UNDERSTANDING THE EFFECT OF RESIDUAL STRESSES AND DEFORMATION ON
THE FATIGUE BEHAVIOR OF PERMANENT FASTENERS

by

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A DISSERTATION

Submitted in partial fulfillment of the requirements
for the degree of Doctor of Philosophy
in the Department of Mechanical Engineering
in the Graduate School of
The University of Alabama

TUSCALOOSA, ALABAMA

2016
ABSTRACT

In this work, the complex relationship between deformation history and mechanical performance of self-pierce riveted (SPR) joints is elucidated. This study focuses on understanding how the material deformation that occurs during the riveting process impacts the quasi-static and fatigue behavior of the joint. Experimental results show that fatigue crack initiation in SPR joints can occur away from the rivet. Numerical simulations predicted the deformation of the riveted aluminum-alloy joint, revealing a strong correlation between residual contact pressure due to the riveting process and fretting induced fatigue crack initiation. Furthermore, the number of cycles to failure was calculated by applying linear elastic fracture mechanics approach, which correlated to the experimental fatigue results and further supported the hypothesis that the deformation induced by the riveting process altered the failure mode under high cycle fatigue. Since residual contact stresses were evaluated and correlated to fretting, a more in-depth analysis was performed. To aid in understanding the role of deformation in the riveting process, residual stresses originating from elastic strains within a magnesium-to-aluminum SPR joint were evaluated using neutron diffraction and X-Ray diffraction (XRD) measurements. Furthermore, micro hardness mapping was performed in order to quantify the plastic deformation on a cross-section of the SPR joint. The experimental characterization of the stress and strain state of the SPR joint were then compared to finite element simulations showing good agreement. After validation, the macro lap-shear mechanical response of the SPR joint was simulated and compared with experimental results. In particular, SPR simulations were carried out in order to assess the effect of including the residual stresses and plastic strains in the
modeling approach. The simulation results reveal that the inclusion of plastic strains is the main driving force for strength and joint stiffness under quasi-static lap-shear loading, while the residual stresses have a negligible effect. Also, the role of residual stresses and plastic strains was investigated on cyclic loading. Results show that including the deformation history captured in the process simulation changes the prediction of the location of crack initiation, resulting in an estimated number of cycles to failure that differs by a factor of two in the high cycle regime.
DEDICATION

To God, my family, fiancé, friends and mentors: thank you for all the incredible support throughout this journey.
LIST OF ABBREVIATIONS AND SYMBOLS

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
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<tbody>
<tr>
<td>a</td>
<td>Crack length</td>
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<tr>
<td>C</td>
<td>Material constant obtained from crack growth experiments</td>
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<tr>
<td>CAE</td>
<td>Computer aided design</td>
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<tr>
<td>d₁</td>
<td>Constant in damage</td>
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<td>d₂</td>
<td>Constant in damage related to triaxiality</td>
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<td>d₃</td>
<td>Constant in damage related to triaxiality</td>
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<tr>
<td>d₄</td>
<td>Constant in damage related to temperature</td>
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<tr>
<td>d₅</td>
<td>Constant in damage related to strain rate</td>
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<td>p</td>
<td>Pressure</td>
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<td>q</td>
<td>Mises stress</td>
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<tr>
<td>E</td>
<td>Elastic modulus</td>
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<td>EDX</td>
<td>X-ray spectroscopy</td>
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<td>FDP</td>
<td>Fretting damage parameter</td>
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<td>FDS</td>
<td>Flow-drill screw</td>
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<td>FEA</td>
<td>Finite element analysis</td>
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<td>FEM</td>
<td>Finite element methods</td>
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<td>H</td>
<td>Diamond pyramid hardness</td>
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<td>ISV</td>
<td>Internal state variable</td>
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<tr>
<td>kₑₑq</td>
<td>Local stress intensity factor – equivalent mode I</td>
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<td>kᵢ</td>
<td>Local stress intensity factor – mode I</td>
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<td>Kᵢ</td>
<td>Global stress intensity factor – mode II</td>
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<tr>
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<td>Description</td>
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<td>$K_{II}$</td>
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<td>$k_{II}^{PS}$</td>
<td>Fretting fatigue damage parameter</td>
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<tr>
<td>LD</td>
<td>Longitudinal direction</td>
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<tr>
<td>m</td>
<td>Material constant obtained from crack growth experiments</td>
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<td>n</td>
<td>Strain hardening coefficient</td>
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<tr>
<td>N</td>
<td>Number of cycles</td>
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<tr>
<td>ND</td>
<td>Normal direction</td>
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<tr>
<td>Nf</td>
<td>Cycles to failure</td>
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<tr>
<td>$N_{Total}$</td>
<td>Total number of cycles</td>
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<tr>
<td>PEEQ</td>
<td>Equivalent plastic strain</td>
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<td>PTFE</td>
<td>Polytetrafluoroethylene</td>
</tr>
<tr>
<td>Q</td>
<td>Applied load per joint</td>
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<tr>
<td>r</td>
<td>Joint radius</td>
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<tr>
<td>R</td>
<td>Load ratio</td>
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<tr>
<td>RSW</td>
<td>Resistance spot welding</td>
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<tr>
<td>SEM</td>
<td>Scanning electron microscope</td>
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<tr>
<td>SIF</td>
<td>Stress intensity factor</td>
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<tr>
<td>SPR</td>
<td>Self-pierce rivet</td>
</tr>
<tr>
<td>t</td>
<td>Sheet thickness</td>
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<tr>
<td>TD</td>
<td>Transverse direction</td>
</tr>
<tr>
<td>XRD</td>
<td>X-Ray diffraction</td>
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<tr>
<td>$\epsilon_0$</td>
<td>Reference strain rate</td>
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<tr>
<td>$\epsilon_f^{pl}$</td>
<td>Damage parameter</td>
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</table>
\( \Delta \varepsilon^{pl} \)  
Damage parameter

\( \varepsilon_x \)  
Strain on x direction

\( \varepsilon_y \)  
Strain on y direction

\( \varepsilon_z \)  
Strain on z direction

\( \gamma \)  
Loading ratio parameter

\( \nu \)  
Poisson’s ration

\( \sigma_1 \)  
Maximum principal stress

\( \sigma_x \)  
Stress on x direction

\( \sigma_y \)  
Stress on y direction

\( \sigma_{yield} \)  
Yielding stress

\( \sigma_z \)  
Stress on z direction

\( \tau_{max} \)  
Maximum shear stress

\( \bar{\theta} \)  
Non-dimensional temperature

\( \delta_{res} \)  
Resultant of slip

\( \omega_s \)  
Damage parameter
ACKNOWLEDGEMENTS

I would like to acknowledge my family, fiancé, friends, and mentors for their encouragement and support during this stage of my life. I would like to specially thank my parents, bother, sister, fiancé and the Iliev family for their unconditional support. Also, I would like to thank my friends and research colleagues, Dr. Rogie I. Rodriguez, Dr. Harish Rao, Dr. Li Huang, Abby Cisko, Omar Rodriguez, Brian Fay, Dr. Jeffrey R. Bunn and Dr. Lindsay Sochalski. Moreover, I would like to thank my graduate committee and mentors, Dr. J. Brian Jordon, Dr. Xuming Su, Dr. Luke Brewer, Dr. Mark Barkey, and Dr. Paul Allison, for believing in me and always giving me guidance. Also, I would like to acknowledge The University of Alabama, the USAMP (U.S. Automotive Materials Partnership LLC) and Ford Motor Co. for supporting my graduate studies.
CONTENTS

ABSTRACT ........................................................................................................................................ ii
DEDICATION ........................................................................................................................................ iv
LIST OF ABBREVIATIONS AND SYMBOLS ................................................................................ v
ACKNOWLEDGEMENTS ...................................................................................................................... viii
LIST OF TABLES .................................................................................................................................. xi
LIST OF FIGURES ............................................................................................................................... xii

CHAPTER 1 INTRODUCTION ........................................................................................................... 1
  1.1 Motivation .................................................................................................................................... 1
  1.2 Preface of Chapters ....................................................................................................................... 5

CHAPTER 2 FATIGUE AND FAILURE MECHANISMS IN MIXED METAL SELF-PIERCING RIVETED JOINT AT HIGH CYCLE FATIGUE ........................................................................ 7
  2.1 Abstract ......................................................................................................................................... 7
  2.2 Introduction .................................................................................................................................... 8
  2.3 Materials and Methods .................................................................................................................. 11
    2.3.1 Self-Piercing Rivet ................................................................................................................... 11
    2.3.2 Mechanical Testing and Material Characterization ................................................................. 11
    2.3.3 Self-Piercing Rivet Process Simulations ................................................................................ 12
  2.4 Results and discussion .................................................................................................................. 15
    2.4.1 Fatigue Behavior ..................................................................................................................... 15
    2.4.2 Finite Element Analysis and Fatigue Life Prediction ............................................................ 25
  2.5 Conclusions ................................................................................................................................... 34
LIST OF TABLES

Table 2-1. Coefficients of friction used in the FEA.......................................................... 26
LIST OF FIGURES

Figure 2-1. Geometrical features of the SPR test coupon........................................................................12

Figure 2-2. Cross-section of the untested SPR joint........................................................................12

Figure 2-3. Initial mesh configuration used in current FEA modeling..............................................14

Figure 2-4. Fatigue test results (frequency 40 Hz, R=0.1).................................................................16

Figure 2-5. Representative of the SPR test coupons tested at 900 N and 1125 N load amplitudes (a) and (b) faying/fretting surface of the top sheet in SPR joints tested at 900 N load amplitude, black arrows indicate the crack initiation region due to fretting in SPR joints tested at 900 N and 1125 N load amplitude respectively, (c) and (d) top view of the failed SPR test coupon indicating the failure of the top sheet away from the rivet in SPR joints tested at 900 N and 1125 N load amplitude respectively, (e) and (f) section view of fractures surface in at 900 N and 1125 N load amplitude respectively.................................................................17

Figure 2-6. Cross-section view of the SPR coupon (a) and (b) macrographs of the top sheet failure, the bold white arrow indicates the crack initiation and propagation direction in SPR coupons tested at 900 N and 11125 N load amplitude respectively, (c) and (d) cross-section of the samples through the mid-section indicates the dominant crack through the top sheet in SPR coupons tested at 900 N and 11125 N load amplitude respectively.................................................................19

Figure 2-7. Fractography images of the SPR coupon tested at 900 N load amplitude (a) macrograph of the fracture surface (b) macrograph of the faying surface of the top sheet, (c) ductile fracture as observed under SEM in region S1 on the fracture surface, (d) SEM images indicating the fretting and crack initiation region, dark gray area indicates the fretting debris and black arrows indicate the crack propagation direction, (e) surface delamination of the top sheet (bottom surface) due to fretting as observed under SEM and (f) crack initiation region on the fretting surface of the top sheet as observed under SEM.................................................................21

Figure 2-8. Fractography images of the SPR coupon tested at 1125N load amplitude (a) macrograph of the fracture surface (b) macrograph of the faying surface of the top sheet, (c) ductile fracture as observed under SEM in region S1 on the fracture surface, (d) SEM images indicating the fretting and crack initiation region, dark gray area indicates the fretting debris and black arrows indicate the crack propagation direction, (e) surface delamination of the top sheet (bottom surface) due to fretting as observed under SEM and (f) crack initiation region on the fretting surface of the top sheet as observed under SEM.................................................................23
Figure 2-9. EDX chemical mapping of the fretting surface (a) SEM image of the fretting surface (b) EDX chemical mapping of the fretting surface and (c) composites indicating the chemical element as represented by each different colored layers........................................24

Figure 2-10. FEA simulated riveting process versus the actual riveting process........................................27

Figure 2-11. FEA simulation of the riveting process (a) right after the SPR process (b) after the release of the punch and blank-holder and (c) after the release of die..................................................27

Figure 2-12. Cross-section of the SPR shows (a) fatigue crack initiation and propagation region in the lap-shear specimens and (b) FEA simulation indicates the contact pressure away from the rivets close to where the fatigue cracks initiated..................................................28

Figure 2-13. Schematic representation of kinked crack for resistance spot welding..................31

Figure 2-14. Load amplitude versus number of cycles to failure plot. Comparison between experimental and predicted curves for high and low cycles parameters...........................33

Figure 3-1. Self-pierce riveting process scheme. [12]..........................................................37

Figure 3-2. Schematic of the SPR specimen. Dashed lines represent the gage volume used in neutron diffraction measurements.........................................................................................39

Figure 3-3. Experimental setup of the neutron diffractometer at Oak Ridge National Laboratory.................................................................................................................................42

Figure 3-4. X-ray diffraction setup: a) X-ray residual stress diffractometer, b) bottom surface of the bottom of the SPR joint with the X-Ray diffraction measurements locations........43

Figure 3-5. Initial mesh configuration used in FEA modeling.: a) axisymmetric “wedge” model used in the calibration stage; b) half 3D model with middle symmetry used for comparison with experimental results. ........................................................................................................47

Figure 3-6. Strain profiles obtained from neutron diffraction results of: a) bottom sheet (aluminum), and b) top sheet (magnesium). ..................................................................................................................50

Figure 3-7. Contour plot of stresses on the aluminum side of the joint obtained from X-Ray diffraction results: a) Stress on the longitudinal direction, b) stress on the transverse direction.51

Figure 3-8. Visual comparison of the simulation results (green lines) superimposed over the experimental cross-section. Note only the left side of the comparison was used for model calibration..........................................................................................................................52

Figure 3-9. Von Mises stress profile: a) right before the end of the piercing process, b) after spring-back. Units in MPa. ..........................................................................................................................53
Figure 3-10. Hardness profile of the self-pierce riveting joint: a) full profile of the rivet and sheets, b) comparison of the experimental hardness of the bottom sheet (left) and the calculated hardness obtained from numerical analysis (right)..........................54

Figure 3-11. Quantitative comparison of residual stresses from the simulation and X-Ray diffraction results: a) Stress on the longitudinal direction, b) stress on the transverse direction. 55

Figure 3-12. Residual stress comparison between numerical model and neutron diffraction results: a) bottom sheet (aluminum), b) top sheet (magnesium). ..........................................................58

Figure 4-2. Geometrical features of the SPR test coupon. ..........................................................66

Figure 4-3. Initial mesh configuration used in the finite element simulations. ..........................70

Figure 4-4. 3-D lap-shear coupon design..........................................................72

Figure 4-5. Comparison of the FE simulation and experimental cross-section.................74

Figure 4-6. Remeshing and remapping evaluation of the fine mesh after self-piercing rivet process: a) final geometry and mesh, c) Mises Stress, e) Equivalent plastic strain (PEEQ). Coarse mesh based of fine mesh geometry: b) final geometry and mesh, d) Mises Stress, d) Equivalent plastic strain (PEEQ). ..........................................................75

Figure 4-7. Evolution of Mises stress: a) before and b) after the spring-back..............76

Figure 4-8. Load displacement curves for experimental and simulation results of self-pierce riveting joint..........................................................78

Figure 4-9. Visual comparison of the fractured surface subjected to fatigue loading and high stress amplitude at the same location predicted numerically........................ 79

Figure 4-10. Fretting wear location comparison. a) experimental, b) without residual stresses and plastic strains, c) with residual stresses only, d) with plastic strains only, and e) including both residual stresses and plastic strain resultants from the manufacturing process....81

Figure 4-11. Cross section of fractured surface. a) experimental, mesh and predicted crack plane: b) not including residual stresses nor plastic strains, c) including residual stresses only, d) including plastic strains only, and e) including residual stresses and plastic strains, superimposed on the experimental cross section. ..........................................................84

Figure 4-12. Schematic representation of kinked crack for resistance spot welding........86

Figure 4-13. Comparison between experimental and predicted number of cycles to failure based on simulation results with and without residual stresses and plastic strains...............87

Figure 5-1. Initial configuration of flow-drill screw. a) lap-shear specimen, b) close view.........92
Figure 5-2. Cross-section initial stage of flow-drill screw

93
CHAPTER 1
INTRODUCTION

Due to high demand of joining dissimilar materials such as magnesium and aluminum alloys, mechanical joining has been widely used as an alternative joining technique. Among methods of mechanical joints available, self-pierce riveting (SPR) has been widely investigated[1–13]. However, the mechanical joint behavior of SPR joints has not yet been fully characterized, therefore leading to the focus of this study. Thus, the main objective of the present work is to develop analytical models to simulate mechanical joining processes, and analyze residual stresses and contact forces (magnitude, direction and distribution) for dissimilar materials (aluminum and magnesium alloys). Residual stresses and contact forces are validated and correlated with experimental results. The validated approach is then used to determine cause-effect relationships of fatigue and quasi-static tensile behavior in SPR joints.

