma_\infty \sqrt{W \tan \left(\frac{\pi a}{W}\right)}$</td>
</tr>
<tr>
<td>2 symmetrical edge cracks, each length a, in plate of total width W</td>
<td>$\sigma_\infty \sqrt{W \left[\tan \left(\frac{\pi a}{W}\right) + 0.1 \sin \left(\frac{2\pi a}{W}\right)\right]}$</td>
</tr>
</tbody>
</table>

The fracture stress is defined as:

$$\sigma_f = \frac{K_{fc}}{\alpha \sqrt{\pi a}}$$  

(2)

where alpha is the geometrical parameter (equal to 1 for edge cracks and generally on the order of unity for other situations). The stress intensity and energy approaches are interrelated, as seen below.

$$\sigma_f = \frac{\sqrt{EG_c}}{\sqrt{\pi a}} = \frac{K_{fc}}{\sqrt{\pi a}}$$  

(3)
This relation applies for plane stress, and for plane strain the relationship is defined as:

\[ K_{lc}^2 = \frac{E G_c}{(1 - \nu^2)} \]  \hspace{1cm} (4)

With \( \nu \) being Poisson’s ratio. Typical values of \( G_{lc} \) and \( K_{lc} \) for an array of materials are listed in Table 4. It is noted that the fracture toughness values vary widely for different types of materials. Certain polymers can be very tough, but steel alloys have the highest fracture values in terms of absolute resistance to crack propagation.

<table>
<thead>
<tr>
<th>Material</th>
<th>( G_{lc} )(kJm(^{-2}))</th>
<th>( K_{lc} )(MNm(^2))</th>
<th>( E )(GPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Steel alloy</td>
<td>107</td>
<td>150</td>
<td>210</td>
</tr>
<tr>
<td>Aluminum alloy</td>
<td>20</td>
<td>37</td>
<td>69</td>
</tr>
<tr>
<td>Polyethylene</td>
<td>20 (( J_{lc} ))</td>
<td>—</td>
<td>0.15</td>
</tr>
<tr>
<td>High-impact polystyrene</td>
<td>15.8 (( J_{lc} ))</td>
<td>—</td>
<td>2.1</td>
</tr>
<tr>
<td>Steel — mild</td>
<td>12</td>
<td>50</td>
<td>210</td>
</tr>
<tr>
<td>Rubber</td>
<td>13</td>
<td>—</td>
<td>0.001</td>
</tr>
<tr>
<td>Glass-reinforced thermoset</td>
<td>7</td>
<td>7</td>
<td>7</td>
</tr>
<tr>
<td>Rubber-toughened epoxy</td>
<td>2</td>
<td>2.2</td>
<td>2.4</td>
</tr>
<tr>
<td>PMMA</td>
<td>0.5</td>
<td>1.1</td>
<td>2.5</td>
</tr>
<tr>
<td>Polystyrene</td>
<td>0.4</td>
<td>1.1</td>
<td>3</td>
</tr>
<tr>
<td>Wood</td>
<td>0.12</td>
<td>0.5</td>
<td>2.1</td>
</tr>
<tr>
<td>Glass</td>
<td>0.007</td>
<td>0.7</td>
<td>70</td>
</tr>
</tbody>
</table>

Table 4 - Fracture toughness of materials [23]

Structural adhesive joints have fewer modes of failure compared to metals or plastics. An example of this is in metals, where fracture may occur due to excessive elastic deformation, or in other cases permanent deformation due to deficient inelastic strength properties, or they may just fracture. Adhesives are, by default, limited to a very
small area compared to the total volume. Even sizeable elastic or plastic deformations are typically tolerable since they would be insignificant in the deformation of the actual structure. Therefore, prevention of cracking is the critical problem.

Durability of the joint is not solely dependent on the adhesive used. It is a complex function of many different factors. Chemical and physical aging, the presence of moisture, and curing conditions all affect durability. Surface preparation and surface treatment play an essential role in durability. The direction of load is very important considering adhesives typically have anisotropic properties, being stronger in shear directions than in the normal direction.

Geometry plays a large role in the durability of the adhesive as well. Some examples of these include thickness of the adherends, thickness of the adhesive, amount of overlap, and many other factors.

Grein et al. developed a corrected plastic zone linear elastic fracture mechanics (LEFM) approach to describe the fracture toughness of polymers [24]. This approach took into account the high plasticity of tough polymers and consisted of experimentally determining the size of the plastic zone developed at the crack tip using specimens with different crack lengths. The effective fracture toughness, $K_{\text{eff}}$, was determined from the slope of the plot $F_{\text{max}}$ over $BW^{1/2}/f(a_{\text{eff}}/W)$ with $a_{\text{eff}} = a + r_p$; the plastic zone radii, $r_p$, were obtained numerically using an iteration procedure in such a manner that all data fall on a line through the origin. This approach displayed geometry independent values for ductile and brittle fracture behavior, except near the ductile-brittle transition zone.
Since the transition is an important parameter for characterizing toughness in polymers, Grein et al. combined the plastic zone corrected LEFM with a more conventional approach using the speed at which the ductile-brittle transition is determined at given ligament length (typically \( W = 0.5 \)). Both approaches provided complimentary information regarding toughness, they were thought to give a complete picture of the fracture behavior in any given material.

Kinloch et al. studied the influence of bond line thickness on the toughness of adhesive joints. This was achieved through considering the size of the plastic zone and provided an account for both qualitative and quantitative bond line thickness. In one case, the fracture toughness of a tapered double cantilever beam (TDCB) adhesive joint with mild steel substrates was investigated as a function of bond line thickness and test speed. Figure 10 shows the results of these tests with plots of toughness vs bond line thickness; these plots are observed to reach a maximum peak before falling into a plateau. Similar results were observed at both speeds.
The explanation for such curvature has to do with the plastic zone size at the crack tip relative to the bond line thickness (t) of the adhesive. At small values of bond line thickness, the plastic zone is relatively large. Since the resin is incapable of fully accommodating the plastic zone size, the fracture toughness is small, but increases with the increase in bond line thickness.

When twice the plastic zone radius equals the bond thickness, the plastic zone is considered filled. Due to the presence of substrates, the plastic zone can take a non-
circular form as seen in Figure 11. This further increases the toughness to the maximum point.

![Plastic zone diagram](image)

**Figure 11**- The plastic zone just fills the resin region [25]

Lastly, when the adhesive thickness increases passed the plastic zone, the plastic zone can become small in comparison. Consequently, the adhesive fracture toughness becomes independent of the bond line thickness and is approximately equivalent to plane-strain toughness in the bulk resin.

Roche et al studied the influence of epoxy-metal interphase properties on fracture toughness [26]. When epoxy-diamine prepolymer were applied onto metallic substrates, interphases between the coating part having the bulk properties and the metallic surface were created. The mechanisms of interphase formation were dominated by dissolution and diffusion phenomena. The interphase formation was related to the liquid-solid contact duration between the pre-polymers and metallic substrates. It was found possible to control interphase formation using an appropriate curing cycle. Roche et al. observed that interphase formation decreased adhesion by increasing residual stress and increasing
hydrothermal durability of the coated samples. It was concluded that practical adhesion represented by strain energy release rate depends on the residual stress.

Samples containing interphase were subject to higher residual stress level compared to ones without interphase. However, the presence of the interphase and the organo-metallic complexes acted as a barrier material to water corrosion within the interphase region, although these organo-metallic compounds still exhibit signs of residual stress. It was concluded that overall properties of coatings, including interphase ones, depend on the contact duration between metallic surfaces and liquid prepolymer and can account for the different findings observed in the literature.

Sing et al. used micron- and nanometer-sized aluminum particles as reinforcements to enhance the fracture toughness of a highly-crosslinked, nominally brittle, thermosetting unsaturated polyester resin [27]. Both particle size and particle volume fraction were systematically varied to investigate their behavior on the fracture toughness and fracture behavior. The sizes of the aluminum particles included 20 µm, 3.5 µm, and 100 nm with a volume fraction of 0.5%, 0.9%, 2.3%, and 4.4%. Experimental results concluded that both particle size and volume fraction played a major role in fracture toughness. As a general trend, the fracture toughness increased monotonically with the volume fraction of aluminum particles, for a given particle size. It was also observed that the increase in fracture toughness was significantly greater for smaller particles, for a given particle volume fraction.

Crack front trapping was identified as the primary extrinsic toughening mechanism by conducting scanning electron microscopy of the fracture surfaces.
Maintaining a uniform particle dispersion and deagglomeration, and proper particle wet-out were found to be crucial in order to ensure that the reinforcement particle promoted crack trapping. The effect of particle volume fraction and size on the tensile properties of the polyester-aluminum composite were also investigated. The measured elastic modulus was in accordance with the rule-of-mixtures. The tensile strength was slightly reduced as a consequence of the inclusion of aluminum particles in the polyester matrix.

Bortz et al. demonstrated significant toughness and fatigue life improvements through the addition of uniquely synthesized and scalable graphene oxide sheets to a thermosetting epoxy system [28]. The fatigue life improvements were observed over the full range of experimental stress levels offering potential performance improvements to numerous applications. Bortze has shown that the effects on the cross-linking reaction and monotonic tensile and flexural properties disseminated by the sheets. The oxygen-containing functionalities present on the basal plane surfaces and edges of the graphene oxide sheets triggered onset of the cure reaction at lower temperatures and resulted in slightly larger exotherms in calorimetric scans. Consequently, moderately higher glass transition temperatures and shifting of the loss modulus were measure in the nanocomposites.

Sheet resistance decreased by 4 orders of magnitude. A larger decrease was not observed due to the insulating nature of graphene oxide. Improvements in stiffness and strength were modest but significant, particularly the increase at low graphene oxide weight fractions. There were noticeable toughness and longevity enhancements due to deflection and pinning of small-scale propagating cracks.
Mostovoy et al. measured the fracture toughness of epoxy used in the bulk and adhesive form, implementing previously developed techniques [29]. A uniform double cantilever-beam specimen was modified to a tapered beam; this simplified experimental procedure and calculations for obtaining fracture toughness. Mosotovoy varied the reaction of hardener to resin and post-cure temperature on a single epoxy system. The results showed that the toughness of the epoxy in either bond or bulk form varied by a factor of 5. The bulk material was found to have a higher toughness from the combination of composition and post-curing temperature compared to the bond form. On the contrary, at high post-cure temperatures, the toughness of the bonds exceeded that of the bulk material. Therefore, it is not possible to predict the toughness of a joint using bulk properties.

Bo et al. studied the relationship between fracture toughness and temperature in epoxy coatings [30]. Fracture toughness of nine kinds of rubber modified epoxy coating systems were tested at four temperatures: 20°C, 0°C, -20°C, and -40°C. It was discovered that the toughness of the epoxy system cured by an amine curing agent was closely related to the test temperature. Rubber modifier changed the morphology of the fracture surface of the epoxy system at low temperature. The external deformation in the fracture surface increased the dissipation of energy and resulted in higher values of fracture toughness. It was found that the values of fracture toughness was influenced by the modulus. A combination of morphology of the fracture surface and modulus led to high toughness. When the epoxy system was kept in the ductile tearing station at low temperatures, the higher toughness was attributed to the modulus increase with cooling.
The addition of rubber modifier was found sufficient to improve anti-cracking performance of the epoxy coatings due to the increase in fracture toughness and the relative low modulus, which can reduce the thermal stress in the cooling process.

2.3.3 Epoxy and Adhesion

Adhesion is the tendency of dissimilar particles or surfaces to cling to one another. There are several types of forces when it comes to adhesion: chemical adhesion, dispersive adhesion, and diffusive adhesion. As a result of these adhesion forces, mechanical effects emerge.

Epoxies are specially formulated to have excellent adhesion. This type of bonding is both chemical and mechanical. Mechanical interlocking is the process where the adhesive penetrates the pores and crevices of the material, once cured, the adhesive is “interlocked” into the material and difficult to remove. Chemically, there exists secondary forces for bonding such as Van der Waal forces and hydrogen bonds. Epoxy adhesion heavily rely on both these factors.

Many metals such as aluminum, iron, and zinc form sizeable oxide layers (40-80 Å) that are thick enough to prevent any direct interaction between the metal and the adhesive. [31] The surface oxygens of the metal oxides hydrate to form surface hydroxyl groups under normal ambient conditions. Figure 12 depicts the chemical representation of oxides bonded to metal surfaces.
It is not safe to assume that epoxies should adhere to all metals equally because they all consist of a hydrated oxide surface layer. This is not the actual case because activity of the hydroxyl groups is heavily influenced by the type of metal atom to which they are attached. The number of hydroxide groups on the surface can be varied by changing the prebonding thermal history. The presence of the hydrated oxide surface layers provides an advantage since it enhances wetting of metal surfaces for epoxies and other polar resins.

![Schematic representation of water and oxide layers on a metal surface](image)

Figure 12 - Schematic representation of water and oxide layers on a metal surface [32]

There are also disadvantages to these hydrated surfaces. Oxide layers will allow water and polar organic contaminant to be readily absorbed and retained. Up to twenty molecular layers of water have been found to exist on ‘dry’ metal surfaces in studies of
aluminum, copper, and iron under normal ambient conditions [33]. Contaminants which are non-polar tend to become displaced by polar contaminants, but can still be present to interfere with bonding to a limited extent. Figure 13 depicts different layers of water and contaminant that have the potential to build up on metal surfaces and cause interference for adhesion.

![Figure 13 - Hierarchy of spontaneously absorbed layers on a metal surface [32]](image)

Initial good adhesion strength values have to do with the epoxy chemistry. Aliphatic hydroxyl and ether groups are present in the initial resin chain and in the cured polymer. Therefore, epoxy resins have high polarity. These polar groups serve as sites for the formation of strong electromagnetic bonding attractions between epoxy molecules and metal oxides [34]. The epoxide group or oxirane ring can also aid in metal/epoxy
adhesion through the formation of chemical bonds with active hydrogens on the metal surface [35].

If the metal surface has been given treatment prior to application of adhesive, such as surface roughening or anodization in the case of aluminum, then mechanical aspects play an important role in the adhesion strength of metal/epoxy systems. While the adhesive is in a liquid state, it can penetrate the crevasses and pores of the oxide layer created through pretreatment. Upon curing, the epoxy resin becomes mechanically interlocked into the oxide structure. Bascom [36] and Packham [37] have suggested that when failure of these systems takes place, considerable plastic deformation of the embedded epoxy resin will occur, with pores and fibrous ends acting as nucleation sites for deformation. The energy dissipated in the plastic deformation of the resin will contribute to the increased adhesion strength of the system.

There are many advantages when it comes to bonding metals using adhesives. Some of these advantages include:

1. Bonding allows for stresses to be distributed over large areas of the joint, therefore preventing local stress concentrations present in riveted or spot-welded joints and increasing overall fatigue resistance.
2. Bonding is often faster and cheaper than welding or bolting.
3. Bonding allows for joining thinner gauge materials and honeycomb structures, allowing the opportunity to produce lighter structures.
4. Due to adhesive’s dielectric properties, corrosion is minimized between dissimilar metals.
5. Bonding can eliminate crevices which would otherwise be susceptible to crevice corrosion in riveted joints.

6. Bonding can greatly simplify construction and design techniques. [32]

These advantages are widely accepted, although it has proven to be a challenge to implement on a large industrial scale. The main reasons why adhesives have not yet realized their full potential are, first, the durability of adhesive joints is poor in wet environments. Second, there is no known successful method for non-destructive testing of adhesive joints. Third, most structural adhesives such as epoxies require an elevated temperature for cure. The elevated curing temperature is not a challenge for automotive except when curing between two dissimilar metals.

Zhang et al. conducted a systematic study to investigate the effects of various aluminum surface morphologies on the fracture resistance of the epoxy-aluminum interface [38]. The geometry was a bi-layer double cantilever beam, which was chosen for its advantages over other specimen geometries, including simple calculation of fracture resistance, achievement of stable crack propagation behavior, and easy optical monitoring of the crack itself. The experimental results showed that an increase in surface roughness enhances the interfacial fracture resistance. The non-linear behavior indicated that such an enhancement is caused not only by the increase in actual contact area between the epoxy and aluminum due to roughness, but also by the change of local mode mixity, as well as bridging and friction behind the crack. Zhang’s results demonstrated
that the roughness index is the primary factor compared to nanoscale surface features of the aluminum surface.

<table>
<thead>
<tr>
<th>Specimen ID</th>
<th>Surface treatment</th>
<th>Roughness index ($R$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>SA-1</td>
<td>Sapphire surface</td>
<td>2.82E−07</td>
</tr>
<tr>
<td>O2-2</td>
<td>CRATER (2.8 V)</td>
<td>4.69E−05</td>
</tr>
<tr>
<td>O5-3</td>
<td>CRATER (5.8 V)</td>
<td>3.08E−05</td>
</tr>
<tr>
<td>O9-4</td>
<td>CRATER (9.6 V)</td>
<td>6.11E−05</td>
</tr>
<tr>
<td>DP-5</td>
<td>1 ( \mu \text{m} ) finish</td>
<td>3.89E−06</td>
</tr>
<tr>
<td>G600</td>
<td>600 grit finish</td>
<td>4.79E−03</td>
</tr>
<tr>
<td>G320</td>
<td>320 grit finish</td>
<td>2.90E−02</td>
</tr>
<tr>
<td>G180</td>
<td>180 grit finish</td>
<td>2.87E−02</td>
</tr>
<tr>
<td>G60</td>
<td>60 grit finish</td>
<td>5.84E−02</td>
</tr>
</tbody>
</table>

Table 5- Surface treatment and roughness index [38]
Brand et al. investigated two different methods for improving adhesion and durability of the adhesion of epoxy coatings to an aluminum substrate [39]. The first approach was by application of a thin polymeric layer, having a thickness of about 10nm, on the aluminum substrate prior to the application of the epoxy adhesive. The second method was by hydration of the aluminum substrates, by immersing in boiling water. This induced the formation of a porous pseudoboehmite oxyhydroxide layer.
Although many polymeric layers were tested in the first method, the only polymer that proved to show good adhesion was the poly(ethylene-alt-maleic anhydride) -based system. As for the second method of dipping the aluminum into a boiling water, the epoxy coating was found to fully penetrate the dobeohmite oxyhydroxide layer. The high hydroxyl density on the oxide surface led to a surface area of highly porous nature and increased surface area. It led to improved adhesion performance for the system. Additionally, very limited water was absorbed by the system upon exposure.

Nakazawa studied the mechanism of adhesion between steel sheet surfaces and epoxy resins [40] for cold-rolled steel, galvanized steel, and galvanized steel sheets. All specimens were bonded with epoxy resin, and the resultant adhesive joints were examined for such macroscopic phenomena as bonding strength, and bond strength loss and change in the mode of failure through exposure to humidity. The epoxy model compounds were then absorbed on zinc oxide and iron oxide surfaces, and resultant interfacial bonding was studied at the molecular level.