1.1 Motivation

In recent years, regulations worldwide have been pushing the industry to reduce fossil fuel consumptions. To achieve this goal, under utilized materials with high strength-to-weigh ratio such as aluminum and magnesium are appealing solutions. However, viable technologies to join these non-ferrous alloys remain an issue. Traditionally, resistance spot welding (RSW) is used to join steel in the automotive industry, such techniques present difficulties in joining aluminum and magnesium alloys [14,15]. These complications are mainly due to high electrical conductivity and low heat generation in these alloys, requiring elevated welding currents [16,17]
resulting in poor weld characteristics. Many relatively new technologies, including SPR and flow-drill screw (FDS), have been shown as viable methods to produce sound joints. In fact, studies show that SPR joints, are joints with comparable strength and superior fatigue performance compared to RSW, and it also shows promising results in peel and shear testing [18], the same applies to FDS. However, good understanding of the SPR and FDS process remains unexplored.

Many simulations that specifically target the SPR process modeling have successfully captured the large deformation that occurs during this process. Moreover, non-linear finite element methods (FEM) have proven the validity in applying this method to model the SPR process itself [19–25]. King [19] was one of the pioneers to use FEA to determine forces, displacements and deformations during the SPR process on aluminum alloys. Later, Hanh and Dolle [20] achieved good agreement between FEA and experimental results of load and displacements in SPR process. More recently, Atzeni et al. [21–23,7] determined suitable friction coefficients and contact strategies for simulating SPR process. Furthermore, Porcaro et al. [24] performed studies on viability of FE modeling for different rivet geometries, stack-up combinations and material tempers. Also, SPR simulations were utilized to explore different materials tempers [24,26,3], different die geometries [24,26,3,27], altered stack configuration [24,26,28], diverse sheet thickness [24,26,29,30], joinability with different materials [28], different steel rivet materials [27], die optimization [31], fracture of the rivet [32], different rivet geometries [24,26] as well as crack prediction during SPR process [25]. Additionally, FEA of SPR studied the influence of numerical parameters such as mesh sensitivity [24,8], strain to failure [8], failure criteria [4,33], different yield strength of rivet and sheets [33], adiabatic and isothermal process [25] and friction between rivet and sheets [27]. SPR process analysis was
done on diverse material combinations such as on aluminum-to-aluminum [24,7,26,3,27,29,30,32,8,34–37], steel-to-aluminum [28,31,4,33,36], steel-to-steel [36], aluminum-to-carbon fiber [38,39], magnesium-to-magnesium [25]. Moreover, simulation with rivets made of different materials other than steel, such as aluminum, were studied [3,32]. For the SPR process, different material models and techniques were employed, where most of the studies used material models with isotropic hardening only [24,7,26,3,27–32,8,4,33–39], and it was found that only one source used isotropic and kinematic hardening and recovery [25]. With these materials, some variances were applied re-meshing techniques [24,26,3,27,29,30,32,4,33–36], strain-rate sensitivity [25,29,37], isothermal [24,7,26,3,27,28,30–32,8,4,33–36,38,39] and adiabatic materials [25,29,37]. With different materials and solvers used, many damage criteria were considered for SPR simulations such as Gurson-Tvergaard [7], Lamaitre [36], Cokoroff-Latham [29,38], Cocks-Ashby [25], maximum shear strain [4], effective strain to failure [8], sheet thinning criterion [24,26,3,27,34], selected element deletion [30,33] and Johnson-Cook [37]. Regarding FEA of SPR performance after the process, some works were published achieving good results for shear [7,26,30,35,39], coach peel [26,35], U-shape pull [26,35,37], and also shear comparison with and without residual stresses [26,36]. In order to achieve the results cited above, many commercial codes were used such as Ls-Dyna [24–26,3,28,31,32,8,4,34,35], Abaqus [7,37], MSC [27,29,30,33], Forge 2005 [36], Deform 2D [38] and Ansys [39]. Until recently, there is no simulation of SPR on aluminum-to-magnesium alloy. Moreover, only one study[13] compared residual stresses to experimental results such as neutron diffraction, and there is no studies comparing residual stresses of SPR to X-Ray diffraction(XRD). In addition, influence of residual stresses and contact forces were not
correlated to fatigue crack initiation and life. As the commercial code Abaqus is widely used for fatigue purposes, the SPR process and analysis will employ this commercial code.

Regarding fatigue of SPR, many studies have been performed on fracture and fatigue behavior of SPR of aluminum-to-aluminum [40–44] and aluminum-to-steel alloys [28,45–48]. The failure of SPR joints under fatigue loading is complex as shown by experimental work [41,48,49,10]. This complexity is in part due to high plastic deformation and contact forces which occurred during the SPR process. Plastic deformation results in residual stresses around the rivet. Such residual stresses and contact forces, combined to the cyclic loading and oscillation between the matting sheets during fatigue, can create stress concentrations and localized fretting that typically results in premature fatigue crack initiation. It is well known that relative motion between two surfaces that are clamped and held closely, results in fretting wear [50,51], leading to a drastic decrease in fatigue life of SPR joints. Moreover, this fretting wear often occurs away from the rivet resulting in failure of the base material instead of the high stress concentrations sites at high number of cycles to failure and relatively low cyclic loads applied. Chen et. al observed debris and fine oxide particles at the fretting location, with multiple fretting wear between the faying surfaces of aluminum 5754 sheets, and between the rivet and botom sheet. Such fretting wear resulted in micro-cracks that grew into a dominant crack failing the SPR joint. Likewise, Huang et al. [45] reported that at high cyclic load levels, fretting occurred between the rivet and the sheets, leading to rivet failure. On the other hand, at low cyclic loads, fretting occurred between the sheets, resulting in aluminum sheet failure. In addition, several studies show that fretting wear damage is unavoidable and leads to failure by the rivet or by the sheet[40,42,43,45,52].
In the study of Han et al. [52], it was shown that coating the faying surface of aluminum sheets with a layer of wax-based slid lubricant improved fatigue life of the SPR joints studied. This is due to the lubricant reducing fretting damage in the joints. In addition, residual stresses can change the location of stress concentration when loading compared to numerical analysis with no residual stresses. In fact, a numerical study done on SPR joints with no residual stresses from the SPR process showed that residual stresses are very critical to predict crack initiation sites [43]. Moreover, applying standard CAE models, such as structural stress methods, is difficult because these methods generally do not take residual stresses into account.

Reasonable results have been reached when simulating SPR using FEA. Comprehensive finite element analysis (FEA) of local stresses under cyclic loading is able to capture local stress raisers due to the added rivet. However, these approaches do not take into account residual stresses due to the riveting process. Nevertheless, the simulation methods found in literature have not been used for additional understanding regarding fretting initiation fatigue damage. As the results found in literature indicate that fretting is a dominant factor that results in fatigue crack initiation, the present study emphasizes the correlation of fretting wear to SPR process. Moreover, since fretting usually occurs at areas with high contact pressure, this study will focus on the correlation of fatigue and fracture mechanics to contact pressure determined by FEA.

1.2 Preface of Chapters

The overall objective of this research is to study the effect of the manufacturing process on the static and fatigue behavior of self-pierce riveted (SPR) joints.
Chapter 2 focuses on the dissimilar aluminum 6111-to-aluminum 5754 SPR joints. In this chapter, fatigue performance at high cycle fatigue is evaluated. Numerical analysis of the SPR manufacturing process is performed and validated through visual comparison. Contact pressures predicted numerically are correlated with fretting locations experimentally. A kinked crack linear elastic fracture mechanics model is used to accurately predict fatigue life of the joint.

Chapter 3 presents experimental and simulation results of residual stresses, elastic strains and yielding surface within the SPR joint. Numerical simulations show good agreement with experimental results including neutron diffraction, XRD, and the yielding surface converted to hardness measurements.

Chapter 4 is a study on the effects of residual stresses and plastic strains on the macro mechanical behavior including the quasi-static and fatigue performance of the SPR joint. Simulations with and without residual stresses and plastic strains due to the riveting process were compared to experimental results. The results show that work hardening is the driving force for stiffness and strength of the SPR joint. Moreover, including residual stresses and plastic strains lead to more accurate results when predicting load-displacement curves, as well as fatigue response including fretting, crack initiation, initial crack propagation plane, and number of cycles to failure.
2.1 Abstract

A study examining the fatigue failure mechanism of self-piercing riveted (SPR) joints between aluminum 6111 and 5754 series is presented in this paper. This study characterizes the high cycle fatigue behavior of the SPR joints in the lap-shear configuration. Experimental fatigue testing revealed that failure of SPR joints occurred due to cracks propagating through the sheet thickness at locations away from the rivet. Postmortem analysis showed that significant fretting wear occurred at the location of the fatigue crack initiation. Energy dispersive X-ray (EDX) of the fretting debris revealed the presence of aluminum oxide that is consistent with fretting initiated fatigue damage. High fidelity finite element analysis (FEA) of the SPR process revealed high surface contact pressure at a location that coincides with the region of fretting initiated fatigue observed during postmortem analysis of the failed coupons. Furthermore, fatigue modeling predictions of the number of cycles to failure based on linear elastic fracture mechanics supports the conclusion that the fretting initiated fatigue occurred at regions of high surface contact pressure and not at locations of nominal high stress concentration at the rivet.
2.2 Introduction

There is an increased demand by both federal agencies and consumers around the globe for automobiles with a better fuel economy and reduced green house gases [53]. One of the ways to increase the fuel efficiency is by the use of lightweight structural alloys in place of more dense steels. Aluminum alloys in particular are of high interest due to their good strength-to-weight ratios. For economical purpose and design consideration, multiple materials may be used in combination with each other. However, aluminum alloys can be difficult to join using traditional welding techniques like resistant spot welding (RSW). As such, several alternate joining techniques exist for joining aluminum alloys including self-pierce riveting (SPR). The SPR technique is a high speed mechanical fastening process, that has been increasingly used to join aluminum and hard to weld materials [54,55]. The SPR process has been studied widely and implemented by several automobile manufacturers as an economical and effective technique to join aluminum vehicle bodies [45,54,56–58].

Several studies have been performed with regards to understanding the fatigue performance and failure mechanisms in SPR joints of similar aluminum-to-aluminum [41,43,52,11,59] and aluminum-to-steel [45–48,60] alloys. It is well documented that the relative motion between two surfaces during cyclic loading typically results in fretting wear [50,51]. Similarly, in SPR joints, fretting can substantially decrease the fatigue life of the riveted joints [42,43,45,52,11]. In aluminum 5754 sheets joined by SPR, multiple fretting was observed between the faying surfaces of the sheets and between the rivet. The contact stresses between the two sheets, which are a result of the setting force applied during the SPR joining, lead to the formation of fretting debris/oxide particles. In some instances, these fretting debris can initiate fatigue cracks, due to movement of the particles during cyclic loading. In all the instances,
fretting wear under cyclic loading led to the formation of micro cracks, which eventually grew into the dominant fatigue crack, resulting in failure of the SPR joint [11]. Most studies on fatigue behavior of SPR joints have indicated that, the fretting wear damage is inevitable and leads to failure of the SPR joints either by sheet or rivet failure [42,43,45,52,11]. Han et al. [52] noted that the fatigue life of SPR joints in aluminum alloys was significantly improved when the faying surface interface was coated with a layer of Polytetrafluoroethylene (PTFE). The PTFE layer reduced the friction between the sheets resulting in less fretting debris leading to an improvement of fatigue life in the SPR joints. In a study on the influence of sheet thickness on tensile strength in SPR joints of aluminum alloys, the thicker sheets exhibited better static performance compared to the thinner sheets [5]. However, the thicker sheets did not translate to better fatigue life performance in the SPR joints [43]. Finite element analysis (FEA) of the thicker sheets indicated higher stress concentrations at the faying surface near to the rivet which corresponded to location where fatigue cracks initiated due to fretting.

Regarding simulation and modeling, studies on SPR joints have been carried out for various materials and joint configurations. One of the pioneers on SPR joint simulations, Porcaro et al. [24] were able to calculate profiles of the SPR joints that geometrically matched the experimental cross-sections. They achieved these results using an axisymmetric model in LS-DYNA with a r-adaptivity re-meshing technique, and part separation criteria based on sheet thickness. In addition, good results were obtained by employing a failure criteria based on effective strain-to-failure [8]. Recently, Moraes et al. [61] performed simulations of the SPR process using an internal state variable (ISV) material model capable of correlating temperature and strain rate effects, and that also includes a damage model based on void growth. Other studies with acceptable results were conducted [7,62], such as applying Gurson–Tvergaard
damage model as the failure criterion [7,62]. As the SPR process is dynamic, FEA of the forming process commonly uses an explicit dynamic solver, followed by spring back calculations with an implicit solver [24,4,62]. However, spring back calculations using explicit analysis with mass damping have also been performed with reasonable results [13,61].