It was found that the adhesive joints of galvanized steel sheets suffered adhesive failure between the adhesive and zinc coating and were poor in durability when wet. Adhesive joints of the galvannealed steel sheets and cold-rolled steel sheets underwent cohesive failure (i.e., failure through adhesive itself) and were superior in wet durability. This suggested that a surface composed of zinc oxide along with a surface containing iron oxide in addition to zinc oxide are different in the mechanism of adhesion with epoxy resins and mechanism of deterioration in adhesion through exposure to humidity.
2.3.4 Curing of Epoxy Adhesive

Unlike thermoplastic polymers, thermosetting polymers require curing. As illustrated in Figure 15, curing starts by growing and branching of chains. As the reaction continues, the increase in molecular weight accelerates, causing an increase in viscosity, and a reduction in the total number of molecules. After a certain period, several chains have linked together into a network of infinite molecular weight. The irreversible, abrupt transformation from a viscous liquid to an elastic gel or rubber is called the gel point. The gel point of a cross-linked system can be defined as the instant at which the weight average molecular weight diverges to infinity [41].
Figure 15 - Two dimensional representation of thermoset curing. (a) uncured collection of monomers and oligomers. (b) Simultaneous linear growth and branching to a more viscous B-stage material below gel point. (c) formation of gelled and partially crosslinked network. (d) cure is finished, now considered at C-stage [41].

Uncured epoxy itself exists as a collection of resins consisting of a central polymer portion and two epoxy rings located on each end of the polymer portion.
When enough thermal energy enters the system, the epoxy rings at both ends undergo reactions with hardener molecules. Examples of substance groups that function
As epoxy hardeners are: amines, amides, acid anhydride’s, imidazoles, boron trifluoride complexes, phenols, mercaptans and metal oxides [44]

As curing progresses, more of these epoxy rings connect with the hardener molecules until the epoxy is fully cured and composed as an entire macromolecule. This solidified, amorphous network gives epoxy its superior mechanical properties. Figure 17 illustrates an uncured epoxy resin, it consists of two epoxide rings and a polymer chain portion in between. Upon curing hardener molecules link to the epoxide groups (Figure 18). An alternative view of this is shown in Figure 16.

Figure 18 – Molecular curing reaction with epoxy [42]

Because of their high degree of crosslinking, epoxy is subject to high strength but low fracture toughness. In other words, epoxy by itself can be too brittle for certain
applications. Compensations have been made by incorporating rubber particles, high performing thermoplastics or nano materials. [45]

Crawford et al. discovered that epoxy networks with controlled microstructure were created by curing a DGEBA resin with ethylenediamine and 3-methoxypropylamine. This increasing fracture toughness was quantified by the tearing modulus. In addition, the ductile specimens displayed significant shear lip behavior in front of the crack tip. The fracture energy increased as the yield strength decreased [46].

The curing process of thermosetting polymers has several stages depending on time and temperature. Figure 19 shows the time-temperature-transformation diagram, or commonly known as a T-T-T diagram. The thermosetting adhesive starts out as a viscous liquid. In the first stage of curing, the temperature rises above the glass transition temperature, \( T_g \). The glass transition temperature is known as a range of temperatures with \( T_{g0} \) being the lower bound and \( T_{g\infty} \) being the upper bound. \( T_g \) is between \( T_{g\infty} \) and \( T_{g0} \) and is influenced by factors such as the chemical structure of the epoxy resin, the type of hardener and the degree of cure. This is because the cross-linked polymer chains have multiple degrees of freedom and modes of movement as a response to the applied thermal energy [47].

In the second stage, it is held at \( T_{R1} \) (above \( T_g \)) for a sufficient amount of time to allow all crosslinking reactions to occur. At the last stage, the adhesive begins to cool. Once it cools below its \( T_g \), then it retains its strengthened, cured properties.
The glass transition temperature can be determined using differential scanning calorimetry (DSC) following ASTM E1356, “Standard Test Method for Assignment of the Glass Transition Temperature by Differential Scanning Calorimetry”. DSC is a thermal analysis technique used to measure the difference in the amount of heat flow rate necessary to increase the temperature of a sample and a reference as a function of temperature [49]. The results are shown as heat flow response as a function of temperature. The $T_g$ is typically determined when the adhesive is cured, although different values of $T_g$ can occur depending on cure state. In the case of Figure 20, $T_g$ is
determined as the midpoint of the temperature range, bounded by tangents of the two flat regions of the heart flow curve.

![DSC Scan of a Typical Medium Tg Epoxy](image)

Figure 20 - Results of DSC scan of cured epoxy to determine $T_g = 75^\circ C$ [47]

DSC is not only used for epoxy adhesives in the cured state, but is also used to determine cure kinetics as well [50]. In this case, the kinetics can be associated with the rate of cure dependent on the reaction occurring. The amount of reactants compared to the product determines the degree of cure, typically denoted as $\alpha$ where $0 < \alpha < 1$. When $\alpha=0$, the solution is 100 percent reactants and zero percent product, and when $\alpha=1$ the solution is 100 percent product and zero percent reactants. DSC is able to determine $\alpha$ because the reaction is exothermic, and will produce a unique thermal profile as seen in Figure 21.
In Figure 21, a baseline run of 10°C/min is conducted following two runs of 2°C/min and 165°C/min. Varying the heating rate is a technique to provide the means to determine the activation energy for cure. A master cure curve can be constructed using time-temperature superposition from isothermal data at various temperatures as well as using the activation energy. Kamal et al. used differential scanning calorimetry to comprise a model for kinetics of isothermal cure. The model yielded results which were qualitatively in agreement with typical experimental data of cure rate and integral heat. With the use of this data, mathematical techniques were developed. Specifically, the determination of specific heat as a function of temperature and degree of reaction. The
results obtained on specific heat yielded useful information regarding the transitions which the material underwent during curing [52].

Hale et al. developed an equation which modeled the dependence of the glass transition temperature (Tg) on the extent of reaction in highly cross-linked thermosetting polymers. An assumption is made that the increase of Tg is caused by multiple factors such as: decrease in chain end concentration, formation of effective cross-links, and further decrease in the configurational entropy due to departure from Gaussian behavior at high cross-link densities. Hale et al. developed an equation that could predict the changes in glass transition of a thermosetting system at a low conversion by using independently obtained data [53].

2.4 Modeling Adhesives

2.4.1 Adhesive Curing Modeling

As for curing, Sung et al. developed a nonlinear transient heat transfer finite element model to simulate the curing process of polymer matrix composites [54]. Temperature distributions inside laminates were evaluated by solving the nonlinear anisotropic heat conduction equations including internal heat generation produced by exothermic chemical reactions. The results were limited in accuracy due to the use of isotropic material properties as opposed to anisotropic.
Jerome et al. performed an analysis to simulate the curing process of axisymmetric filament-wound composites. The model related the cure temperature to the thermal, chemical and physical processes occurring in the composite during cure. Results of the cure analysis showed that the temperature distribution inside the composite depended on the thermal conductivities of the material as well as the curvature and shape of the assembly. For thermoset composites, heat generation due to exothermic chemical reactions caused a substantial temperature 'overshoot', resulting in a non-uniform temperature distribution, which must be controlled during cure. [55]

In another study, researchers developed FEM coupling model properties prediction during the curing of an epoxy matrix. This model accounted for the thermal, chemical and mechanical couplings as seen in Figure 22.
Figure 22- – Diagram of various subroutines used in Abaqus to improve model accuracy

[56]
Prediction of local temperature evolution was very consistent except for the decrease of the exothermic peak that is done too rapidly, as shown in Figure 23. This difference is related to heat exchanges between the matrix and the air above the Pyrex test tube that was not taken into account for the simulation. It is particularly interesting to note that temperature start and exothermic characteristic time were well fitted by the model. This validates thermal parameters modelling on one hand, and a good coupling between heat produced by the reaction and the heating on the other hand.
Figure 24 - Dynamic mechanical properties during cure of epoxy resin [57]

The mechanical behavior during the curing process is changing all the time. Lewis considered the dynamic mechanical behavior during the thermosetting curing process with respect to the curing of an epoxy with an amine; three transition regions were detected. Figure 24 shows the flexural modulus versus time as a result of the flexural braid analysis of epoxy resin-DETA curing reaction at 25°C. Peak A was observed within the region of the expected gel point of the system. In peak B, a large maximum appeared. Since peak B was associated with the rubber to glass transition, it was called the glass transition point of the system. The third peak, peak C, was discovered to be a characteristic of the internal structure of the resin. From thermal-
mechanical studies, it was concluded that the C peak was reflecting internal rotation of pendant side chains composed of unreacted epoxide groups [57]

2.4.2 Plasticity of Cured Epoxy

Yielding is a permanent deformation incapable of recovery without increased temperature. In the case of metals, plasticity occurs upon exceeding the elastic limit of the material. This is not the same for thermosets, once these polymers exceed the elastic limit, over time they may return to their original form.

The deformation mechanism of yielding thermosets is different than metals. During yielding of thermosets, local shear induces changes of chain configuration. Amine chains are preferentially aligned in the loading direction due to their relative flexibility. No clear experimental evidence of crazing has been produced, yet they are occasionally encountered in studies [58].

There are several widely known theories for plastic deformation in cured thermosets.

The first of these theories was an empirical theory developed by Brown in 1971. Brown proposed that ratio between yield stress and elastic modulus is a constant value in the range of 1/60 to 1/30. Many authors demonstrated that Brown’s rule has no general validity, especially with no stoichiometric networks [59].

The second theory uses free volume for yielding prediction. The free volume of a thermoset is calculated, then used to predict the glass transition temperature under stress,
At a temperature $T$, the polymer reaches a critical state and yielding starts to occur. Using $T_g$ and $T$, the strain during plastic yielding can be determined.

A typical behavior of polymers is that the yield stress increases when test temperature decreases and the strain rate increases, suggesting that the presence of thermally activated mechanisms. In Eyring’s theory, yielding occurs when molecular segments jump due to stress and temperature activation. The applied stress reduces the activation energy required to induce motion. This model provides a good fit to experimental data for thermosets on a large range strain rates. Eyring’s equation may be regarded as a good phenomenological description of yield stress as a function of temperature and strain rate, but it cannot be related to physical processes at a molecular scale. The equation can be used to predict ductile to brittle transition temperature used at high strain rates for impact property. Below is the general form of the equation:

\[
k = \frac{k_b T}{\hbar} e^{-\frac{\Delta G}{k T}}
\]  

where $\Delta G$ is Gibbs energy of activation, $k_b$ is Boltzmann’s constant, $h$ is Planck’s constant, and $k$ is the reaction rate. This equation describes the variance of the rate of chemical reaction with temperature.

Robertson’s theory is based on a statistical distribution of stiff and flexible bonds. The applied stress improves the density of flexible bonds up to a critical value where the material becomes similar to a liquid. Robertson’s theory fits experimental data for both thermoplastics and thermosets [60].
In Argon’s theory, yielding is produced by local molecular kinks at the nanoscale level. The formation of a kink pair is modeled as a wedge declination. Permanent yielding is achieved when surrounding molecules perform a similar process, which can relieve the initial kink pair’s local stored elastic energy. A good correlation has been reported for epoxies and bismaleimide / unsaturated polyester blends [54]. The model provides some molecular basis to the microscopic yielding process.

In Bowden’s theory, it is assumed that yielding begins with a thermally activated disk-shape shear region, which grows inside as the stress increased. The analytical treatment is based on dislocation theory using the Burger’s vector. Yield is assumed to occur at a critical energy value produced from dislocating loops; the direction of these dislocated loops is defined using the burgers vector. The agreement between theory and experiment is well-matched for epoxy networks over a large range of temperatures, especially near the glass transition temperature [54].

The thickness of epoxy applied for certain applications is greater than in a testing environment. Traditional modeling of any adhesive in ABAQUS will, by default be assumed negligibly thin. Rather, experiments have shown that as the thickness is increased, the effect of ductility plays a larger role. [61]

2.4.3 Adhesive Delamination Modeling

In one literature study, three-dimensional non-linear finite element analyses of laminated fiberglass reinforced plastic composite double lap joints were carried out to
study the onset and growth of adhesion failure and delamination induced damages [62]. Out-of-plane stress distributions were evaluated at different interfacial surfaces of the joint. The inter-laminate stresses between the first and second plies of the main adherend adjacent to the adhesive layer within the overlap region were evaluated for failure analyses. This was performed to identify the critical locations for the onset of the adhesion failure and the delamination induced damage. The individual modes of strain energy release rates along the damage fronts were evaluated using modified crack closure integral technique for varied embedded lengths of adhesion failure and delamination induced damage. Some of the conclusions, based on the results and observations on the variations of the different components of the strain energy release rates, included that the out-of-plane stress components were on the interfacial surface between the main adherend and the adhesive layer of the double lap joint. The adhesion failure initiated from the stress singularity points at the edges of the double lap joint, i.e. from the overlap end of the joint. Adhesion failure propagation was due to mixed mode strain energy release rate whereas the delamination damage propagates primarily due to mode I.

In another study, the computational methods using a finite element approach were used for predicting delamination growth. In the technique, the delamination profile was sought so that the modal components of the energy release rate (G) satisfy a growth criterion along the advancing delamination front for a given applied load or displacement. [63] The G values in the elements along the delamination front were calculated using the virtual crack closure method. An automatic mesh moving algorithm was used to update the delamination position in the finite element model.
Overall, the re-meshing strategies provided an accurate representation of the delamination front but were relatively computationally inefficient. The fixed mesh strategy provided a less accurate representation of the delamination front but could be made very computationally efficient. The authors are investigating which of these strategies represents the best practical compromise for the prediction of delamination growth.

Lastly, a new approach was developed using damage material model and continuum elements in FEM to simulate damage initiation and propagation for delamination in fiber reinforced polymers (FRP) and crack growth in the adhesive layer. [64] The damage evolution law was based on the cohesive zone model; and the damage onset criterion considers the effect of in-plane normal stress on the damage development. The new approach has a major advantage over the existing interface element-based approach in that the former can easily adopt any stress or strain-based damage initiation criteria. The paper proposed a new damage initiation criterion that considers both shear and hydrostatic stresses for the damage initiation. Results showed that with a simple bi-linear function for the damage evolution, the new damage material model could accurately predict the delamination development in either mode I or mode II fracture of FRP. The new model was firstly verified using benchmark problems such as the double cantilever beam, the end-notched flexure and the ELS tests, by comparing the simulation results with the analytical solutions. The new model was then verified using the delamination development in the fiber reinforced polymer. Beam specimens did not contain any pre-crack. The study shows that the new approach successfully predicts the
location for the delamination initiation and the final delamination size with good accuracy.

### 2.4.4 Traction vs Separation Approach

Modern approaches to FEM modeling of adhesive utilize the cohesive zone method (CZM). Unlike stress intensity factor or linear elastic fracture mechanics (LEFM), which is based on fracture energy approach, cohesive zone modeling is purely phenomenological. This is because the separation of the involved surfaces in the crack takes place across an extended crack tip, or cohesive zone, and is resisted by cohesive tractions. Barenblatt [65] and Dugdale [66] were the first to represent nonlinear processes located at the front of a pre-existing crack.

The major advantages of CZM include: its ability to predict behavior of an cracked structures (including situations with blunt notches), a less-constrained size of non-linear zone when compared to other dimensions in the cracked geometry, and no need for an initial crack (as opposed to LEFM).

One of the fundamental aspects in cohesive zone modeling is the definition of the traction-separation relationship across fracture surfaces, which approximates the nonlinear fracture process. Cohesive interactions are generally regarded as a function of displacement jump (or separation). Just as stress and strain are energy conjugate, traction and separation are work conjugate. Traction and separation are also used because it is impossible to determine strain in two separated surfaces.
Figure 25 depicts a classic, linear traction vs separation curve. The initial linear portion is similar to that in metals, i.e. when metals undergo elastic deformation. $K_{\text{eff}}$ represents the bulk elastic modulus divided by thickness, $t$. At a certain distance, $\delta_o$, maximum traction is reached. At this point, material degradation is initiated. The cohesive element begins to lose stiffness and the damage propagates. At a final separation value, $\delta_f$, the material has failed and the cohesive element is deleted.

There are several possibilities for defining the damage initiation and failure. Damage initiation can be defined either as a function of separation or a function of
maximum nominal stresses [68]. Failure is defined as a function of fracture energy release rates, $G_I$, $G_{II}$, and $G_{III}$. The fracture energy release rate is the area under the traction separation curve as denoted in Figure 26. Depending on the elastic and damage initiation criterion, the downwards slope of the traction separation plot is determined by $G$.

Traction vs separation law is used in modeling indiscernibly thin layers of adhesive. Above a certain thickness, the material may begin to display plastic-like behavior. The linear traction vs separation law cannot account for the non-linearity. One approach to counteract this is to use the continuum approach. Another alternative is to use a non-linear traction vs separation law, which is provided by LS-Dyna but is not available for use in ABAQUS.

Figure 26- Normalized traction-separation law in LS-Dyna (* MAT_186)
Figure 26 depicts the non-linear traction vs separation curve provided available as material card *MAT_186 as provided by LS-Dyna [69]. This non-linear behavior is because of the trilinear traction vs separation law:

\[
t(\lambda) = \begin{cases} 
\sigma_{\text{max}} \frac{\lambda}{\Lambda_1/\Lambda_{\text{fail}}} & \lambda < \frac{\Lambda_1}{\Lambda_{\text{fail}}} \\
\sigma_{\text{max}}, & \frac{\Lambda_1}{\Lambda_{\text{fail}}} < \lambda < \frac{\Lambda_2}{\Lambda_{\text{fail}}} \\
\sigma_{\text{max}} \frac{1 - \lambda}{1 - \Lambda_2/\Lambda_{\text{fail}}}, & \frac{\Lambda_2}{\Lambda_{\text{fail}}} < \lambda < 1
\end{cases}
\]

\( t \) represents traction, \( \lambda \) is a dimensionless separation measure which consists of the magnitude of displacement interactions for each of the three directions. \( \Lambda_1 \) is the scaled distance to peak traction, \( \Lambda_2 \) is the scaled distance to beginning of softening, and \( \Lambda_{\text{fail}} \) is the scaled distance for failure. \( \sigma_{\text{max}} \) is the maximum stress. Using this formulation of traction, a matrix to solve traction vs separation is defined as:

\[
\begin{bmatrix}
t_1 \\
t_2 \\
t_3
\end{bmatrix} = \frac{t(\lambda)}{\lambda} \begin{bmatrix}
\frac{NLS}{TLS^2} & 0 & 0 \\
0 & \frac{NLS}{TLS^2} & 0 \\
0 & 0 & 1
\end{bmatrix} \begin{bmatrix}
\delta_1 \\
\delta_2 \\
\delta_3
\end{bmatrix}
\]

where NLS and TLS is the maximum separation length scale of the normal and tangential direction respectively.

Ji et al. studied the behavior of adhesive joint failure under flap-wise loading for a wind turbine blade. The cohesive zone model approach using traction-separation was used to predict damage initiation and propagation of the skin-web adhesive joint. The results indicated that stress concentration occurs in adhesive joint and shear stress is
mainly responsible for debonding. Debonding was initiated in the edge of the adhesive bond line prior to reaching the extreme design load. Damage was propagated in the direction of the blade tip along the adhesive-web interface as the load increased. Damage propagation led to the redistribution of stresses. Ji concluded that the initial failure mechanism was skin debonding from the adhesive joint, which led to early failure of the blade before reaching ultimate design load [70].

Neto et al. characterized the failure process and strength of adhesive joints with composites, bonded with different adhesives and from short to long overlaps, with validation of different FEM models [71]. It was concluded that for single lap joints with composite bonded to an adhesive (SikaForce 7888), the failure load increased as the overlap increased and the failure was cohesive in the adhesive for all overlaps. In the case of brittle adhesive (AV 138), as the overlap increased, a plateau was reached. An overlap of 30 mm observed inter-laminate failure which was dictated by the composite.