Even though satisfactory results have been achieved for SPR simulations using FEA, the process simulation methods developed in literature have not been used to further understanding of fatigue damage. Iyer et al. [43] studied the fatigue behavior of SPR, where in addition to experimental tests, they performed a three dimensional (3D) elastic finite element analysis of single rivet joints in order to evaluate load induced local distributions of relative micro slip, contact pressure and bulk stress. However stress, strain, and contact forces due to the SPR process itself were not taken into account in the simulations. Their study indicated that some failures of SPR joints under fatigue conditions may have occurred due to high levels of clamping residual stresses, which were not included in the FEA by Iyer et al. [43].

Since the consensuses in literature is that fretting is the dominant factor resulting in fatigue crack initiation in SPR, the emphasis in this study is to relate the mechanics of fretting wear to the deformation history that develops in the SPR process. Furthermore, since fretting generally occurs at areas of high contact pressure, efforts have been made to determine contact pressure due to SPR process using finite element methods. As such, this work presents a comprehensive study on the fatigue and fracture mechanisms and the correlation of contact pressure determined by FEA. Lastly, linear elastic fracture mechanics is used to predict the fatigue life at the locations of fretting predicted by the FEA.
2.3 Materials and Methods

2.3.1 Self-Piercing Rivet

The dissimilar aluminum lap-shear SPR test coupons were made of aluminum 6111-T4 and aluminum 5754-O sheet and riveted by HENROB servo-electronic riveting equipment using 36MnB4 rivets. Each sheet measured 3.0 mm in thickness and the rivets measured 5 mm in diameter and 7.8 mm in length. The final geometric configuration of the SPR joined test coupon is shown in Fig. 2-1 and the cross-section of the SPR joint is shown in Fig. 2-2.

2.3.2 Mechanical Testing and Material Characterization

The load controlled fatigue tests were performed on servo hydraulic load frames at a 40 Hz frequency at a load ratio of R=0.1. Shims were used on both ends of the lap-shear coupons to reduce the bending of the samples during testing. For metallurgical failure analysis of the SPR test coupons, the tested and untested SPR coupons were cut along the center of the coupon width parallel to loading direction. The cross-sectioned samples were then cold mounted in epoxy, ground and polished, with a final polishing done using a 0.5 μm colloidal silica. The macrographs of the samples were performed using a Keyence VHX100 digital optical microscopy. For energy dispersive X-ray spectroscopy (EDX) and fracture surface analysis, the samples were analyzed under Jeol 7000 series scanning electron microscopy (SEM).
2.3.3 Self-Piercing Rivet Process Simulations

Numerical simulations were performed using the commercial solver ABAQUS version 6.13-3. Since the SPR is a dynamic process, the explicit solver was chosen. SPR joints can be approximated as axisymmetric and many studies have been done on SPR process simulations using 2D elements [24,7,8]. Nevertheless, 3D elements with reduced integration were used in order to take advantage of a better contact formulation included in ABAQUS (General Contact). The dimensions of the elements in different sections of the model on the X-Y plane (Fig.2-3)
include 0.2 x 0.2 mm on average for elements in the rivet and 0.05 x 0.05 mm for elements in the sheets. The depth of the elements were graded depending on the distance of the element from the Y-axis, as the mesh was built by sweeping 7.5 degrees around the Y-axis. As a result, the model appears to have a “wedge” geometry. The initial mesh configuration is shown in Fig. 2-3. An isotropic material hardening model was applied for both the sheets and the rivet. Stress-strain curves for various materials were obtained from literature: 6111 aluminum alloy (top sheet) [63]; 5754 aluminum alloy (bottom sheet) [64]; and HB37 (rivet) [65]. The die and punch were modeled as rigid bodies while the blank-holder was modeled as purely elastic with a very high elastic modulus which allows it to be nearly as stiff as a rigid body. However, this also allows pressure loads to be easily applied on its top surface. In order to allow the rivet to pierce through the top sheet, elements were deleted according to the Johnson-Cook damage criterion. Damage evolution was selected to rapidly decrease the energy of the element to be removed before it is deleted, avoiding numerical complications due to energy equilibrium. Equations (1) and (2) are the governing equations of Johnson-Cook damage criterion implemented in ABAQUS. Complete damage occurs when the damage parameter, \( \omega \), reaches unity:

\[
\omega = \sum \left( \frac{\Delta \varepsilon_{pl}}{\varepsilon_{pl}^i} \right)
\]  

\( \text{(2.1)} \)

where \( \Delta \varepsilon_{pl} \) is an increment of plastic strain, \( \varepsilon_{pl}^i \) is the strain at damage initiation, and the summation is performed at each increment of the analysis. \( \varepsilon_{pl}^i \) is defined in Equation (2.2):

\[
\varepsilon_{pl}^i = \left[ d_1 + d_2 \exp \left( d_3 \frac{p}{q} \right) \right] \left[ 1 + d_4 \ln \left( \frac{\varepsilon_{pl}^i}{\varepsilon_0} \right) \right] (1 + d_5 \tilde{\theta})
\]  

\( \text{(2.2)} \)

Constants \( d_{1-5} \) are failure parameters, \( \varepsilon_0 \) is the reference strain rate, \( \tilde{\theta} \) is the non-dimensional temperature, \( p \) is the pressure stress and \( q \) is the Mises stress. Since parameters \( d4 \) and \( d5 \) are
related to strain rate sensitivity and temperature, respectively, they were set to zero during the model calibration because the material model chosen is neither strain rate nor temperature dependent. Failure parameters and friction were calibrated through visual comparison between simulation and cross-section of the SPR joint.

Following the calibration stage, a spring-back study was performed. This simulation was performed using the explicit code in order to keep contact histories intact. This type of spring-back was achieved releasing the punch and blank-holder slowly, applying a viscous pressure load to damp dynamic oscillations and to achieve equilibrium.

Figure 2-3. Initial mesh configuration used in current FEA modeling.
2.4 Results and discussion

2.4.1 Fatigue Behavior

Figure 2-4 is the plot of fatigue test results of the dissimilar aluminum SPR joints. Two prominent modes of failure were observed; failure of the rivet and failure of the top sheet. The failure of the rivets occurred in coupons tested above a load amplitude of 2000 N. In SPR coupons tested below a load amplitude of 2000 N, the failure occurred due to gross section failure of the top AA6111 sheet. In addition, at a load amplitude of 450 N, the majority of the SPR coupons achieved run-out, which was defined as a minimum of 10 million cycles without failure. The focus of this work is on the fatigue and fracture of the SPR joints in the high cycle fatigue regime ($N_f > 10^6$ cycles, highlighted in Fig.2-4). For high cycle fatigue, fretting can be the driving force of crack initiation and thus initiate fatigue cracks at locations away from the high stress concentration (i.e., the rivet), resulting in sheet failure. The failure modes of SPR joints with fatigue life cycles below $10^6$ cycles, where the failure locations are at the rivet and fretting is not the root cause of crack initiation, will not be discussed in this work, but will be addressed in future work.
Figure 2-5 shows the cross-section of the representative failed SPR coupons. Figure 2-5(a) and Fig. 2-5(b) are the macrographs showing the top sheet failure in SPR joints tested at 900 N and 1125 N load amplitude respectively and Fig. 2-5(c) and Fig. 2-5(d) are the magnified cross-section view of the above SPR coupons. The bold white arrows in these figures indicate the location and direction of fatigue crack initiation and propagation due to fretting. Shear lips were observed in both the failure modes. In SPR coupons tested at a 900 N load amplitude, the shear lip was observed on the opposite corner of the crack initiation region and in SPR coupons tested at a 1125 N load amplitude, the shear lip was observed close to center of the sheet width as seen in Fig. 2-5(a) and 2-5(b) respectively. No secondary cracks were observed in either of the SPR
coupons as is evident from the cross sections seen in Fig. 2-5(c) and 2-5(d). Also, the rivet and sheet interface exhibited no signs of fretting, indicating fretting occurred away from the rivet.

Figure 2-5. Representative of the SPR test coupons tested at 900 N and 1125 N load amplitudes (a) and (b) faying/fretting surface of the top sheet in SPR joints tested at 900 N load amplitude, black arrows indicate the crack initiation region due to fretting in SPR joints tested at 900 N and 1125 N load amplitude respectively, (c) and (d) top view of the failed SPR test coupon indicating the failure of the top sheet away from the rivet in SPR joints tested at 900 N and 1125 N load amplitude respectively, (e) and (f) section view of fractures surface in at 900 N and 1125 N load amplitude respectively.

Figure 2-6 represents the failure mode in SPR coupon tested at 900 N and 1125 N load amplitudes. Fretting was observed on the faying surface of the top and bottom sheets. In Fig. 2-6(a) and (b) the bold arrows represent the fretting regions on the top sheet faying surface.
the bold black arrows represent the dominant fretting regions, the bold white arrows indicate the non-critical fretting regions on the faying surfaces. In both the failure modes, the fatigue cracks propagated away from the rivet as seen in Fig. 2-6(c) and 2-6(d). In SPR coupons tested at a 900 N load amplitude, fretting on one corner of the faying surface initiated fatigue cracks while in the SPR coupons tested at a 1125 N load amplitude, fretting on each of the corners of the sheet initiated a fatigue crack which led to failure of the top AA6111 sheet as seen in Fig. 2-6(e) and 2-6(f) respectively.

In Fig. 2-6(c) and 2-6(d), a circular mark appears at some distance away from the rivet head. This is the mark that is left behind by the punch of the SPR process. During the SPR process, the vertical holder and die hold the top and bottom sheets together throughout the complete process. The downward force applied by the vertical holder and die may be high enough to introduce contact pressure which remains at the sheet interface even after the SPR process is complete. A close observation in Fig. 6(c) and 6(d) also indicate that the failure occurs at the circumference of this circular mark. The FEA simulation, discussed in the next section, also indicates high contact pressure at the region where the vertical holder is placed on the top sheet, thus indicating the downward force applied by the vertical holder introduces high contact pressure. Consequently, fretting of the faying surface at this region resulted in crack initiation.
Figure 2-6. Cross-section view of the SPR coupon (a) and (b) macrographs of the top sheet failure, the bold white arrow indicates the crack initiation and propagation direction in SPR coupons tested at 900 N and 11125 N load amplitude respectively, (c) and (d) cross-section of the samples through the mid-section indicates the dominant crack through the top sheet in SPR coupons tested at 900 N and 11125 N load amplitude respectively.

Figure 2-7 shows various regions of the fracture surface of SPR joint tested at 900N load amplitude. Figure 2-7(a) and 2-7(b) are the top surface and fretting surface of the top AA6111 sheet as seen under a digital optical microscope. The square boxes indicate the regions of interest, S1, S2, S3 and S4, which were observed under SEM. As mentioned earlier, the fatigue cracks initiated at one corner of the sheet width and propagated to the other corner. Region S1
indicates the ductile fracture surface as seen in Fig. 2-7(c), while S2 indicates the region of crack initiation as seen in Fig. 2-7(d). The dark gray region closer to the bottom surface of the top sheet (faying surface) in region S2 as seen in Fig. 2-7(d) indicates the presence of fretting debris. It appears that the fretting debris ploughed through the surface leading to erosion of the sheet by forming localized craters at the faying surfaces. The lighter gray region in Fig. 2-7(d) is characterized by presence of fatigue striations and river marks that point towards the dark gray region indicating the cracks initiated near the faying surface. The bold black arrows indicate the crack propagation direction. On the opposite side of the sheet width, surface delamination can be observed (region S3) as seen in Fig. 2-7(e). While this area shows high levels of fretting, there is no evidence of fatigue crack growth or initiation. Region S4, as seen in Fig. 2-7(f) shows the fretting surface where the dominant fatigue cracks initiated. As observed, the crack initiation site showed the presence of fretting debris which were formed due to surface delamination and ploughing similar to observations noted elsewhere [45].
Figure 2-7. Fractography images of the SPR coupon tested at 900 N load amplitude (a) macrograph of the fracture surface (b) macrograph of the faying surface of the top sheet, (c) ductile fracture as observed under SEM in region S1 on the fracture surface, (d) SEM images indicating the fretting and crack initiation region, dark gray area indicates the fretting debris and black arrows indicate the crack propagation direction, (e) surface delamination of the top sheet (bottom surface) due to fretting as observed under SEM and (f) crack initiation region on the fretting surface of the top sheet as observed under SEM.
Figure 2-8 depicts the fracture surface of the SPR joints tested at a 1125 N load amplitude. Unlike in SPR joints tested at a 900 N load amplitude, fretting initiated cracks were observed on the two corners of the top sheet width. Figure 2-8(a) and (b) are the top surface and fretting surface of the top AA6111 sheet as seen under a digital optical microscope, respectively. The square boxes indicate the regions of interest, R1, R2, R3 and R4, which were observed under SEM. Top view of the fracture surface (region R1 and R2) in Fig. 2-8(c) and (d) reveal dark gray areas, which are fretting debris. The depth of these dark gray regions indicates the erosion of the faying surface due to fretting. Striation and river marks on the surface indicate the fatigue cracks initiated at these regions and propagated outwards as indicated by the bold black arrows as shown in Fig. 2-8(c) and (d). The fretting surface in region R3 and R4 indicate surface delamination and ploughing due to fretting as seen in Fig. 2-8(e) and (f) respectively. The fatigue cracks appears to have crack initiation at the end corners of the top sheet AA6111 and propagated towards the center of the sheet width.
Figure 2-8. Fractography images of the SPR coupon tested at 1125N load amplitude (a) macrograph of the fracture surface (b) macrograph of the faying surface of the top sheet, (c) ductile fracture as observed under SEM in region S1 on the fracture surface, (d) SEM images indicating the fretting and crack initiation region, dark gray area indicates the fretting debris and black arrows indicate the crack propagation direction, (e) surface delamination of the top sheet (bottom surface) due to fretting as observed under SEM and (f) crack initiation region on the fretting surface of the top sheet as observed under SEM.
Figure 2-9(a) shows the surface delamination of the top sheet due to fretting as imaged under SEM. Figure 2-9(b) shows the EDX chemical composition map of the delaminated surface, the composite of the different colored layers are shown in Fig. 2-9(c). The EDX mapping indicates, the delaminated surface and fretting debris around the region is primarily composed of oxides. The oscillatory motion between the top and bottom sheet contacting surfaces leads to micro slip, where the surfaces experience a relative motion in the order of 5-100 µm [66]. It is well documented that the first stage of fretting damage leads to the removal of the thin oxide layer present on the contacting surfaces [50,51,66]. As the continuous sliding action between the contacting surfaces continues, oxide debris accumulated and a fresh layer of the underlying surface was exposed. Further fretting cycles introduced plastic deformation resulting in formation of micro cracks, which are clearly seen in Fig. 2-9(a).

**Figure 2-9.** EDX chemical mapping of the fretting surface (a) SEM image of the fretting surface (b) EDX chemical mapping of the fretting surface and (c) composites indicating the chemical element as represented by each different colored layers.
Fretting between the sheet interface and rivet or between the two sheets has been commonly observed in SPR joints. At higher loads, the rivets tend to rotate and this results in a moveable interface between the sheets and the rivet surface resulting in fretting in the region [45,11]. At lower loads however, the fretting zone develops at certain distance away from the rivet. This region where the fretting occurs is due to the spring back after the riveting process [45]. The constant rubbing of the top and bottom sheets during fatigue loading results in a formation of fine oxide particles. These particles become embedded in the sheets and over a period of time ploughs the surface of the top and bottom sheet. In general, lap-shear tensile coupons when subjected to cyclic loading experience a slight bending moment. This bending along with contact pressure due to SPR processing tends to favor the fretting process which eventually led to failure of the top sheet.

2.4.2 Finite Element Analysis and Fatigue Life Prediction

In order to simulate the SPR process using finite element methods, material model parameters were calibrated including damage and friction coefficients. As noted previously, parameters $d_2$ and $d_3$ in equation (2.2) are related to the stress triaxiality, and they are key to obtaining a good comparison. Parameters $d_1$, $d_2$ and $d_3$ were found to be 0.18, 2, and 0.2 respectively. Moreover, coefficients of friction are also important and can change simulations results considerably. The combination of the correct damage and friction parameters are key to predicting the final configuration of the joint, which can make it possible to predict not only the final rivet geometry, but also the flow of material of the top sheet underneath the rivet and the thinning of the bottom sheet. The coefficients of friction found suitable for this work are shown in Table 2-1.
Table 2-1. Coefficients of friction used in the FEA.

<table>
<thead>
<tr>
<th>Contact pair</th>
<th>Coefficient of friction</th>
</tr>
</thead>
<tbody>
<tr>
<td>Punch/Rivet</td>
<td>0.4</td>
</tr>
<tr>
<td>Rivet/ Top sheet</td>
<td>0.12</td>
</tr>
<tr>
<td>Rivet/ Bottom sheet</td>
<td>0.12</td>
</tr>
<tr>
<td>Blank-holder/ Top sheet</td>
<td>0.2</td>
</tr>
<tr>
<td>Top sheet/ Bottom sheet</td>
<td>0.2</td>
</tr>
<tr>
<td>Top sheet/ Top sheet</td>
<td>0.15</td>
</tr>
<tr>
<td>Die/ Bottom sheet</td>
<td>0.15</td>
</tr>
</tbody>
</table>

Figure 2-10 shows the final configuration of the simulation after spring-back compared to the experimental cross-section. On the left, the edges of the rivet, top and bottom sheets of the simulation are shown, while the die, punch, and blank-holder were omitted for a clearer image. On the right side, the actual cross section of the riveted joint is shown. It is shown that the model predicts well the final geometry of the joint when comparing the shapes of the rivet and sheets to the experimental results. In addition, the material flow of the top sheet underneath the rivet was also predicted with good accuracy, demonstrating that damage parameters for triaxiality were calibrated.

Figure 2-11 shows the evolution of residual stress that occurs during the spring-back, after the SPR process. Figure 2-11(a) shows the FEA results just after the SPR process and Fig. 2-11(b) shows the results just after the release of the punch and blank-holder. Figure 2-11(c) shows the FEA results after the release of the die. It can be readily observed how the residual stresses decrease during and after spring-back. In addition, there are more residual stresses near the rivet location, as this may indicate the reason of failure of the coupons tested at a higher cyclic loading, where failure occurs at the rivet. Moreover, this indicates that the main cause of
crack initiation away from the rivet is not due to residual stresses in the sheet, but indeed residual contact stresses, which leads to fretting.

![Figure 2-10. FEA simulated riveting process verses the actual riveting process.](image)

![Figure 2-11. FEA simulation of the riveting process (a) right after the SPR process (b) after the release of the punch and blank-holder and (c) after the release of die.](image)
Once the final geometry was validated against the experimental cross-section, contact pressure was evaluated and compared to the location of experimentally observed fretting initiated fatigue. Figure 2-12(a) shows the cross-section of the failed specimen tested at a 900 N amplitude, where the bold arrow indicates the point of fatigue crack initiation at a distance of 8.01 mm from the rivet center and Fig. 2-12(b) shows the contact pressure of the simulation after the spring-back. In Fig. 2-12(b) the arrow indicates where the highest contact pressure between top and bottom sheets is located as determined by the FEA. Contact pressure as high as 35 MPa is shown in the area indicated by the arrow. Moreover, the location of the highest contact pressure is located away from the rivet, at 9.05 mm from the rivet center, and correlates well with the fretting region determined experimentally.

**Figure 2-12.** Cross-section of the SPR shows (a) fatigue crack initiation and propagation region in the lap-shear specimens and (b) FEA simulation indicates the contact pressure away from the rivets close to where the fatigue cracks initiated

In order to further validate the location of peak contact pressure as the driving force for crack initiation, linear elastic fracture mechanics was used to predict the number of cycles to failure. The fatigue life calculations assumed that the majority of the fatigue life cycles occurred during the long crack growth regime. The failure mode observed here is a kinked crack, which is similar to numerous published works [67–70]. In this study we employed a modified Paris law
approach, based on stress intensity factor, to calculate the number of cycles to failure. The number of cycles to failure were calculated using a method similar to Newman and Dowling [71], where a kinked crack stress intensity solution for resistance spot welding (RSW) was applied. Lin et al. [67,68] and Jordon et al.[69] used the same approach for friction stir spot welds achieving good agreement with experimental results. Equations (2.3) and (2.4) are modes I and II of global SIF for RSW lap-joint specimen crack under tensile loading [72]:

\[
K_I = \frac{Q}{r^{3/2}} \left[ 0.341 \left( \frac{2r}{t} \right)^{0.397} \right]
\]  
\[
K_{II} = \frac{Q}{r^{3/2}} \left[ 0.282 + 0.162 \left( \frac{2r}{t} \right)^{0.710} \right]
\]

where \( Q \) is the applied load per weld, \( r \) is the weld radius and \( t \) is the sheet thickness. Assuming that the crack is a kinked crack, the SIF in global modes I and II can be correlated to a local SIF as a function of kinked crack angle \( \theta \) [73,74]:

\[
k_I = \frac{k_I}{4} \left[ 3 \cos \left( \frac{\theta}{2} \right) + \cos \left( \frac{3\theta}{2} \right) \right] - \frac{k_{II}}{4} \left[ 3 \sin \left( \frac{\theta}{2} \right) + 3 \sin \left( \frac{3\theta}{2} \right) \right]
\]

\[
k_{II} = \frac{k_I}{4} \left[ 3 \sin \left( \frac{\theta}{2} \right) + \sin \left( \frac{3\theta}{2} \right) \right] + \frac{k_{II}}{4} \left[ 3 \cos \left( \frac{\theta}{2} \right) + 3 \cos \left( \frac{3\theta}{2} \right) \right]
\]

The local stress intensity solutions \( k_I \) and \( k_{II} \) are shown in equations (2.5) and (2.6).

Figure 2-13 depicts [67,74] modes I and II of SIFs related with a kinked crack. Note that the use of SIF assumes that crack initiation is negligible and the SIF remains constant as the crack propagates. To apply a Paris law method to predict the fatigue life of SPR which is under mixed mode loading, an equivalent mode I SIF is used:

\[
k_{eq} = \sqrt{k_I^2 + k_{II}^2}
\]

To account for the R-ratio loading effect [75], equation (2.8) was used:
\[
\frac{da}{dN} = C \left( \frac{\Delta k_{eq}}{(1-R)^{\gamma}} \right)^m
\]  
(2.8)

where \( C \) and \( m \) are material constants obtained from crack growth experiments, \( R \) is the loading ratio (minimum load/maximum load), \( \gamma \) is the loading ratio parameter, and \( \Delta k_{eq} \) is the equivalent stress intensity range. Integrating eq. (2.8), the total number of cycles, \( N_{Total} \), can be calculated:

\[
N_{Total} = \frac{t-t_{crack}}{C \sin \theta} \left( \frac{\Delta k_{eq}}{(1-R)^{\gamma}} \right)^{-m}
\]  
(2.9)

where \( t \) is the sheet thickness. Since the crack starts at the surface of the sheet, \( t_{crack} \) is zero and \( \frac{t}{\sin \theta} \) can substituted by the crack length. Since material constants for AA6111-T4 are not available, the material constants of aluminum 6013-T4 [76] were used. Such substitution for the material constants gave reasonable results in a similar study [77]. The material constants used were \( C = 1.35 \times 10^{-7} \frac{mm}{cycle} \) (MPa m) and \( m = 2.55 \). As \( \gamma \) values can vary from 0 to 1, and are not currently available, \( \gamma \) was assumed to be 0.5.
Figure 2-13. Schematic representation of kinked crack for resistance spot welding.

Figure 2-14 shows comparison of the load amplitude vs. the number of cycles to failure for the experiments and modeling results. As noted earlier, the cycles to failure were calculated assuming the majority of the fatigue cycles occurred during the crack growth regime. It means that the number of cycles for crack nucleation is assumed to be zero for the high cycle regime. Such assumption is made based on that stresses at low cycle fatigue are high enough to nucleate a crack rapidly, and that the fretting wear is high enough nucleate micro-cracks prematurely, drastically shortening the crack nucleation stage (and also the life of the component). Moreover, Ruiz et al. [78] conducted a study on fretting and fatigue, where they detected cracks with lengths of 0.5-1mm (crack length relatively long for this study) due to the severe fretting at stages as low as 21,700 cycles. These results suggest that micro-cracks were nucleated very early in the fatigue lifetimes, thus making the contribution to total life from the nucleation stage
negligible. Therefore the assumption of no crack nucleation in the present study is consistent with results found in literature. The diamond shaped data points represent the experimental fatigue results, arrows indicate run-outs, and the solid line represents fatigue life predicted under high cycle condition, where the $r$ parameter in Equation 2.3 and 2.4 is the distance from the rivet center to the location of high contact pressure predicted by the simulation. The dashed line represents the fatigue life predicted for low cycle conditions. For this condition, a value close to the rivet was chosen, so $r=3.6$ mm. For both cases $\theta$ was assumed to be $90^\circ$, and $a$ was assumed have the value of the sheet thickness $t=3$ mm. As can be observed in Fig.2-14, experimental results correlate very well with the predicted ones, which is within the two predicted life curves for low and high cycle conditions. Note that the low cycle fatigue calculation assumes the fatigue cracks initiated at the rivet.

The good correlation with LEFM model further strengthens the hypothesis that the driving force for the number of cycles to failure under high cycle fatigue is due to fretting wear. Furthermore, this fretting wear is a result of the deformation of the SPR process and the corresponding residual stress that lead to the peak contact surface pressure at locations some distance away from the rivet. Thus, the process modeling performed in this work enable fatigue predictions that more accurately captured the failure mechanisms in the SPR joints.
Figure 2-14. Load amplitude versus number of cycles to failure plot. Comparison between experimental and predicted curves for high and low cycles parameters.
2.5 Conclusions

1. In dissimilar aluminum lap-shear joints produced by the SPR process, two modes of fatigue failure were observed. In the high cycle fatigue regime, the failure occurred in top sheet away from the rivet. In the low cycle fatigue regime, fractured occurred at the rivet.

2. Fretting was observed in lap-shear coupons that were subjected to high cycle fatigue testing. The fretting resulted in fatigue crack initiating away from the rivet and propagating through the top sheet in kinked crack manner.

3. Using finite element methods, the SPR process was successfully simulated to closely match the experimental results.

4. Based on the finite element simulations results, contact pressure away from the rivet head is introduced by the SPR process. The force applied by the top vertical holder and bottom die during the SPR process is sufficient enough to create high contact pressure areas.

5. The residual contact pressure predicted by the simulation results correlates well with fretting locations/crack initiation sites in the high cycle fatigue regime.

6. Fatigue lives were predicted using a kinked crack model based on linear elastic fracture mechanics. Two fatigue curves were generated: one for the high cycle regime (fracture due to fretting initiated fatigue), and one for low cycle regime (fracture occurring close to the rivet).

Comparison of the experimental fatigue and modeling results suggest that under high cycle fatigue, the location of crack initiation is dependent on the process history and the mechanics of fretting wear.
CHAPTER 3

RESIDUAL STRESS CHARACTERIZATION OF SELF-PIECE RIVETING IN MIX-METAL JOINTS

3.1 Abstract

In this study, we preset the results of experimental and modeling characterization of residual stresses in a self-pierce riveting (SPR) magnesium-to-aluminum joint. Experimental residual stress measurements included techniques such as neutron diffraction, X-Ray diffraction, as well as cross-sectional hardness tests. In addition, a numerical model of the SPR process was created in order to simulate the residual stresses in the SPR joint. Better correlation was achieved between the model results and surface X-Ray diffraction and hardness results. However, greater discrepancy was observed between the model correlates and neutron diffraction results. Outcomes of this study indicate that the gage length used in neutron diffraction measurements may be too large, leading to inaccuracies mainly in areas closer to the rivet. Despite the difference in the residual stresses in the magnesium top sheet, the model predicted the trend of experimental results, suggesting that SPR modeling techniques developed in the work to be a good tool for understanding SPR behavior as well as optimizing the joint manufacturing.

3.2 Introduction

Over the past quarter century, governmental regulations around the world have been requiring the transportation industry to improve fuel economy and reduce green house gases emissions. One way of achieving this target is reducing the weight of the vehicles through the
increased use of materials with high strength-to-weigh ratio such as aluminum and magnesium alloys. However, manufacturing technologies to join these non-ferrous alloys are limited. Traditionally, resistance spot welding (RSW) is used to join steel in the automotive industry, however this technique presents several complications in joining aluminum and magnesium alloys [14,15]. These barriers are mainly due to high electrical conductivity and low heat generation in these non-ferrous alloys, requiring elevated welding currents [16,17] resulting in poor weld quality. Among the list of relatively new joining technologies, self-pierce riveting (SPR), have demonstrated to be an alternative method to produce sound joints. In fact, studies have shown that SPR has a comparable strength and superior fatigue performance compared to RSW, and promising results have been shown in peel and shear testing [18].

Self-pierce riveted joints have been studied and implemented by several automotive manufacturers as a cost-effective technique to join aluminum vehicle bodies [45,54,56–58]. The SPR process can be illustrated in four steps, as shown in Fig. 3-1 [25]: (1) the blank holder presses the top and bottom sheets against the die in the clamping stage; (2) the punch drives a semi-tubular rivet into the upper sheet during the riveting process; (3) the material of the bottom plate flows into the die and the rivet shank begins to flare outward, forming a mechanical interlock between the two sheets; (4) lastly, the punch is released and the joint is obtained.
Finite element analysis (FEA) of the SPR process has been extensively conducted over the past years. Porcaro et al. [24] achieved good visual comparisons between the simulations and experimental cross-sections of the joint. Their results were achieved using an axisymmetric model in LS-DYNA with r-adaptivity, a re-meshing technique, and part separation criteria based on sheet thickness. Also, good FEA results were obtained by Casalino et al. [8] employing a failure criteria based on effective strain-to-failure. More recently, Moraes et al. [61] have performed their simulations on magnesium alloy at room and high temperatures by applying an internal state variable (ISV) material model able to capture temperature and strain rate, along with a damage model based on void growth. In addition, Huang et al.[13] used the same ISV modeling approach, with maximum shear strain as the failure criteria. In this study, aluminum-to-steel joining was simulated and compared with the neutron diffraction results achieving
reasonable agreement. Several other studies with acceptable results were conducted, such as applying a Gurson–Tvergaard damage model as the failure criteria [7,62]. It is important to note that the SPR is a dynamic process, therefore the FEA is usually performed under an explicit formulation, while the springback is generally performed in implicit mode to balance stresses [24,4,62]. However, the springback calculations using explicit analysis with mass damping have also been performed and achieved reasonable results [13,61].