Numerical methods such as FEM modeling were used to predict the strength of the joints. For both the ductile and brittle adhesive, Neto used a global yield criterion for damage initiation in the traction-separation response. These numerical methods yielded satisfactory values for the brittle adhesive, but the ductile behavior could not be captured. This was due to the triangular structure used rather using a trapezoidal shape.

Sandowski et al. conducted an analysis of damage and failure behavior of a hybrid joint consisting of three aluminum plates, polyurethane adhesive, which were reinforced by 5 rivets [72]. The FEM tool ABAQUS was used to simulate tensile strengths of the joint; this included modeling of the aluminum plates, cohesive layer and adhesive. Mass
scaling and half-symmetry were utilized for computational efficiency in the explicit analysis. The rivets and plates experienced a large degree of plastic deformations, which where modeled using Huber-von Mises yield criterion with isotropic hardening. A displacement-based damage parameter for traction-separation law was used. Three kinds of joints were produced: a rivet-only joint, an adhesive-only joint, and a hybrid joint containing both rivets and adhesive.

The results of this study found that the hybrid and bonded joint had much better performance in comparison to only rivet joint or only bonded joint. The tensile strength of the hybrid joint is about 11% higher than analogous bonded joint and about 130% higher than the simple joint with five rivets. Additional reinforcement by the five rivets increased energy absorption by 64% during the failure process in comparison to the adhesive joint. The stiffening effects of the hybrid joint led to higher reliability and durability of the structural system. Overall there was a good consistence between numerical model and experiment.

Rudawska tested and modeled adhesive joint strength of hybrid assemblies consisting of different materials and joints of the same materials [73]. A special numerical model of a single-lap adhesive joint was created based on structural mesh of hexagonal type elements. Boundary conditions were defined by restricting the translational degree of the node freedom located on the front surface of the upper sheet. The only load in the application was the displacement of the front surface on the lower sheet. Traction vs separation law was utilized to allow consideration of both normal interactions (tearing) and tangential interactions. Rudawska a method of adhesive joint
modeling which allowed for visualization of adhesive joint deformation for both the
connected materials as well as the adhesive joints with a load increase and adhesive joint
failure. The comparison of the numerically obtained and experimentally obtained
destructive force reveals that for a majority of tested adhesive joint types, both agreed.
This verified correct behavior of the model as well as the fact that numerical analysis can
be widely used for designing adhesive joints.
Chapter 3: Objectives and Approaches

The goal of this research is to develop an approach for predicting and modeling adhesive delamination and failure due to thermo-mechanically induced buckling between aluminum and steel adherends. The desired outcome is to establish a general application of thermal distortion prediction for different types of geometries which include physical attributes and interactions. The specific research tasks and approaches used are listed in greater detail as follows:

3.1 Modeling of Aluminum/Steel Structures and their Resulting Distortion

A bimetallic structure will be modeled using ABAQUS implicit. The geometry will be two plates consisting of AL6061-T6 on top and DP 590 on the bottom, fastened together using four self-pierced rivets in each corner made of tool steel. Quarter symmetry will be used as well as fixed node in the bottom corner of the structure to prevent rigid body motion. An imperfection as well as positive pressure will be incorporated into the geometry to resemble the initial profile deformed from the riveting. A linear temperature profile will be applied from room temperature to 180°C uniformly.
within the assembly. Stress, plastic and elastic strain profiles will be evaluated to determine the root cause of permanent thermal deformation. The side profile of the specimen will be used to measure vertical deflection, and then compare to existing experimental results available in the literature.

3.2 Modeling of Cured Adhesive Delamination

Cohesive elements, consisting of two faces at a set distance apart, will be incorporated into the thermal buckling model as a single layer of elements sandwiched between the aluminum and steel. The cohesive elements will be defined by the tractions vs separation law for element damage initiation, damage evolution, and failure. Damage initiation will be a function of maximum nominal stress, while evolution and failure will depend on the fracture energy release rate. The STATUS flag will remove the failed elements from the contour plot, allowing for an analysis of remaining adhesive. Kinematic coupling between a reference node and the pulling surface will provide force-displacement data to compare to experimental results. These results will be normalized to protect confidentiality with the supplying companies.

3.3 Fracture Toughness Testing of Adhesive

Mode I fracture energy release rate, $G_I$, will be determined following ASTM D3433-99. Tapered double cantilever beams will be used as adherends for the uncured
epoxy. The epoxy will be deposited onto the beams and slimmed 250 μm bead will be dispersed onto the surface. Another beam will be applied to the top and excess adhesive will be removed. Specimens will be clamped together and placed in an oven above the epoxy’s glass transition temperature. Once cured then cooled, the specimens will be tensile tested. Using a camera to video record the events, $G_I$ will be determined as a function of crack length before onset of rapid propagation, and its corresponding load. This information will be used in determining damage evolution and failure for the cohesive element based thermal buckling model.
Chapter 4: Modeling of Thermo-Mechanical Buckling

4.1 Materials

The experimental data used in this study was taken from an existing literature study by Zhu et al. [3] In their experiment, two sheets (one aluminum and other one steel) were joined together with either bolts or self-piercing rivets. Thermal buckling both with and without adhesive were conducted and compared.

AL6061-T6 and DP 590 steel were used as the bimetallic components. H13 tool steel was used for the rivet material. Table 6 defines the material properties used in FEA modeling:

<table>
<thead>
<tr>
<th></th>
<th>E (MPa)</th>
<th>Poisson’s ratio</th>
<th>YS (MPa)</th>
<th>CTE (1/°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>AL6061-T6</td>
<td>68900</td>
<td>0.33</td>
<td>276</td>
<td>2.36E-05</td>
</tr>
<tr>
<td>DP 590</td>
<td>219000</td>
<td>0.3</td>
<td>316</td>
<td>1.58E-05</td>
</tr>
<tr>
<td>H13</td>
<td>188000</td>
<td>0.3</td>
<td>1450</td>
<td>1.10E-05</td>
</tr>
</tbody>
</table>

Table 6 - Mechanical properties used in the FEA model.

4.2 Approach

4.2.1 Oven Experiment
Details of the experiment used in the study by Zhu et al. are available in Ref [3]. For completeness, salient features of the experiment are summarized in the following.

Adherends were mounted on the riveting equipment, with steel adherend on the bottom. Holes with a diameter of 5mm were drilled in the corners of the adherends. Screws were installed with a pre-tightening torque of 59 N m.

Because specimens underwent temperatures of up to 180°C, conventional methods such as strain gauges and displacement sensors could not be used for measuring the transient distortion of the specimens in the oven. Instead, a Canon 500D camera with a picture size of 4752 x 3168 pixels as shown in Figure 27 was used. Resolution was determined to be within 0.5 mm.

Since there was no window in the oven used for the experiment, the oven door was left open when the measurement was taken, and the sample was placed on a brick with high specific heat. Thermocouples were connected to an end of the specimen to record the changes in temperature. The variation turned out to be less than 0.5°C while photographs were being taken.
17 evenly spaced locations were selected to record distortion values. This was along the side profile of the sheets in the longitudinal direction (defined as the x axis). The vertical deflection recorded was on the y axis. Temperatures were selected to take photographs at different stages of heating, holding and cooling. To ensure replicability, three specimens were made and two measurements were taken at each specified temperature.

4.2.2 FEM Model
4.2.2.1 Model Assumptions

Assumptions made include the following:

• No temperature dependent properties were implemented for the materials

• Aluminum mechanical properties do not change with time
  
  o A change in properties due to precipitation hardening could be expected

• The act of riveting was not modeled. Instead, residual stresses were applied to the rivet and surrounding area

• Friction was defined through tangential behavior with penalty and a friction coefficient of 0.12
  
  o This behavior permits relative motion of the surfaces (an “elastic slip”)
  
  o While the surfaces are sticking, the magnitude of sliding is limited to elastic slip.

• Uniform temperature profile for all parts
  
  o No heat conduction defined; temperature at the surface of the metals is always the same as the interior

4.2.2.2 Model Approach

The FEM analysis using ABAQUS standard (an implicit code) consisted of two sheets, 1 mm thick aluminum on top and 1.8 mm thick steel, bounded together by a cylinder representing a rivet. To cut down the computational time, a quarter symmetry
was used in the z and x directions (refer to Figure 28). At the bottom quarter of the steel sheet, close to the rivet, a fixed node constraint was implemented to prevent rigid body movement. The rivet was simplified as a cylinder of radius 7.8 mm and vertical height of 2.2 mm, which is the combined thicknesses of the adherends. The rivet shape was chosen to be cylindrical instead of the original “tooth” shape for simplicity. The assumption was made that extra rivet material would play a secondary role in the desired distortion effects.

Figure 28 - Model Setup.
A tie constraint was used between the outer surface of the rivet and the inner surface of the holes in the sheets. This allowed for the simulation of a bonded joint, with zero degrees of freedom between the tied surfaces. Surface to surface contact was implemented between the aluminum and steel. The contact interaction corresponding to this contact incorporated normal behavior with exponential pressure overclosure. Specially, once the two sheets come into a distance of 0 mm, 50 MPa of negative pressure acts to repel the surfaces. As the surfaces move away, the pressure lessens exponentially until a clearance of 0.1 mm is reached. This specific condition prevented convergence errors. Tangential behavior with a friction coefficient of 0.12 was also incorporated into the contact definition. The diameter of the top portion of the rivet was 7.8 mm as can be seen in Figure 29.

Figure 29 - Rivet geometry used in experiment by Zhu et al. [3]
A linear buckling analysis was conducted before running the full model. This analysis provided a definite geometric profile for the configuration, which is required for consistent results; particularly for the simple geometries (flat sheets). Three modes of buckling were outputted, portraying possible physical configurations of buckling. Literature has shown that a combination of these modes as the imperfection leads to the most accurate depiction [74]. A .fil binary file was produced and included in the primary analysis where a combination of the first two modes were utilized; a magnitude of 0.8 for the first mode and 1.35 for the second.