During the SPR process, the rivet and sheets undergo high plastic deformation [79]. The plastic deformation on the top sheet (first to come in contact with the rivet) is so high that fracture occurs, making the piercing process possible. Such complex and large plastic flow may result in high residual stresses. Residual stresses and strain hardened materials have strong influence on mechanical properties, and while not assessed explicitly, it is thought that residual stress can affect the fatigue response [80–83].

A few studies have been carried out characterize the SPR joints using neutron diffraction. Haque et al.[9,2] showed the feasibility of measuring residual stresses of SPR joints of aluminum-to-steel and steel-to-steel despite the small dimensions of a SPR joint. Later, Haque et al.[84] measured residual stresses on a steel-to-steel joint. More recently, Huang et al. [13] experimentally measured the residual stress using neutron diffraction on a dissimilar aluminum-to-steel SPR joint. In addition, they conducted a FEA simulation and correlated the residual stresses calculated numerically with the neutron diffraction results achieving partial agreement. However, to the best of the authors’ knowledge, there is no published study on residual stress measurements of dissimilar magnesium-to-aluminum SPR joints. Here, we characterize the SPR magnesium-to-aluminum joint using neutron diffraction, X-ray diffraction and hardness profile correlation to three dimensional finite element simulation results.
3.3 Materials and Methods

3.3.1 Materials

Figure 3-2 shows a schematic representation of the SPR coupon. The total joint length is 90mm long and 30 mm wide. The top sheet material is cast magnesium alloy AM60B alloy and the bottom sheet is aluminum alloy 6082-T4, the thickness of each sheet is nominally 3.1 mm. The direction nomenclatures (i.e. longitudinal direction (LD), transverse direction (TD) and normal direction (ND)) adopted in the present work are shown.

![Diagram of SPR specimen](image)

Figure 3-2. Schematic of the SPR specimen. Dashed lines represent the gage volume used in neutron diffraction measurements.
3.3.2 Neutron Diffraction

In order to quantify the residual stress profile within of the dissimilar Mg-Al SPR joint, neutron diffraction measurements were accomplished using the strain-scanning diffractometer on the HB2B beam line at High Flux Isotope Reactor (HFIR) at the Oak Ridge National Laboratory in Oak Ridge, TN. Figure 3-3 shows a representative neutron diffraction set-up. A monochromatic beam of thermal neutrons (wavelength 1.728 Å for Al and 1.885 Å for Mg) from the incident beam is directed to the SPR coupon, such that the beam is diffracted and captured by the detector located on the perpendicular direction of the incident beam. Diffraction planes for Al \{311\} and Mg \{112\} were chosen for the present investigation. The counting times varied from 1800 to 5600 seconds depending on material, direction and neutron counts (minimum of 900). Measurements of strains in the longitudinal (LD), normal (ND) and transverse (TD) directions were made on both sides of the rivet. Measurements began 4 mm away from the rivet center and progressed 0.5 mm per step for 6 mm in the LD. Due to time constrains only measurements were taken between 10 to 14 mm and progressing 2 mm per step. Even though the counting times were relatively long for each measurement, the gage volume for LD and ND was relatively large having dimensions of 1.5 mm x 10 mm x 1.5 mm, with the geometric center on the middle plane of width and thickness of each sheet. Note that the 10 mm dimension is parallel to the TD depicted by the rectangle shown as dashed lines in Fig.3-2a, and the diamond shape as dashed lines, with 1.5 mm length shown in Fig.3-2b. For the TD measurements, the gage volume was smaller due to favorable grain orientation for diffraction, thus the gage volume was defined as 1.5mm x 2mm x 1.5 mm. The stress free d-spacing, \(d_0\), was measured near a free corner of each sheet, which is a significant distance from the rivet.
Elastic strains in the longitudinal, transverse, and normal directions were calculated from the neutron diffraction results using the pseudo-Voigt fit for the diffracted peaks. Even though strains for each of the six directions are required to define a strain tensor, a common assumption is that the measured directions coincide with the principal directions. Therefore, stresses in the three measured directions were calculated using Hooke’s law shown by Equations 3.1-3, which are the corresponding expressions for \( \sigma_x \), \( \sigma_y \) and \( \sigma_z \) as functions of \( \varepsilon_x \), \( \varepsilon_y \) and \( \varepsilon_z \):

\[
\sigma_x = \frac{E}{(1+\nu)(1-2\nu)} \left[ (1 - 2\nu) \varepsilon_x + \nu(\varepsilon_y + \varepsilon_z) \right] \tag{3.1}
\]

\[
\sigma_y = \frac{E}{(1+\nu)(1-2\nu)} \left[ (1 - 2\nu) \varepsilon_y + \nu(\varepsilon_x + \varepsilon_z) \right] \tag{3.2}
\]

\[
\sigma_z = \frac{E}{(1+\nu)(1-2\nu)} \left[ (1 - 2\nu) \varepsilon_z + \nu(\varepsilon_x + \varepsilon_y) \right] \tag{3.3}
\]

where \( E \) is the elastic modulus (69 GPa for aluminum and 45 GPa for magnesium), and \( \nu \) is the Poisson’s ratio (0.33 for aluminum and 0.35 for magnesium).
3.3.3 X-ray Diffraction

In order to characterize residual stresses on the surface of the SPR joint, X-ray diffraction measurements were performed and Bragg’s law was employed to calculate the lattice spacing. The proto iXRD residual stress diffractometer used for taking the measurements is shown in Fig.3-4a. The x-ray beam used a cobalt kα tube, and operated at a voltage of 20kV and a current of 4mA. The \{hkl\} plane used for diffraction was Al \{311\}, with 2θ angle being approximately 149°. A 2 mm circular aperture was chosen, with exposure time of 12 seconds. The strains were
measured using the standard $\sin^2\psi$ method for x-ray residual stress[85], and the residual stresses were calculated using linear elasticity. A schematic of a sample diffracting the incident X-ray beam to the two detectors is drawn in Fig. 3-4a. The measurements were taken starting 7 mm from the center of the rivet and ending at 17 mm on the LD direction of the bottom sheet. Measurements were taken from the center of the bottom sheet to its edge on the TD, with 2 mm spacing between points. Figure 4b shows schematically the locations of the XRD measurements on the SPR coupon.

![Figure 3-4. X-ray diffraction setup: a) X-ray residual stress diffractometer, b) bottom surface of the bottom of the SPR joint with the X-Ray diffraction measurements locations.](image)

3.3.4 Hardness Measurements

In order to evaluate work hardening across the joint, a SPR coupon was sectioned through the centerline, mounted in cold epoxy and polished. The final polishing was done with 1µm diamond paste, ultrasonic cleaned using ethanol so the magnesium sheets do not oxidize leading to inaccurate hardness readings. For the hardness generation, the AMH43 automated
hardness with diamond Vickers indenter was used. A load of 300g was applied with a dwell time of 13 seconds. The spacing between each indentation was 300µm. In order to avoid inaccurate hardness readings due to plasticity of a neighbor indentation, it was assured the distance between indentations exceeded more than 2.5 times the diagonal of an indent.

3.3.5 Numerical Simulations

Numerical simulations were performed using the commercial solver ABAQUS v6-14-1. Since SPR is a dynamic process, the explicit solver was chosen. Due to apparent symmetry around the punch and die axis, SPR joints are usually treated as symmetrical around this axis (axisymmetric). Numerous studies have been done on SPR process simulations using 2D axisymmetric elements [24,25,7,8]. Nonetheless, 3D elements with reduced integration were used in order to take benefit of a better contact formulation included in ABAQUS (named General Contact). Using this kind of contact formulation, new exposed surfaces are automatically defined when elements are removed from the simulation, making it possible to have contact between the rivet and top sheet, or even between newly exposed top sheet elements themselves, specifically during the piercing process. The elements of the punch and rivet axis are C3D6, and the other elements are hexahedral with reduced integration C3D8R. As the elements are relatively small and undergo high plastic deformation, distortion control was used throughout the simulations. Mass scaling was carefully applied to accelerate the run time, where the energy added by mass scaling never reached more than 5% of the total internal energy of the model.

Due to the high number of elements, model calibration can require extensive time and effort. To overcome this barrier, two models were created: one model for calibration, which was symmetrical to the punch and die axis (Fig. 3-5a), and second half model with a plane of
symmetry parallel to the LD (Fig. 3-5b). The model used for calibration is defined with different element sizes: rivet elements are 0.1 x 0.1 mm on average; and the sheet elements on areas subjected to high plastic deformation are 0.025 x 0.025 mm; on X-Y plane. The meshes of the sheets gradually become coarser as it gets away from the area of higher deformation. Depth of the element depends on how far the element is from the Y-axis, as the mesh was built by sweeping 5 degrees around the Y-axis. As a result, the model appears to have a “wedge” geometry. Proper boundary conditions were applied to achieve axisymmetric analysis and the initial mesh configuration of the model used for calibration as shown in Fig. 3-5a. The model that was further analyzed and used in comparisons with results from neutron and X-ray diffraction analyses is shown in Fig.3-5b. This model was built using the initial configuration of the wedge model, swept 180°. In addition, extensions of sheets were created with a coarse mesh with average element size of 2 mm x 0.5 mm x 2 mm and connected to rivet portion of the mesh, resulting in a lap-shear coupon with a plane of symmetry along the longitudinal direction of the coupon. An isotropic material hardening model was used for both the sheets and the rivet. Moreover, the materials defined for the sheets were strain rate sensitive. Stress-strain curves for the various materials were obtained from literature: AM60 (top sheet) quasi-static and strain rate of 800/s [86]; aluminum alloy 6082 (bottom sheet) at a strain rate of 3900/s [87] and quasi static [88]. Only two curves were selected per material since Abaqus can interpolate between these curves for different strain rates. Also, the curves used here were the highest strain rates for each material found in the literature by the authors, and it is expected that these strain rates represent the upper limit of the strain rate during the SPR joining process. For the rivet material, the initial strain hardening slope was taken from Van Hall et al. [89], where the strain-to-failure was reported as 2.86%. The rivet material was defined with a yielding point of 1094 MPa and 1590
MPa at 8% strain. The die and punch were defined as rigid bodies, while the blank-holder was modeled as purely elastic with a very high elastic modulus, which allows it to be nearly as stiff as a rigid body, which then allows the pressure loads to be defined and applied on its top surface easily. In order to allow the rivet to pierce through the top sheet, elements were deleted according to the Johnson-Cook damage criterion. The damage evolution was set to rapidly decrease the energy of the element before its removal from the simulation, avoiding numerical complications due to energy equilibrium. Equations (3.4) and (3.5) are the governing equations of Johnson-Cook damage criterion implemented in ABAQUS[90]. Complete damage occurs when the damage parameter described in Eq.3.4, \( \omega \), reaches unity:

\[
\omega = \sum \left( \frac{\Delta \varepsilon^{pl}}{\varepsilon_f^0} \right)
\]  

(3.4)

where \( \Delta \varepsilon^{pl} \) is an increment of plastic strain, \( \varepsilon_f^{pl} \) is the strain at damage initiation; and the summation is performed at each increment of the analysis. \( \varepsilon_f^{pl} \) is defined in Equation (3.5) as:

\[
\varepsilon_f^{pl} = \left[ d_1 + d_2 \exp \left( d_3 \frac{p}{q} \right) \right] \left[ 1 + d_4 \ln \left( \frac{\varepsilon_f^{pl}}{\varepsilon_0} \right) \right] (1 + d_5 \hat{\theta})
\]  

(3.5)

Constants \( d_1-d_5 \) are empirical failure parameters, \( \varepsilon_0 \) is the reference strain rate, \( \hat{\theta} \) is the non-dimensional temperature, \( p \) is the pressure stress and \( q \) is the Mises stress. Parameter \( d_1 \) is a constant, while parameters \( d_2 \) and \( d_3 \) are related to stress triaxiality, allowing for the deletion of elements at different strain values, depending on stress state. Even though \( d_4 \) is related to the strain rate, it was defined as zero for the sake of simplicity when calibrating the model. The constant \( d_5 \) is related to temperature, and it was also set to zero in this study because the material formulation used is not sensitive to temperature. Damage parameters and friction coefficients
between all parts contained in the model were calibrated via trial and error through visual comparison between simulation and experimental cross-section of the SPR joint.

Figure 3-5. Initial mesh configuration used in FEA modeling: a) axisymmetric “wedge” model used in the calibration stage; b) half 3D model with middle symmetry used for comparison with experimental results.

Once the calibration was finished, all the parameters used during calibration were used to simulate the half-plane 3D model. In order to calculate the residual stresses, a spring-back simulation was performed after the SPR process. This stage was performed using the explicit code in order to keep contact histories intact. This type of spring-back was achieved by releasing the punch, blank-holder and die, and applying viscous pressure load to damp dynamic oscillations to achieve equilibrium. After the spring-back process, the simulation results were compared to stresses obtained from the neutron diffraction and XRD experiments. To compare the simulation results to neutron diffraction results, the stresses for each direction of all nodes within a defined volume were averaged for each measurement position. Note that the volume
was defined exactly like the gage volume used in the neutron diffraction measurements. To compare with XRD measurements, a square of 2mm was defined on the bottom surface of the bottom sheet, for each measurement position, so that the stresses of the nodes within that area were averaged in each stress direction.

To validate the plastic deformation calculated using FEM, the yielding surface was based on an equivalent plastic strain after the SPR process. The yielding surface was defined using the quasi-static stress-strain curve of the selected material at room temperature. Cahoon et al. [91] correlated the yielding stress to hardness values for brass, steel and aluminum. In their study, equation (3.6) was used for such correlation:

$$\sigma_{yield} = \frac{H}{3} (B)^{(m-2)}$$

(3.6)

where $\sigma_{yield}$ is the yielding stress in [Kg/mm$^2$], $B$ is a constant, usually defined as 0.1 for steel and aluminum, $H$ is the diamond pyramid hardness, and $m$ is Meyer’s hardness coefficient. Cahoon et al. [91] also showed evidence that $n=m-2$, where $n$ is the strain hardening coefficient from the stress-strain relationship $\sigma = K \varepsilon^n$, where $\sigma$ is the stress, $K$ is a constant and $\varepsilon$ is the true strain. Rearranging eq.(6), and converting the units of stress to [MPa], and using the suggested value $B=0.1$, the hardness values can be obtained from the yielding surface from the simulation using eq.(3.7):

$$H = \frac{3\sigma_{yield}}{9.8665(0.1)^n}$$

(3.7)
3.4 Results and discussion

Figure 3-6 shows calculated elastic strains obtained from neutron diffraction measurements. Such strains were obtained using the pseudo-Voigt function to fit the diffracted peaks and to determine the peak centroid. Figure 6a shows the calculated strains in each direction in the aluminum alloy sheet. The horizontal axis of the plot represents the distance from the rivet center on the longitudinal direction shown in Fig. 3-2. Figure 3-6b shows the calculated elastic strains obtained from neutron diffraction measurements for the magnesium alloy sheet. It is important to note that the strains at the same distance from the rivet center are not symmetric. This asymmetry may be due to several reasons: first, the sample might not be completely flat (the gage volume lays at different positions on the normal direction not coinciding with the mid-plane of the sheet thickness); second, the materials in the joint are not isotropic due to rolling; third, the die and punch axis may not be coincident. It is also noted that the riveting process leaves circular marks on top and bottom surfaces due to high pressure of blank-holder, punch and die. These marks are not symmetric on either the sides of the sheet as observed in Fig. 3-4b where this mark is much more prominent on the aluminum side of the joint, which is close to the bottom left side of the rivet. Such off-set markings suggest that asymmetrical deformation existed due to slight tilting during riveting. In fact, this asymmetry of the joint was noted in previous analyses [92], and further confirmed by neutron diffraction results that exhibited asymmetric residual stress profiles [9,84,93]. This asymmetry will be discussed later in the study.

Figure 3-7 shows stress contour plots of experimentally X-Ray diffraction results superimposed on the surface image of the aluminum side of the SPR joint. It is noted that a large stress gradient is exhibited. This large gradient in residual stress results is likely due to
measurement uncertainty and material texture due to the rolling process. Figure 3-7a shows the stresses on the direction parallel to the longitudinal direction of the aluminum alloy sheet. Likewise, Fig. 3-7b shows the stresses on the transversal direction for the aluminum alloy.

Figure 3-6. Strain profiles obtained from neutron diffraction results of: a) bottom sheet (aluminum), and b) top sheet (magnesium)
Figure 3-7. Contour plot of stresses on the aluminum side of the joint obtained from X-Ray diffraction results: a) Stress on the longitudinal direction, b) stress on the transverse direction.

Regarding the SPR simulation, a visual validation of the non-linear finite element simulation of the riveting process is shown in Fig. 3-8. The half symmetry simulation after spring-back is superimposed over a cross-sectional image of a physical SPR joint. It is important to note that the simulation model assumed a symmetric riveting process and isotropic material response. However, it is clear from Fig. 3-8 that experimentally, the rivet exhibited asymmetrical deformation as shown by the off-set shape of the rivet. We further note that the left side of the cross-section was used in the calibration of the FE model. As such, we acknowledge that the simulation is in better agreement on the left side of the image shown in Fig. 3-8 and shows greater error on the right side of the image. Based on the calibration of the FE simulation,
the friction coefficients from the wedge model were determined to be 0.15 for all surfaces except for 0.4 for the punch/rivet interaction; damage constants from Eq. 3.5 were defined as: \( d_1 = 0.1 \), \( d_2 = 1.6 \) and \( d_3 = 1.3 \).

Figure 3-9 shows the evolution of von Mises stress from the SPR FE simulations before and after spring-back. Figure 3-9a shows the contour plot right before the end of the SPR process. On the other hand, Fig. 3-9b depicts von Mises stress contour plot after the spring-back simulations. It is worth noting that while the stresses decrease drastically after springback, residual stresses are still evident in the joint as shown in Fig 3-9b.

Figure 3-8. Visual comparison of the simulation results (green lines) superimposed over the experimental cross-section. Note only the left side of the comparison was used for model calibration.
a) Finishing the piercing process

b) After spring-back

Figure 3-9. Von Mises stress profile: a) right before the end of the piercing process, b) after spring-back. Units in MPa.

Figure 3-10 shows the hardness profile of the SPR magnesium-to-aluminum joint. Figure 3-10a shows the experimental measured hardness profile. The measurements were divided in two contour plots because the hardness values of the rivet are much higher than the ones of the sheets, so the rivet and sheets were plotted separately with different scale bars for better visualization. Using Eq. 3.6, the Vickers hardness was estimated from the yielding surface, for quasi-static and room temperature, based on the equivalent plastic strain of the simulation results after the SPR process. No published studies have predicted the yielding surface of magnesium well, based only on hardness. Since, the equation proposed by Cahoon et al. [91] does not lead to reasonable results due to the high values of the strain hardening coefficient of AM60, only the aluminum hardness simulation results are shown as depicted in Fig. 3-10b. The value of the strain
hardening component, used in the simulation for aluminum AA6082, \( n \), was 0.043139, In addition, Fig.3-10b shows the comparison of experimental hardness values for the aluminum bottom sheet on the left, and the calculated results from the simulation on the right. This visual comparison indicates that the simulation accurately captured the plastic deformation, which occurred during the SPR process.

Figure 3-10. Hardness profile of the self-pierce riveting joint: a) full profile of the rivet and sheets, b) comparison of the experimental hardness of the bottom sheet(left) and the calculated hardness obtained from numerical analysis (right).
Figure 3-11 shows a quantitative comparison between XRD and the FE simulation results by means of direct, point-by-point correlation. Each value of stress at different locations was plotted, where the horizontal axis represents the XRD values of stress and the vertical axis represents the stress values from the simulation. An “ideal” line is drawn as a reference at a 45 degrees angle. Thus, if the results obtained experimentally and numerically are coincident, the corresponding point will lay on the 45 degrees angle. Figure 11a shows the comparison of stresses on the longitudinal direction, where generally, good agreement was found between the simulation and experimental results even if the stress gradients within a relatively small area are high. Figure 3-11b shows the comparison for the stresses on the transverse direction, where a good correlation between the simulation and experimental results are also observed. Surprisingly the stresses close to the symmetry plane were very high, so elements up to 4mm away from the symmetry plane were not shown in the plot.

![Figure 3-11. Quantitative comparison of residual stresses from the simulation and X-Ray diffraction results: a) Stress on the longitudinal direction, b) stress on the transverse direction.](image-url)
Figure 3-12 shows the experimental neutron diffraction results compared to the residual stresses obtained from the FE simulation results. As noted previously and also shown in studies elsewhere [9,84,93], residual stresses measured by neutron diffraction are not symmetric about the rivet center. Figure 3-12a shows the residual stresses in the aluminum alloy sheet, and it is observed that it generally correlates very well with experimental results on the right hand side of the plot. Stresses in TD and LD also correlate well with measurements greater than 5 mm from the rivet center, where the edge of the rivet begins at 4mm. Normal direction stresses differ marginally from the experimental values, however they accurately follow the same trend as the experimental ones (i.e. same general shape but shifted slightly compressive). As the general trend of the simulation and experiments is similar, the difference may be due to several factors. First, the pressure applied on the blank-holder, which directly affects stresses on the normal direction, experimentally may have been lower than the pressure set for blank-holder in the simulation. However, we were unable to confirm the blank-holder pressure for the riveting process used in this study. Another possible explanation for the error may arise from micro-cracks that develop in the rivet and sheets due to the piercing and extreme deformation process, which can locally change the stress state, and may have occurred experimentally but are not captured presently by the model due to their microscopic size. Additional possible contribution to the error observed is related to the element deletion during the simulation of the SPR process. Even though the elements are relatively small, removal of elements due to damage during the numerical simulation changes the total mass in the calculation, and such change could be sufficient to alter the stress states. Experimentally the riveting process is not isothermal, and most of the friction and plastic flow energies are converted into heat during the piercing process, creating large thermal gradients. These large thermal gradients are created because the rate of
deformation in the SPR process causes local heat generation to occur faster than material can dissipate it. Thus, such thermal gradients may induce residual stresses that are not taken into account in the simulation. Finally, it is noted that the error could be related to slight misalignment of the dies during riveting which was not modeled in the numerical simulation.

Another additional cause of the discrepancy in the comparison of the simulations and the experimental results could be related to the conversion of the diffraction peaks to elastic stresses. In fact, the fitting for the magnesium diffracted peaks proved to be challenging, and thus, the asymmetric fitting using the Mg \{201\} plane instead of the Mg \{112\} was attempted with no further improvement in the comparison. Using the Mg \{112\} diffracted peaks, the stress values comparing experiment and numerical simulation are different, however the general trend of the simulation and experiments is similar, and the difference may be due to the reasons previously mentioned. The authors are aware that the gage length used in the neutron diffraction measurements might limit the spatial resolution of the measurements, as well as the standard deviation when having such a steep stress/strain gradient. However, even with a relatively large gage volume, long counting times were accomplished in order to reach a reasonable neutron count; therefore, a smaller gage volume would not have been feasible.
Figure 3-12. Residual stress comparison between numerical model and neutron diffraction results: a) bottom sheet (aluminum), b) top sheet (magnesium).
3.5 Conclusions

The present study attempted to characterize residual stresses and strain hardening due to the riveting process in magnesium-to-aluminum SPR joints. This comprehensive study included advanced experimental stress characterization such as neutron and X-ray diffraction, and hardness mapping with comparison and validation with SPR process simulations. A summary of the key findings are presented:

1. Neutron diffraction results show that the stresses are asymmetrical about the center of the rivet. This may be due to anisotropy of the materials and misalignment of the riveting process with respect to the die. This is confirmed by visual asymmetry of the cross section and also by asymmetric marks caused by the punch, holder and die.

2. The non-linear explicit finite element SPR model developed in this study predicted the geometry of the experimental cross section with generally good results.

3. A hardness profile was determined experimentally showing very high plastic deformation around the rivet and in both the aluminum and magnesium sheets. Hardness values were estimated using the yielding surface taken from the FE simulations and the hardening exponent of aluminum sheet. The estimations form the FE simulations correlated well with experimental results.

4. The XRD results show high stress gradients on the surface of the aluminum sheet suggesting possible influence of the sharp texture due to the rolling process. Numerical results agree generally with XRD results in both longitudinal and transverse directions.
5. Numerical results partially agree with neutron diffraction results. The model prediction mostly agrees with experimental results in the transversal and longitudinal directions in the aluminum sheet, and at positions farther than about a rivet diameter away from rivet center. However stresses predicted in the normal direction differ significantly from the experimental measurements, yet still follow the general trend. Comparing stresses in the magnesium alloy, the model follows the trend of the experimental results, but the magnitudes of the stress do not agree well.

6. The FE simulations achieved good agreement with the hardness and XRD (small gage area) experiments, and partially predicted the neutron diffraction results. There are several reasons the simulations only partially correlated with the neutron diffraction measurements: the gage length of neutron diffraction measurements might be too large leading to inaccurate results; micro-cracks and thermal analysis were not included in the model; the model eliminates elements when a damage threshold is reached, which changes the total mass of the joint. Finally, there is likely some error associated with the fit the diffraction peaks for the magnesium alloy due to the many deformation planes.

7. Even though the material model chosen for the FEA is isotropic, and the numerical model assumes symmetry around the punch axis, most of the experimental measurements were generally predicted with good results.

8. This study indicates that the SPR model developed in this study coupled with experimental approaches can be used for joint optimization and development. For even greater accuracy, the numerical calculations could employ an anisotropic
material formulation sensitive to temperature, as well as a slight incline of the
punch axis, to represent the real experimental results that displayed slight asymmetry.
4.1 Abstract

The present study attempts to elucidate the role manufacturing history on the mechanical performance of self-pierce riveted (SPR) joints. The SPR process was numerically simulated using a non-linear finite element model coupled with the Johnson-Cook damage model. Results of the FE simulation compared closely to cross-sectional images of experimental SPR joints. The FE simulation results suggest that the strain hardening due to the piercing process is the dominant cause for joint strength under quasi-static condition. Aspects of cyclic loading were modeled including crack initiation, propagation, fretting and number of cycle to failure and compared to experimental results. Results from the study suggest that residual stresses and plastic strain may change the crack location and initial crack plane propagation, leading to more accurate results when included in finite element analysis of SPR joints. Including these components also altered the predicted fretting locations leading to better agreement with experimental results. Lastly, linear elastic fracture mechanics were performed to estimate fatigue lives based on initial crack location and angle of crack plane. The estimation of the number of cycles to failure that included residual stresses and strains yielded good correlations compared to the calculations that excluding residual stresses and strains.
4.2 Introduction

Due to the increase use of high strength-to-weight alloys in the automobile body structure, such as magnesium and aluminum alloys, several studies have been performed on alternative joining techniques of non-ferrous materials since the traditional resistance spot welding (RSW) used in steels is challenging [1,2]. There are several relatively new joining techniques, including self-pierce riveting (SPR) that are a promising method to produce sound joints. Moreover, it has been shown that SPR may have comparable strength and superior fatigue properties compared to RSW, in addition to favorable results in peel and shear testing[3].

In order to evaluate and understand the SPR joining process, several studies have been carried out experimentally and numerically. From the numerical point of view, Porcaro et al. [10], used 2-d axisymmetric finite element simulations with re-meshing and top sheet separation based on sheet thickness. Their results were visually compared to experimental cross-sections achieving good agreement. Many other studies were performed achieving good results with different material formulations such as: internal state variable sensitive (ISV) which is sensitive to strain rate and temperature[11,12] and Johnson-Cook [13], as well as failure criteria based on effective strain-to failure [14], void growth [11], maximum shear strain [12] and Gurson–Tvergaard damage model [15,16].

Regarding quasi-static mechanical performance of SPR, Atzeni et al.[15] accurately predicted the shear resistance of an aluminum-to-aluminum SPR joint including residual stress and strains due to the SPR process itself. Mori et al. [19] simulated the process using axisymmetric elements and transferred the results to a 3-D mesh, achieving good comparison to experiments in cross tension tests. Transferring results from 2-d to 3-d in order to perform shear, coach-peel or cross-tension simulations while achieving good agreement with experimental
results was accomplished in several studies [20,21]. Bouchard et al. [22] performed simulations on SPR process using 2-D axisymmetric elements in three different stacking orientations. In this study shear tests were performed and compared to simulations with and without the results transferred from the SPR process simulation. It was found that the simulated joint, where residual stresses and strain hardening history were not included, exhibited a lower peak strength compared to the same geometry with residual stresses and strain history included. Similar results comparing joints with and without residuals was reported by Porcaro et al. [23]. These results highlight the importance of including the residual stresses and deformation history when simulating the mechanical performance of the joint.