The first step of the analysis involved adding pressure to the rivet and within a radial distance around the rivet, refer to Figure 28. The initial deflection profile of the experiment indicated a residual distortion of the aluminum and steel after riveting, as shown in Figure 30. This is due to the application of the rivet. Consequentially, after riveting there remained residual stress in and around the region of the rivet. This has been studied by Bouchard et al. and others. [75] [76] [77] It has been observed that region around the rivet in the substrates contains a radius of residual stress up to 300 MPa (Figure 31). For this reason, the first step to reproduce the physical scenario was to apply pre-stress to the area that was affected by the rivet. As a first approximation, 170 MPa of pressure was applied to the rivet and 75 MPa was applied to a radius of 14 mm starting from the outer perimeter. This allowed the structure to match the initial distortion of the experiment.
The heating process was the next step in the analysis. In this step, the entire structure was uniformly heated throughout, defined by a linear heating curve. The experiment used a close to linear heating procedure as well, although a difference
between the analysis and experiment is that the experiment held the specimens at 180°C while the analysis immediately initiated the cooling stage. Because the analysis followed a uniform heating condition, there would be no change in thermal expansion, and thus no difference in the distortion due to the hold at peak temperature. The analysis was cooled linearly and the experiment close to linear. A comparison in the cycles can be seen in Figure 32.

![Figure 32](image_url)  
**Figure 32 - Experimental thermal cycle (left) [3]. Model thermal cycle (right).**

### 4.3 Results and Discussion

Once analyses were completed for the buckling sheets, deflection at each node was recorded and plotted against the node’s corresponding x position. Figure 33 is a deflection plot of the middle of a quarter of the buckled structure at 180°C (i.e., peak
temperature). There is a corresponding contour plot below the deflection plot showing the orientation of the structure. The different coloration of the deflection plot corresponds to different deflection amounts.

The model, as seen as the solid lines, either showed a slight over-prediction or under-prediction when compared to the experiment, although it generally follows the deflection pattern of the experiment. Most of the nodes of the aluminum model tended to slightly under-predict deflection except near the very middle of the structure where it slightly exceeded it. On the other hand, steel consistently deflected higher than its experimental counterpart and appeared to be the slightest bit more concave. Although there were slight variations, the overall behavior was well captured by the model.
Figure 32 consists of two up-close contour plots of the rivet and neighboring regions of aluminum. The post-analysis of the aluminum was the primary focus of the results as the aluminum sheet was subjected to a compressive load in the longitudinal (x) direction, which resulted in a majority of its final distortion and residual stress. Any other region not surrounding the rivet was not of interest because it displayed uniform stress and strain values and had no physical interactions.

The bulk of the material did not experience residual stress. This can be explained through a simple calculation using aluminum’s coefficient of thermal expansion. As an
upper estimate by assuming that the steel does not expand at all, multiplying the CTE by the original length and temperature difference provides the change in length. A brief calculation of the change in length using the coefficient of thermal expansion shows that the aluminum would deflect 0.391 mm past the steel. When used to calculate strain, stress in the aluminum is 134 MPa at 180°C, below the yield stress.

In Figure 34, the elastic strain (left) revealed that certain regions of the aluminum were in compression around the rivet area. The stress in the longitudinal direction, S11 experienced both compression and tension in the aluminum around the rivet area. The crescent shaped blue region in Figure 34 was caused when the aluminum buckled, pushing in the surfaces directly in front of and behind the rivet. The maximum stress experienced locally around the rivet was significantly over the yield stress of AL6061 which in the analysis was defined as 276 MPa (Table 6). The high hardness of the rivet proved to be an obstacle for the aluminum while expanding, which caused it to fold into itself. The rivet undergoes shear due to the two opposing forces of the aluminum in compression and the steel in tension.
Figure 34 - Contour plots of the rivet and aluminum at 180 degrees. Linear elastic strain in the x direction (left) and stress in x direction (right).

There exists a physical explanation for what causes the aluminum to be in compression and steel to be in tension. The CTE of aluminum is almost twice that of steel, so with each increase in degree Celsius, aluminum will expand almost twice as much as steel will. This causes aluminum to push axially on the steel, leaving aluminum in compression and steel in tension. Figure 35 depicts the bending theory behind the direction that aluminum and steel both choose to buckle. When undergoing bending, aluminum’s neutral axis is within the lower region, while the load applied is compression. This induces a concave, upward bending. The steel also bends upwards because it’s neutral axis is closest to the top, where it undergoes tension. The combination of neutral axes and forces applied induces both materials to bend in the same direction.
The deflection of the cooled structure calculated by the analysis also follows that of the experiment. The experiment shows that the aluminum is slightly higher near the middle of the structure when compared to the analysis. The experimental data for the steel appears to be inconsistent, as it does not have a smooth, arch-like appearance. This is likely due to limited resolution of the camera to capture very small deflections.
As for the riveted regions, no residual stress occurred in the steel. Figure 37 depicts the equivalent plastic strain and axial stress component in the aluminum sheet around the rivet. The equivalent plastic strain in the rivet region reached up to 0.7%. Although equivalent strain is always a positive number, the region is in tension as depicted in the right picture in Figure 37. The maximum stress noted on the aluminum was 173 MPa. While at peak temperature this region was in compression, inducing negative plastic strain, therefore when the structure returned to room temperature, the release of linear elastic strain caused the structure to go into tension, creating a residual gap. This interaction is what prevents the structure from returning to its original position.
4.3.1 Sheet Thickness Analysis

A plate thickness analysis was conducted by varying the thickness of both the aluminum and steel sheets. Three cases were examined and compared to experimental results, as seen in Table 7. All other conditions remained the same. The objective of varying thicknesses was to find a correlation between thickness of sheets and final degree of deflection. Measurements were taken from the center bottom of the aluminum to the center top of the steel, mentioned as “gaps.” A physical explanation is then derived from the results.
<table>
<thead>
<tr>
<th>Thickness</th>
<th>Aluminum (mm)</th>
<th>Steel (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Case 1</td>
<td>1</td>
<td>1.8</td>
</tr>
<tr>
<td>Case 2</td>
<td>1.6</td>
<td>1.2</td>
</tr>
<tr>
<td>Case 3</td>
<td>1</td>
<td>1.2</td>
</tr>
</tbody>
</table>

Table 7 - Thicknesses used in analysis.

The results depicted in Figure 38 confirm the accuracy of the analysis. Case 1, using the thicknesses from the original model matched the experimental data the closest, while Case 2 and 3 had some difference in gap. This may be because, for Case 3, the size of the cylindrical rivet in the analysis changed with the total thickness, while in the experiment, the rivet remained the same size. A trend appears that Cases 2 and 3 have a smaller final gap than Case 1 (Figure 38). For Case 2, this is because the steel is thinner, allowing the aluminum to apply more tension to the steel, thus extending it more. In Case 3, the combined thickness was decreased, which reduced the moment of inertia of the structure, allowing for less compression around the riveted region to occur.
The results observed in Figure 38 can also be explained using the section modulus which is used to determine the maximum stress experienced in a structure. The general formulation for the section modulus is seen below:

$$ S = \frac{I}{y} $$

(6)

where $I$ is the moment of inertia and $y$ is the maximum distance from the centroid. For a rectangular cross-section, the section modulus is specifically:
\[ S = \frac{bh^2}{6} \]  

where \( b \) is the base length and \( h \) is the height. As the total thickness increases, the section modulus increases squared, this is equivalent to an increase in inertia. The section modulus provides an explanation using physics for why the combined thickness of the sections experienced larger gaps.

4.3.2 Fastener Pitch Analysis

A fastener pitch analysis was conducted using 5 cases: original length (200 mm), half length (100 mm), third length (66.67 mm), quarter length (50 mm), and double length (400 mm). Figure 39 is a plot of aluminum deflection at 180°C. Doubling the pitch can increase the deflection squared, while decreasing the pitch may not make a significant difference, especially for the smaller fractions.
Figure 39 - Aluminum deflection comparing pitch lengths at 180 °C.

The steel deflection profile at 180°C (Figure 40) followed the same pattern of deflection as the aluminum. Noting that the doubled length deflection had squared value compared to the original length.
Figure 40 - Steel deflection comparing pitch lengths at 180 °C.

This effect can be explained by concepts in a classical buckling analysis. The equation for the maximum critical force that a beam can withstand before buckling provides insights to this phenomenon. For long, slender columns, Euler’s formula states:

\[
F = \frac{\pi^2 EI}{(KL)^2}
\]  

where \( F \) is the maximum or critical force for a vertical load on a column, \( E \) is the modulus of elasticity, \( I \) is the moment of inertia, \( L \) is the unsupported length of the column, and \( K \) is the effective length factor. In this case, both ends would be fixed, leading to a \( K \) value of 0.5. Notice that critical force is proportional to the inverse square
of the unsupported length. In other words, the longer the pitch, the less force it requires to induce buckling. This is consistent with the squared trend in the deflection profiles.

An equivalent explanation would be using the slenderness ratio:

\[
\frac{L}{r}
\]  

(9)

where \(L\) is the length and \(r\) is the radius of gyration. A higher slenderness ratio will cause a lower amount of stress to buckle and a lower slenderness ratio results in a higher critical stress. As the plates are elongated and unaltered in thickness, this increases their slenderness ratio and thusly reduce the critical threshold for buckling.

4.3.4 Channel Buckling Model

Another case involving thermal-induced buckling involves a more complex geometry. Details on the profile of this beam as well as details of changed geometry cannot be stated due to confidentiality with respect to the collaborating participants.

The thermal load profile is a duplicate of the simple sheet model described in the previous section. Quarter symmetry is used, where each corner location is tied together using a bolt. A corner node of the quarter model is restricted in movement in all directions, preventing rigid body movement.