Regarding fatigue performance, Kang and Kim [24] created SPR models of tensile-shear, coach-peel and cross tension based on experimental geometry, however, no residual stresses and strains due to the manufacturing history were included. In their study maximum principal, Mises and SWT (Smith–Watson–Topper) fatigue parameters did not show a good correlation compared to experimental results. However, they found good correlation between experimental and calculated number of cycles to failure using stress intensity factors (SIF). It is worth noting that SIF where calculated and plotted versus number of cycles to failure from experimental fatigue tests of SPR but did not use the SIF to calculate the crack propagation rate. In additional Iyer et al. [25] modeled the SPR under lap-shear configuration to predict fretting locations. However, their study did not include residual stress and strains due to the SPR process. Their results suggest that history variables from the SPR process may be critical to predict crack initiation sites. Huang et al. [26] observed eyebrow cracks with a semi-elliptical crack front in experimental results of SPR under lap-shear configuration. In their study, new SIF equations were derived for a semi-elliptical surface in a finite plate near a rigid cylindrical inclusion with
axial bending force and shear moment. Their model was calibrated with experimental fatigue results of SPR, and an empirical linear geometry factor was used. Moreover, a plastic zone correction was applied since residual stresses and strains due to SPR process were not included.

Despite the few studies published in literature on the effect of including the deformation history in the simulation of the macro behavior of the SPR joint, several knowledge gaps still exist. First, the role of work hardening and residuals stresses has not been explicitly elucidated. Second, the role of deformation history on simulating the fatigue behavior is largely unknown. As such, in this paper, we evaluate the effects of residual stresses and work hardening on simulations of mechanical performance of a dissimilar magnesium-to-aluminum alloy joint, including quasi-static lap-shear and fatigue behavior. In particular, we analyze the effect of deformation history on fretting, fatigue crack initiation, fatigue crack propagation, and the number of cycles to failure.

4.3 Materials and Methods

4.3.1 Materials

Figure 4-2 shows a schematic representation of the SPR lap-shear coupon. The total joint length is 90mm long and 30 mm wide. The top sheet material is cast magnesium alloy AM60B alloy and the bottom sheet is aluminum alloy 6082-T4, the thickness of each sheet is nominally 3.1 mm.
4.3.2 Mechanical testing and fracture analysis

Displacement controlled lap-shear tests were performed on servo hydraulic frames in order to obtain quasi-static load-displacement curves. The fatigue tests were performed under load control at 40 Hz frequency at a load ratio of R=0.1. Shims were used on both ends of the lap-shear coupons to reduce the bending of the samples during testing. The grip-to-grip distance for fatigue and tensile pull tests was 50 mm. To visually compare the FEA model to experiments, untested samples were cross-sectioned, cold mounted in epoxy, ground and polished in order to obtain a representative cross-section. The macrographs of the samples were performed using a Keyence VHX100 digital optical microscope. For fracture surface analysis, the samples were analyzed under Jeol 7000 series scanning electron microscopy (SEM).

![Figure 4-1. Geometrical features of the SPR test coupon.](image)
4.3.3 Numerical simulations

Many commercial solvers are available and have proven to be suitable for SPR simulations [15,22,27–29]. However, ABAQUS (Version 6.14-1) was chosen as the solver in this study, due to its extended use in fatigue. Since the SPR process happens in milliseconds, it is typically considered a dynamic process, therefore the explicit solver was used. Since the SPR joint presents apparent symmetry, it is usually considered to be a process symmetric to the punch-die axis (axisymmetric). In order to reduce computing cost (time), many of the studies on SPR process found in the literature employ 2-d axisymmetric elements [10,9,15,14]. However, if 2-d axisymmetric elements and explicit solver are used, ABAQUS does not redefine new exposed surfaces after element deletion. Moreover, for this kind of problem only surface-to-node contact formulation, which may be inaccurate. To overcome this issue, 3-d elements were adopted, where a better contact formulation can be employed. This contact option in Abaqus redefines automatically newly exposed surfaces after elements are eliminated from the calculation. Note that elements should be eliminated in order to allow piercing of the rivet through the top sheet. The elements of the punch and elements on the axis of symmetry are triangular prisms defined as C3D6, while the rest of the elements are hexahedral with reduced integration of C3D8R type. To achieve high accuracy, the mesh where high plastic deformation is expected is relatively fine. Since these elements may get distorted and re-meshing was not employed, distortion control was adopted. To decrease computational time mass scaling was carefully applied, and the energy added was always lower than 5% of the total internal energy of the model.

In order to further decrease computational time, different mesh sizes were used in the SPR process simulation. On the X-Y plane, rivet elements are 0.1 x 0.1 mm on average, while
the mesh of the sheets are biased, having 0.025 x 0.025 mm elements in areas where high plastic deformation is more likely to occur, and with larger elements far from these areas. A 3-D mesh was created sweeping the mesh 5 degrees on X-Y plane previously described, resulting in a “wedge” model. To assure axisymmetry around the die/punch axis, proper boundary conditions were applied. The initial mesh configuration of the model is shown in Fig. 4-3. Regarding the material model, an isotropic hardening model was chosen. In addition, strain rate sensitivity was added to material definitions of the sheets. Stress-strain curves obtained from literature were used to calibrate the material model. For AM60 (top sheet), quasi-static and strain rate of 800/s [30] curves were used; while for the aluminum alloy 6082 (bottom sheet) at a strain rate of 3900/s [31] and quasi static [32] were employed. No data was found in the literature regarding the steel rivet material used in the present study, therefore rivet material properties were estimated. The initial strain hardening slope was obtained from the steel rivet material tested by Van Hall et al. [33], however the strain-to-failure was reported as 2.86% in their study. In the presented study, the rivet material was defined with a yielding point of 1094 MPa, with the same slope reported by Van Hall et al. [33] up to 2.86% strain, and 1590 MPa was defined at 8% plastic strain. Since the die and punch are relatively stiff parts, they were defined as rigid bodies for computational efficiency. Moreover, the blank-holder was defined as purely elastic, with a very high elastic modulus. This is because that rigid bodies are defined as a set of nodes, which are dependent on one point/node, therefore the force should be applied on only one node. For the sake of simplicity, the blank-holder was defined as elastic, so the clamping pressure can be defined directly on its top surface.

Since the rivet needs to pierce through the top sheet in order to form the joint, elements of the top sheet were defined with the Johnson-Cook damage criterion, which allows elements to be
deleted. Once the Johnson-Cook damage criterion is achieved, the damage evolution parameter must be achieved to delete the element from the calculation. The damage mode was defined to quickly decrease the energy of the element to be deleted, avoiding numerical complications due to energy equilibrium.

The Johnson-Cook damage criterion implemented in ABAQUS[34] is described in equations (4.1) and (4.2). Complete damage occurs when the damage parameter described in Eq.1, \(\omega\), reaches unity:

\[
\omega = \sum \left( \frac{\Delta \varepsilon^{pl}}{\varepsilon_f^{pl}} \right)
\]  

(4.1)

where \(\Delta \varepsilon^{pl}\) is an increment of plastic strain, \(\varepsilon_f^{pl}\) is the strain at damage initiation; and the summation is executed at each increment of the analysis. \(\varepsilon_f^{pl}\) is defined in Equation (4.2) as:

\[
\varepsilon_f^{pl} = \left[ d_1 + d_2 \exp \left( d_3 \frac{p}{q} \right) \right] \left[ 1 + d_4 \ln \left( \frac{\varepsilon_f^{pl}}{\varepsilon_0} \right) \right] (1 + d_5 \theta)
\]

(4.2)
Figure 4-2. Initial mesh configuration used in the finite element simulations.

Constants $d_{1-4}$ are empirical failure parameters according to stress state, strain rate and temperature, $\varepsilon_0$ is the reference strain rate, $\Theta$ is the non-dimensional temperature, $p$ is the pressure stress and $q$ is the Mises stress. Parameter $d_1$ is a constant, and if no other parameters are defined, $d_1$ will be the equivalent strain to failure of the element, parameters $d_2$ and $d_3$ are related to stress triaxiality, which allows the deletion of elements at different strains, depending on the stress state. The constant $d_4$ is related to the strain rate, however it was defined as zero for the
sake of simplicity for model calibration. The constant $d_5$ relates the temperature to the strain at failure, and it was also defined as zero since the material model chosen is not sensitive to temperature.

Friction and damage parameters were calibrated through visual comparison between the final configuration of the simulated joint and experimental cross-section, spring-back was not performed during the calibration process since it does not significantly change the final shape of the joint [25]. Since the elements used in the explicit analysis are relatively small, and a few might be distorted after the SPR process, these elements are not recommended to be included in an implicit analysis. Therefore the final configuration of the SPR joint was entirely re-meshed to a coarser mesh and the residual stresses, plastic strains and equivalent plastic strain, which motivate the strain hardening effect, were mapped to the coarser mesh using a closest node algorithm. Such remapping technique remaps the history variables to a node of the new mesh based on the closest node from it in the old mesh. After remapping, contour plots of the remapped mesh and the original mesh were compared for validation. Lastly, an elastic spring-back was performed on the remapped mesh. Once the residual stresses and strains are defined, a lap-shear lab scale coupon was created. This model was built using the final configuration of the remapped wedge model after spring-back, swept 36 times completing an $180^\circ$ 3D half joint (reference Fig.4-4 top left). In addition, extensions of sheets were created with a coarse mesh with average element size of 1 mm x 1 mm x 0.395 mm and connected to the rivet portion of the mesh (swept mesh), resulting in half of a lap-shear coupon with a plane of symmetry along the longitudinal direction of the coupon as shown in Fig.4-4.

Four simulations were performed in order to define load-displacement behavior of the SPR joint under lap-shear configuration: (1) no residual stresses and no plastic strains; (2)
residual stresses only and no plastic strains, thus no strain hardening effect; (3) plastic strains only and no residual stresses; (4) both residual stresses and plastic strains. The curves from these simulations were compared to experimental results in order to help elucidate the role of work hardening and residual stress on the mechanical behavior of the SPR joint.

Regarding fatigue performance, the simulations described above were each evaluated. In order to evaluate the fatigue response, each simulation was carried out using mechanical loading similar to the quasi-static load (i.e. lap-shear). In addition, to take into account the strain hardening due to the first loading cycle, two complete cycles were simulated. The last cycle was
then used for each analysis. Fretting wear was evaluated based on the fretting parameter of Anandavel et al. [94], which is a modified parameter developed by Ruiz et al. [78]. The fretting fatigue damage parameter, \( k_{\text{II}}^{ps} \), is defined in equation (4.3):

\[
k_{\text{II}}^{ps} = \sigma_1 \tau_{\text{max}} \delta_{\text{res}}
\]  

(4.3)

where \( \sigma_1 \) is the maximum principal stress, \( \tau_{\text{max}} \) is the maximum shear stress and \( \delta_{\text{res}} \) is the resultant of slip. The fretting damage parameter (FDP) is the product of \( \tau_{\text{max}} \) and \( \delta_{\text{res}} \). The FDP was plotted and compared to experimental results. Crack initiation and growth were estimated based on maximum principal stress amplitude of the last cycle analyzed.

4.4 Results and Discussion

A visual validation of the non-linear finite element analysis of the riveting process is shown in Fig. 5. The “wedge” simulation after the SPR process is represented by the green line and is superimposed over cross-section of the physical SPR joint. As can be observed, good agreement between simulation and experimental cross section was achieved. The friction coefficients were determined to be 0.15 for all surfaces except for 0.4 for the punch/rivet interaction; damage constants from Eq. 4.5 were defined as: \( d_1 = 0.1 \), \( d_2 = 1.6 \) and \( d_3 = 1.3 \).
Figure 4-4. Comparison of the FE simulation and experimental cross-section.

Figure 4-6 shows the comparison between the fine mesh used for the SPR process simulation and the coarse mesh created with the same geometry. Figure 4-6a shows the fine mesh configuration after the SPR process while Fig. 4-6b shows the same geometry re-meshed with coarser elements and optimized element geometry. Figure 4-6c shows the Mises stress contour after the SPR process while Fig. 4-6d shows Mises stresses remapped on the new geometry. Similarly, Fig. 4-6e shows the equivalent plastic strain (PPEQ), which drives the work hardening behavior for the fine mesh while Fig. 4-6f shows the remapped (PEEQ) on the coarse mesh. Note that the Mises scale bar for Figs. 4-6c and d is the same, while the PEEQ scale bar is the same for Figs.4-6e and 4-6f. It can be seen that remapping of the stresses and strains were accurately remapped to the course mesh profile. In fact, similar results were observed when all six components of stress and plastic strain were remapped showing good agreement, but are not shown for the brevity’s sake.

Figure 4-7 shows the evolution of Mises stress from the SPR finite element simulations before and after spring-back. Figure 4-7a shows the stress contour plot right after the SPR
process, remapped on the coarse mesh. On the other hand, Fig. 4-7b depicts Mises stress contour plot after the spring-back simulations performed implicitly (i.e. using standard mode in Abaqus). It is worth noting that the stresses decrease drastically but residual stresses are still present in the joint as shown in Fig 4-7b.

Figure 4-5. Remeshing and remapping evaluation of the fine mesh after self-piercing rivet process: a) final geometry and mesh, c) Mises Stress, e) Equivalent plastic strain (PEEQ). Coarse mesh based of fine mesh geometry: b) final geometry and mesh, d) Mises Stress, d) Equivalent plastic strain (PEEQ).
Figure 4-6. Evolution of Mises stress: a) before and b) after the spring-back.

Figure 4-8 shows the load displacement curves for experimental and simulation results. Four experimental results were plotted in order to show the variability of the mechanical response. The four simulations results shown are: (1) residual stresses and plastic strain obtained from the SPR process; (2) residual stresses only and no plastic strains; (3) plastic strain only and no residual stresses; (4) and with no residual stresses nor plastic strains. We note that the load-displacement curves were plotted up to the peak load. Since damage was not defined in the material cards of the tensile-pull simulations, the joint exhibited a rivet pull-out. On the other hand, experimentally, the failure of the joint occurred due to failure of the top magnesium sheet. The simulation performed with the mesh with no residual stresses and no plastic strains exhibited the least stiff mechanical response of all cases. This is mainly due to the low yielding surface of
the elements in and around the rivet. With lower yielding values, lower loads are needed to plastically deform each element, leading to larger displacements, and lower peak loads reached. Performing the simulations by adding only residual stresses, show that they did not differ much from the simulation with no residual stresses and no plastic strains. In addition, the joint appeared slightly stiffer at lower load (up to 3000N). This indicates that residual stresses in the majority of the elements may be opposite to the stresses inducted by pulling the joint, and when the loads are big enough, the induced stresses overcome the residual stresses, yielding the material, making the joint behave quite similarly to the joint with no residual stresses nor plastic strains. Evaluating the results when only plastic strains resulted from the SPR are included, it can be observed that the stiffness of the joint is greatly increased when compared to the two simulations discussed previously. Having plastic strains included in the SPR simulation increased the yielding surface due to typical work hardening. This results in a joint with an overall stiffer behavior, since much higher loads are needed to yield the material locally, leading to smaller displacements and higher peak loads. Adding residual stresses and plastic strains results in a joint slightly stiffer at lower loads when compared to a joint with plastic strains only. The discussion is analogous to the discussion about joints with and without residual stress, and no plastic strains, presented previously.