In this experiment, the top flange region of the beam seen in Figure 41 was reduced and the resulting deflection was observed. The experiment only involved one case. This was to validate the analysis. Reducing the height of the top flange caused
deflection to increase. A physical explanation for this involves the section modulus. As the flange height was reduced, the moment of inertia for the cross section was reduced, which, reduces the section modulus.

Figure 41 - Deflection plot of complex beam comparing different vertical flange heights.

4.4 Summary and Conclusions

A FEA analysis was conducted to study the thermal buckling of bi-metallic structure without adhesive. The initial analysis involved linear buckling to obtain buckling modes. A combination of these modes was used as an initial imperfection in the main analysis. The analysis consisted of three stages: rivet stress application, heating,
then cooling. The deflection profile for heating and cooling were compared with those from the experiment in an existing literature study. Both experiment and FEA analysis were consistent for heating and cooling.

The sheet thickness sensitivity study followed the same trend as the experiment. Gaps between the bottom of the aluminum plate and the top of the steel plate in the midsection were measured and compared. Gap trends indicated a large role in both stiffness of the material and section modulus. The thicker the steel section was, the more limited in movement the aluminum was due to its overall stiffness. When the total thickness of the material was reduced, the reduced section modulus lead to lower deflections and smaller final gaps.

Altering the pitch of the structure revealed a consistent deflection pattern. Increasing the length would lead to an increase of deflection squared. This phenomenon can be explained by the Euler theory which states that the critical force for bending to occur is directly proportional to the inverse of the length squared. This is especially true for simple geometries such as flat plates, which are closer in resemblance to basic beams.

For more complex geometries such as the channel buckling model, an alteration in feature of the cross-section play a significant role in the deflection. Reducing the height of a vertical flange increased the deflection due to the decrease in section modulus. The section modulus was reduced by decreasing the moment of inertia, allowing greater ease of vertical movement.
Chapter 5: Modeling of Cohesive Failure

5.1 Organization

This chapter consists of three sections: fracture toughness testing, lap shear delamination modeling, and flat plates buckling delamination modeling. Both delamination models require fracture toughness for modes I, II, and II for cohesive elements to experience damage and failure. Procedure for fracture toughness testing is first described before moving onto the modeling discussion. The lap shear and buckling plates model are two separate analyses. The lap shear scenario replicates experimental tests of two cured adhesives conducted by ORNL while the buckling plates model replicates a literature paper. The primary objective of these two analyses is to accurately portray cured adhesive delamination.

5.2 Materials

Adhesive A and adhesive B were provided by two separate suppliers and are compositionally different. These polymers are thermosetting epoxy adhesives provided in a viscous liquidus state, commonly known as B-stage. When they are cured, they become
hard and gain their well-known strength as structural epoxies. The cohesive zone method in Abaqus requires the following groups of mechanical properties to be defined: elastic moduli (E), maximum failure stress ($\sigma_i$), fracture toughness (G); all three groups are defined with respect to three orthogonal directions (1 normal and 2 shear directions). Additional detail of the cohesive zone method can be found in Chapter 2. As these properties were provided by the adhesive suppliers, they are not listed in this thesis due to confidentiality. The modeling approach and results are provided in detail in the subsequent sections.

5.3 Approach

5.3.1 Fracture Toughness Testing

The fracture toughness especially that for Mode I is a critical parameter for modeling the adhesive damage evolution and eventual failure. It is a complex function of both temperature and curing state. As a first attempt, the Mode I fracture toughness of adhesive at room temperature was tested. The testing approach can be extended to test the fracture toughness as a function of temperature for cured adhesive in the future.

Tapered double cantilever beams (TDCB) were used as the adherends. These cantilever beams were made of Al 6061 T-6. The geometry of the TDCB is shown in Figure 42.
The adherend was prepared by thoroughly cleaning with acetone. The cartridge of adhesive was first placed in the oven at 45°C for 15 minutes to ensure smooth dispensing. Using a caulking gun, the adhesive was dispensed as 3 rows across the flat end of the tapered double cantilever beam. Once adhesive was laid on the beam, a clean razor was used to flatten out the three rows, creating one smooth, uniform layer. 250 um beads were scattered randomly and sparsely over the adhesive by dispensing through an eyedropper tube. These beads were died bright orange to contrast to the blue adhesive. The other adherend was then placed on top of the adhesive and clamped together using two C-Clamps, as shown in Figure 43.
The prepared specimen is then placed into the oven at 180°C for 35 minutes. The oven used was the Lucifer 7GT-K24 furnace. Upon removing from the oven, expelled adhesive around the edges was scraped off. The cantilever beam was left to cool.

To calculation of the fracture energy release rate requires the load as a function of the crack length. To measure the load, the TDCBs were tested using uniaxial tensile testing equipment (Figure 44) with a displacement rate of 2.118 mm/min. The TDCB was fastened through specifically designed metal clamps, as seen in Figure 43. A video was taken using a Nikon D7100 with a frequency of 29 Hz. A verbal signaling at the start of the tensile test ensured the time match-up between the video feed and the load data.
Images were produced at a rate of 3 frames per second using Linux avconv. Using the image time, the corresponding load at the same time was found. The crack length was measured using ImageJ software.

Following ASTM D3433 − 99 standard, the crack length at the beginning of rapid crack propagation and its corresponding load were used to compute the fracture energy release rate $G_{\text{IC}}$.

### 5.3.2 Adhesive Delamination Modeling
5.3.2.1 Model Assumptions

Assumptions made include the following:

- Linear traction-separation damage model for cohesive elements is used, as opposed to a non-linear law.
- Crosshead displacement was plotted against reaction force for the traction-separation lap shear curves. This is not the most accurate method, and adhesive displacement measurements are preferred.
- Elastic modulus, shear modulus, maximum nominal stresses and fracture toughness for all three modes were provided by the company for Adhesive A. Adhesive B provided maximum nominal stress for the normal direction as well as the Young’s Modulus. All other mechanical properties were used as a scaled percentage of Adhesive A’s properties (i.e. if Adhesive A has a mode I fracture toughness of 1 and Adhesive B has a mode I fracture toughness of 0.5, then the model II fracture toughness for adhesive B would be half of the mode II fracture toughness of Adhesive A).

5.3.2.2 Lap Shear Model

A lap shear model was conducted as a validation to experiments with cured epoxies. The geometry and composition can be seen in Figure 45. The aluminum consisted of AL 6061-T6 while the steel consisted of DP1180. Adhesives A and B were
used in two separate analyses. The end surface of the steel was constrained of all degrees of freedom while the aluminum was pulled towards the positive x direction. The aluminum was pulled at a strain rate of 0.0067 s\(^{-1}\). Two types of glass beads were used in the experiments: 500 \(\mu\)m and 300\(\mu\)m beads. Overall there were four analyses, two for each type of adhesive and each type with the two bead sizes.

![Figure 45 - Lap shear model geometry and measurements in mm.](image)

**Cohesive elements following traction-separation law were chosen to represent the cured epoxies.** The adhesive layer was created via partitioning. That is, the entire
configuration was designed as one piece, then partitioned out into the two plates and an adhesive layer. Upon meshing, only one layer of cohesive elements (COH3D8) were needed as by design.

To account for the potential contact after adhesive failure, contact was defined between the entire structure and itself using general contact. Under general contact, the normal behavior of contact interaction was used. This interaction property included “hard” contact, meaning that if the two surfaces are with zero clearance, a large spike in contact pressure will occur to repel the surfaces and such pressure drops to zero once the surfaces are separated. No frictional conditions were used either. This was sufficient when using ABAQUS explicit, since convergence is easier to obtain.

Force displacement curves were obtained through kinematic coupling of the pulling surface with a reference node. Using the reference node, a history output was created for a set containing the reference node, recording the reaction force in the x direction. Using the output data, the force-displacement curve could be plotted.

5.3.2.3 Paint Bake Model with Adhesive

The geometry, symmetry, boundary conditions, contact interactions and thermal load all remained the same from the paint bake model of bi-metallic structure without adhesive described in Chapter 4. The new addition involved adding a layer of adhesive sandwiched between the Al and steel sheets. The classic approach involves adding a layer of cohesive elements in between the substrates. In the case of this existing experiment by
Zhu et al. [3], the initial state of the adhesive was uncured. As the structure was held at 180° C, the adhesive went through its curing cycle. Upon cooling, the adhesive was cured in the buckled position which locked its position and stress state. This can be observed through the experimental deflection plots when comparing the cooled structure using no adhesive with one using adhesive, as shown in Figure 46.

![Figure 46 - Cooled experimental deflection plots. With no adhesive (top) and with adhesive (bottom). Pictures from the existing experiment by Zhu et al. [3]](image)

Cohesive elements are designed to fail at given loads or at given strains. They could not be used to capture the cured interaction of the adhesive with adherends. This is because once flagged for failure, they would be permanently removed. To account for
this effect, a hybrid element was created involving two layers of elements. One set of continuum elements and one of cohesive elements, as shown in Figure 47.

Using this hybrid design, the configuration would still be able to experience delamination while conserving a remaining layer as the “cured layer.” The cohesive elements resembled a thin strip with a thickness of 100 µm. The cohesive elements had weak properties corresponding to uncured adhesive. The continuum elements on the other hand comprised the bulk of the thickness. Maximizing the thickness of the continuum
elements helped minimize computational time. As shown in Figure 47, the continuum elements were chosen to be attached to the aluminum sheet. This choice was made based on the experimental observation that the adhesive stuck much more to the aluminum plate after failure (expected due to better adhesion to aluminum). The continuum elements were given cured epoxy mechanical properties, which were constant throughout the analysis.

5.4 Results and Discussion

5.4.1 Fracture Toughness Testing

It took many trials to achieve a fully cured state on the tapered double cantilever beams. It was concluded that curing times, at minimum, had to exceed thirty minutes in the oven. This is due to the large size in the cantilever beams as well as the large size of the C-clamps which held them together. Figure 48 shows two beams, one that is partially cured from a 25-minute cure, and one that is fully cured from a 35-minute cure.
Figure 48 - Cured (left) versus uncured (right) tapered double cantilever beams.

Figure 49 displays the load displacement curves of 4 tests conducted. Each test proved to consistent except for test 4, which failed at half the load as the other samples. It was concluded that the sample had insufficient curing, and thus the cure time was increased from 35 minutes to 60 minutes.
Figure 49- Load displacement curves from fracture toughness testing.

The mode I fracture energy release rate was determined using the crack length right before rapid crack propagation. This was determined qualitatively through video footage of the test. The corresponding load was used in the calculation as shown below:

\[ G_{ic} = \frac{[4L^2](m)}{[EB^2]} \]

where

\[ m = \frac{3a^2}{h^3} + \frac{1}{h} \]
In the above equation, \( L \) is the corresponding load in Newtons, \( h \) is the thickness of the adherend normal to the plane of bonding, \( a \) is the crack length, \( E \) is the elastic modulus, and \( B \) is the thickness of the cantilever beam.

<table>
<thead>
<tr>
<th></th>
<th>Case 1</th>
<th>Case 2</th>
<th>Case 3</th>
<th>Case 4</th>
<th>Average</th>
</tr>
</thead>
<tbody>
<tr>
<td>( G_i )</td>
<td>1.38</td>
<td>1.96</td>
<td>1.77</td>
<td>0.84</td>
<td>1.49</td>
</tr>
</tbody>
</table>

Table 8- Test results for \( G_i \).

Figure 50 provides the results of load, the slope \( m \), and numerator term of the \( G_i \) equation. It is seen that for each case, the \( m \) value or load value may be greater or less than the other. Since \( E \) and \( B \) are held constant, these two parameters determine the difference in \( G_i \). The green line representing the numerator indicates that these values naturally balance each other out, being within a range of \( \pm 0.2 \text{ N}^2\text{m}^{-1} \) (except for case 4, which was determined to only be partially cured).
Since the critical crack values, $a$, where chosen by hand, a sensitivity experiment was conducted on case 1. This study took the video footage of the mechanical test and correlated each crack length with corresponding load. From there the fracture energy release rate was calculated and plotted versus the crack length, as shown in Figure 51. This sensitivity study proved consistent in the energy release rate, no matter what crack length was chosen.
Figure 51 - Crack length sensitivity analysis of Case 1.

This can be explained physically using the taper in the beam. Given a point further on any rod or beam, the bending moment will increase:

\[ M = r \times F \]

where \( r \) is the distance from the loading point, and \( F \) is the force. The bending stress which occurs in the beam is defined as:

\[ \sigma = \frac{M}{S} \]
where $S$ is the section modulus. $S$ is directly proportional with the moment of inertia, $I$.

Having a combination of tapered beams behind the crack tip will increase the moment of inertia by increasing the height, $h$, as it becomes a function of location:

$$I(x) = \frac{bh(x)^3}{12}$$

So as $M$ increases from an increase in $r$, $S$ will increase from the increasing inertia.

Therefore, the force at any location will be the same.

5.4.2 Modeling of Cured Adhesive Delamination in Lap Shear Testing

When compared to experimental load-displacement curves, adhesive A had better replicability than adhesive B (Figure 52). Each adhesive was uniquely formulated and provided by separate suppliers. Adhesive A displayed strictly linear behavior while adhesive B quickly diverged into non-linearity. Adhesive B clearly contained additive elastizers to increase toughness.
Figure 52 - Force displacement plots of model versus experimental results. Adhesive A (left) and adhesive B (right).

The deformation behavior of Adhesive B was not fully predicted due to this toughness effect. ABAQUS only provides linear traction-separation laws for use in its cohesive elements, while other FEA software such as LS-Dyna are known to incorporate these non-linear material cards. This limitation will be addressed in a future work.

5.4.3 Paint Bake Model with Adhesive

Using the hybrid cohesive-continuum element configuration, the adhesive could successfully delaminate while remaining within the model to further interact with the adherends during the cooling stage. The cohesive skin elements, were given weak properties of uncured epoxy, while the continuum elements were assigned the cured
epoxy mechanical properties. This allowed for freezing of aluminum at higher displacements when compared to the model without adhesive.

The degree of delamination depended on the region of the two sheets. With no local restraint, the center of the plates removed the cohesive elements quite easily, while the pressure of the bolt or rivet acting on the sheets preserved the surrounding cohesive elements. When comparing the model to the experiment, as seen in Figure 53, a local region of the adhesive remains for both. This indicates a degree of physical accuracy for the model.

Figure 53 – Comparison of delamination profile between model (left) and experiment (right) [3]

Work is still being done in the comparison of the experimental deflection profile with the model’s. Temperature depended properties for the continuum and cohesive elements where not incorporated and could play a major role in how deflection occurs. Especially for the continuum elements, which in future work should have low mechanical properties until the cooling stage.
5.5 Summary and Conclusions

Fracture toughness testing was conducted to determine mode I fracture energy release rate. B stage epoxy adhesive was applied to tapered double cantilever beams along with 250μm glass beads for consistent thickness. The beams were held together using C-clamps to cure adhesive in the oven at various times. It was discovered that for the geometry and configuration, a minimum cure time of 35 minutes was needed, but a cure time of at least 60 minutes was essential to ensure full curing consistently. The fracture toughness testing generated consistent results with an average Gi value of 1.45 mJ/mm² at room temperature.

Because the crack length to determine Gi was subjective, a crack length sensitivity study was performed to validate the results. After selecting several random crack lengths and using their corresponding loads to calculate Gi, they were compared to each other by comparing Gi versus crack length. Each value only varied by ±0.2 mJ/mm², demonstrating that the calculated fracture toughness is largely independent of the crack length of choice.

Lap shear experiments using cured adhesive were simulated with ABAQUS FEA analysis. The adhesive was modeled as cohesive elements following linear traction-separation law for damage initiation, damage evolution, and failure. Gi determined from the fracture energy testing was used as a parameter for damage evolution and failure. It was found that the accuracy of the analysis results from linear traction-separation law compared to the experiment depended on the type of adhesive used. The epoxy
formulated for strength had a better match between the modeling results and experimental data. On the other hand, the linear traction-separation law had some difficulty in predicting the non-linear deformation behavior for the epoxy designed for toughness.
Chapter 6: Summary and Conclusions

6.1 Thermo-Mechanical Buckling

- The thermo-mechanical model of Al-steel bi-metallic structure successfully reproduced the physical interactions of the existing oven experiments reported by others in the literature. The vertical deflection profile calculated by the model matches reasonably well the experimental data, both at the 180 °C peak temperature as well as after final cooling.

- Altering the thickness of the aluminum and steel resulted in a significant change of the deflection profiles. The amount of deflection is a result of stiffness and section modulus. The calculated residual gap vs. sheet thickness is consistent with the experimental data.

- Variation in pitch (i.e., spacing between rivets) produced a varied degree of deflection. Increasing the length resulted in increasing the deflection squared. This is partially explained by Euler theory which state that the critical force to induce buckling is inverse squared proportional to the length.

- Occurrence of residual deflection upon cooling was a result of the interaction between the aluminum plate and the steel rivet. Compression to the point of
plastic deformation within a local radius of the rivet causes the aluminum to remain compressed, and then hold itself up in tension upon cooling.

6.2 Fracture Mechanics Testing

- Adhesive fracture mechanics testing following ASTM D3433-99 was successfully carried out to determine mode I fracture energy release rate at room temperature for a fully cured adhesive. The average value of $G_I$ was found to be 1.49 mJ/mm².
- Testing was validated through a crack length sensitivity study. Different crack lengths and corresponding loads were measured and compared. All values before failure of the beam ranged within ±0.2 mJ/mm².

6.3 Adhesive Delamination Modeling

- Cohesive elements were used for modeling adhesive elasticity, damage initiation, damage evolution, and eventual failure. The cohesive elements followed linear traction-separation law, and could match force-displacement curves for experimentally stiff adhesive. Toughened adhesive will require a non-linear traction-separation law, which is not currently available as a built-in option in ABAQUS.
- To capture the interaction between cured adhesive and cooling aluminum, a hybrid element consisting of both cohesive elements and continuum elements was
created. This hybrid element allowed for the capturing of residual stress and halting of downward movement. Cohesive elements close to the rivet did not fail, just as in the experiment.
Chapter 7: Future Work

Similar to metals, adhesives exhibit complex mechanical behaviors that are a strong function of temperature. Additional complexity is that the mechanical properties are also a function of curing state. The present research made a first step toward (1) experimentally testing adhesive fracture toughness, (2) modeling delamination of fully-cured adhesive in lap-shear tensile testing, and (3) modeling thermal buckling without and with adhesive layer in the Al-steel bi-metallic structure. Over the course of this research, the following areas are identified for future work. Addressing these areas will further improve the understanding of the stress and strain evolution in the bi-metallic Al/adhesive/steel structure exposed to a thermal cycle representative of that used in automotive paint bake process.

7.1 LS-Dyna Cohesive Element Delamination Modeling

- Construct a lap shear model in LS-Dyna with cohesive elements using the material card *MAT_186 for general, non-linear traction-separation behavior.

7.2 Lap Shear using DIC
• Run lap shear test using speckled pattern painted on the adhesive. Use in DIC with the virtual extensometer to obtain displacement profile, which is needed for validating the lap-shear model.

7.3 Elevated Temperature Fracture Mechanics Testing

• Securely cover double tapered cantilever beams with silicon heating mats on all sides with the exception of the location containing adhesive. Test for Gi at various temperatures up to 180°C.
• Create a temperature dependent material card with temperature depended fracture toughness properties.
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