Figure 4-9 shows the visual comparison of the fractured surface subjected to fatigue loading and a similar section view from simulation results. Figure 4-9a shows the fractured surface of the magnesium alloy top sheet tested under the maximum load of 4600 N that failed at 9966 cycles. Based on fractography analysis, the fatigue crack initiated close to the rivet and then grew throughout the magnesium sheet. The initial crack is located at the magnesium sheet
between the rivet and the aluminum sheet. Figure 4-9b shows the predicted location from the simulation results based on the maximum principal stress amplitude. It is important to note that the simulation results depict high stress amplitudes at the location where the fatigue crack initiated experimentally. Visually, similar results were found for the simulation results with no residual stress and work hardening.

Figure 4-7. Load-displacement curves for experimental and simulation results of self-pierce riveting joint
Figure 4-8. Visual comparison of the fractured surface subjected to fatigue loading and high stress amplitude at the same location predicted numerically

Figure 4-10 shows the fretting wear location comparison between experimental and simulation results. Figure 10a shows a fractured joint tested at a maximum load of 1640 N, and 1033109 cycles to failure. It shows a top view of the joint, where only part of the magnesium top sheets is shown since fracture occurred in the magnesium sheet. The top surface of the aluminum sheet, which was in contact with the magnesium sheet is shown. The dark areas on the aluminum sheet in Fig. 4-10a are fretting oxides, they are a result of relative motion and contact stresses between the aluminum and magnesium sheets. Figures 4-10b-e show the predicted fretting locations on the bottom aluminum sheet based of the FDP, which is the product of $\tau_{max}$ and $\delta_{res}$, included in eq. (4.3). Residual stresses and plastic strain resultants from the SPR process were not included at the beginning of the simulation shown in Fig.4-10b, while only residual stresses were included at the beginning of the analysis shown in Fig.4-10c, only plastic strain resultants from the manufacturing process were included in Fig 4-10d, and both residual stresses and plastic strain resultants from the SPR process were included in Fig.4-10e. It can be observed
that adding residual stresses and plastic strains resulted in better agreement with experimental results compared to all other simulation combinations. Furthermore, we note that the predicted fretting locations (color contour) have a good correlation with experimental results (oxides/dark areas). Moreover, it can be observed that residual stresses and plastic strains have meaningful effects on fretting damage based on Figs 4-10c and d. In addition, combining the areas of fretting of residual stress only (Fig 4-10c) and plastic strain only (Fig. 4-10d) apparently gives a similar result compared to including both residual stresses and plastic strain resultants from the manufacturing process (Fig. 4-10e) in one simulation. This result suggests that changing internal stresses and plastic strains not only affects the internal state of the joint, but also affects the quasi-static (discussed previously) and fatigue (discussed later in this work) behavior, but also alters external reactions on each component of the joint (i.e. contact variables such as pressure, shear stress, slip). Such alterations appear to be enough to change fretting locations and fatigue crack initiation.
Figure 4-9. Fretting wear location comparison. a) experimental, b) without residual stresses and plastic strains, c) with residual stresses only, d) with plastic strains only, and e) including both residual stresses and plastic strain resultants from the manufacturing process.
Figure 14-11a shows the cross-sectional view of the failed SPR coupon shown in Fig. 4-10, where we noted that fatigue crack propagated into the magnesium sheet. The cross section of the failed magnesium sheet (cut plane A-A depicted in Fig.4-11a) is compared to predicted simulation results in Figs. 4-11b-e. In these figures, the experimental cross-sectional, A-A plane, is superimposed by the mesh simulation. In particular, Fig. 4-11b shows the direction of maximum principal stress amplitude indicated with an arrow predicted by the simulation without residual stresses and plastic strains from the SPR process, and the initial crack plane perpendicular to the maximum principal direction. The simulation results predict that the crack initiated at a distance of 4.146 mm from the rivet center at an angle of 81.1 degrees with the loading direction. Similarly, Fig. 4-11c shows the direction of maximum principal stress and initial crack plane, with residual stresses and no plastic strains included in the simulation. These simulation results predict that the crack initiated at same angle and location as the simulation with no residual stresses and no plastic strains. Likewise, Fig 4-11d shows that the crack angle is 77.37 degrees with crack initiation at 3.971 from the rivet center if only plastic strains are included. Analogously, Fig. 4-11e shows that including residual stresses and plastic strains predicts that the crack initiated at 3.8 mm from the rivet center at an angle of 77.37 degrees with the loading direction. Similar to the quasi-static results, we note that the simulation results with residual stresses and plastic strains predict the location and crack angle much closer to experimental results than the simulation results without the deformation history. From Fig. 4-11, we can see that including the residual stresses and plastic strains in the simulation changes the crack initiation location as well as crack propagation direction, and thus changes the calculated number of cycles to failure.
In order to calculate the number of cycles to failure and further validate the predicted fatigue crack location discussed previously, a linear elastic fracture mechanics (LEFM) approach was used. In this approach, the number of cycles to nucleate the crack, are assumed to be small, and thus were ignored. The failure mode observed here is a kink-crack, which is similar to works elsewhere [37–40]. The approach used in this study is based on a method similar to the one used to calculate number of cycles to failure for resistance spot welding (RSW) developed by Newman and Dowling [41]. This approach, which is a kinked crack stress intensity solution, was also used to predict fatigue lives of friction stir spot welds (FSSW) achieving good agreements with experimental results by Lin et al. [37,38] and Jordon et al.[39]. This model is described in Eqs. 4-10. Equations (4.4) and (4.5) are modes I and II of global SIF for RSW lap-joint specimen crack under tensile loading [42]:

\[ K_I = \frac{Q}{r^{3/2}} \left[ 0.341 \left( \frac{2r}{t} \right)^{0.397} \right] \]  (4.4)

\[ K_{II} = \frac{Q}{r^{3/2}} \left[ 0.282 + 0.162 \left( \frac{2r}{t} \right)^{0.710} \right] \]  (4.5)

where \( Q \) is the applied load per weld, \( r \) is the weld radius and \( t \) is the sheet thickness where the crack is supposed to grow into. Assuming that the crack is a kinked crack, the SIF in global modes I and II can be correlated to a local SIF as a function of kinked crack angle \( \theta \) [43,44]:

\[ k_I = \frac{K_I}{4} \left[ 3cos \left( \frac{\theta}{2} \right) + cos \left( \frac{3\theta}{2} \right) \right] - \frac{K_{II}}{4} \left[ 3sin \left( \frac{\theta}{2} \right) + 3sin \left( \frac{3\theta}{2} \right) \right] \]  (4.6)

\[ k_{II} = \frac{K_I}{4} \left[ sin \left( \frac{\theta}{2} \right) + sin \left( \frac{3\theta}{2} \right) \right] + \frac{K_{II}}{4} \left[ cos \left( \frac{\theta}{2} \right) + 3cos \left( \frac{3\theta}{2} \right) \right] \]  (4.7)
Equations (4.6) and (4.7) show the local stress intensity solutions for $k_I$ and $k_{II}$. Figure 12 illustrates [37,44] modes I and II of SIFs related with a kinked crack. It is worth noting that this approach assumes that the number of cycles to initiate the crack and grow it after the long crack regime, are negligible, and the SIF remains constant as the crack propagates through the sheet.
To apply a Paris law method to predict the fatigue life of a weld, which is under mixed mode loading, an equivalent mode I SIF, $K_{eq}$, is used:

$$k_{eq} = \sqrt{k_I^2 + k_{II}^2} \tag{8}$$

To estimate the number of cycles to failure, and account for the $R$-ratio loading effect [45], equation (9) was applied:

$$\frac{da}{dN} = C \left( \frac{\Delta k_{eq}}{(1-R)^{(1-\gamma)}} \right)^m \tag{9}$$

where $C$ and $m$ are material constants obtained from crack growth experiments, $R$ is the loading ratio (minimum load/maximum load), $\gamma$ is the loading ratio parameter, and $\Delta k_{eq}$ is the equivalent stress intensity range. Integrating eq. (9), the total number of cycles, $N_{Total}$, can be obtained with eq.(4.10):

$$N_{Total} = \frac{t-t_{crack}}{\Delta k_{eq}} \left( \frac{1}{(1-R)^{(1-\gamma)}} \right)^m \tag{4.10}$$

Since the crack starts at the surface of the sheet, $t_{crack}$ is zero, therefore $\frac{t}{\sin \theta}$ can be substituted by the crack length $a$. Analogously to the described method for RSW, the presented SIF model was applied to SPR with the assumption that the weld radius, $r$, is the predicted crack radius(i.e. the distance of the predicted crack by the FEA model to the center of the rivet) of the SPR. The material constants for cast magnesium alloys AM60B used were obtained from literature[46]. The material constants used were $C= 7 \times 10^{-7} \frac{mm}{cycle \cdot MPa \cdot m}$ and $m=6.7039$. As $\gamma$ values can vary from 0 to 1, and are not currently available, thus $\gamma$ was assumed to be 0.5.
Figure 4-11. Schematic representation of kinked crack for resistance spot welding.

Figure 4-13 shows comparison of the maximum cyclic applied load vs. the number of cycles to failure for the experiments and modeling results. As already mentioned previously, the cycles to failure were calculated assuming the majority of the cycles occur during long crack growth regime. Thus, it means that the number of cycles for crack nucleation is assumed to be zero. The circular shaped data points, in Fig.13, represent the experimental fatigue results, and the arrows indicate run-outs. The dashed lines in light blue represent the fatigue life predicted with no residual stresses and no plastic strains included at the beginning of the simulation. The dark blue dotted line is the prediction including residual stresses only. The dotted and dashed black line are the predictions including plastic strains, while the continuous red line is the prediction including both residual stresses and plastic strains. The crack initiation location was estimated from Fig. 4-12, where r is the distance from the predicted crack location to the center.
of the joint, and $\theta$ is the predicted crack angle. All curves agree well with experimental results, however the curve generated with residual stresses and plastic strains is more conservative compared to the experimental results and in particular is much more accurate in predicting the number of the cycles to failure in the high cycle regime. While the predicted fatigue curves do not appear to differ significantly in Fig. 13, it is important to note that the fatigue results are plotted in log format that can mask large differences in the number of cycles to failure. In fact, the difference in the predicted number of cycles to failure based on the simulation results with and without the deformation histories is a factor of two.

![Graph showing comparison between experimental and predicted number of cycles to failure based on simulation results with and without residual stresses and plastic strains.](image)

*Figure 4-12. Comparison between experimental and predicted number of cycles to failure based on simulation results with and without residual stresses and plastic strains.*
4.5 Conclusions

The present study characterizes the effect of residual stresses and strain hardening on the lap-shear performance of magnesium-to-aluminum SPR joints. In this study, high fidelity simulation results of SPR process including spring back and history variables were remapped to 3-D lap-shear coupon. The influence of adding residual stresses and strain hardening form the process simulations was evaluated. A summary of the key findings is presented:

1. The dissimilar magnesium-aluminum SPR process was simulated and good visual comparison with the physical joint was obtained.

2. Under quasi-static lap-shear loading, the simulation results reveal that the strain hardening due to the piercing process is the dominant factor for joint strength, while considering only residual stresses has negligible effect the joint behavior.

3. Residual stress and strain hardening resultants from the SPR simulation process leads to a change in the crack initiation location, crack angle, fretting intensity, and number of cycles to failure. These results indicate that including residual stresses and plastic strains from the process simulation leads to more accurate predictions when modeling the mechanical performance of SPR.

4. A kinked crack LEFM model was used to estimate fatigue lives. Despite the simplicity of the model, it agrees well with experimental results and helps to elucidate the role of including the deformation history in the finite element simulations. In fact, the difference in the number of cycles to failure based on the simulations with and without the deformation histories included was a factor of 2.

5. Residual stresses and plastic strains resultant from the SPR manufacturing process dramatically change quasi-static and fatigue behaviors. It can be observed that joint
stiffness is increased under quasi-static condition by adding these history variables. Fatigue crack initiation, propagation, fretting and fatigue lives are different when comparing simulations with and without residual stresses and strains from the SPR process. Moreover, including these variables leads to a better agreement with experimental results.
CHAPTER 5
CONCLUSIONS AND RECOMMENDATIONS

The effect of history variables related to SPR such as residual stresses, strains and contact pressure were evaluated in this study. To carry out this effort, numerical models for SPR were developed and validated with experimental results. Residual contact pressure variables, stresses, and plastic strains were simulated by non-linear finite element methods. Furthermore, the effect of including the deformation history in the quasi-static and fatigue simulations was evaluated. The findings and contributions made in this study are summarized as follows:

- Crack initiation was observed away from the rivet in high cycle fatigue regime in an aluminum-to-aluminum SPR joint. These observations indicate that the crack initiation driving force was fretting-induced. Simulation results correlate residual contact pressure with the fatigue crack initiation site strongly suggesting that the riveting process can alter the mechanics of fatigue crack initiation and propagation.
- The nonlinear finite element model developed in this work predicted the residual stresses measured from neutron and X-Ray diffraction methods. Furthermore, the hardness from FEA, calculated from the yielding surface, was compared to the experimental hardness profile showing good agreement.
- Residual stresses due to manufacturing process changes the joint behavior under quasi-static and fatigue regimes. In fact, quasi-static behavior is driven mainly
due to work hardening and less influenced by residual stresses. FEA including residual stresses and plastic strains shows better agreement with experimental results comparing quasi-static load-displacement curves, fatigue crack initiation, crack plane and fretting.

- The modeling approach developed in this work accurately predicted crack location and crack angle under fatigue loading. A kinked-crack, linear elastic fracture mechanics model was used to predict the fatigue lives based on results of the finite element simulations. The fatigue lifetimes based on fretting locations and maximum principal stresses predicted by the finite element simulation show good agreement with experimental fatigue lives.

**Future work**

The modeling approach presented in this work was employed to predict the behavior of the SPR joint under quasi-static and fatigue conditions. Suggestions for improvement and future work are proposed as follows:

- The modeling effort in this work could be extended and improved to predict damage to the SPR joint due to thermal expansion that occurs during the paint-bake process in the manufacturing of automobiles.
- Asymmetry issues of the SPR joint could be studied by changing the punching angle and/or using an anisotropic material model
- The use of a kinematic material should be evaluated to assess the effect of such models on predicting the macro mechanical behavior on quasi-static and fatigue behavior.
• Evaluation of residual stresses and plastic strains due to large deformation could be analogously used to evaluate performance of new joining technologies similar to the flow drill screw process.

Future work on the Flow Drill Screw Process

The flow drill screw (FDS) process is expected to be a more challenging process to model, when compared to the SPR process. Figure 5-1 shows the initial configuration of a FDS joint with a clearance hole. The top sheet has a clearance hole, the semi-circular arc shown is the blank-holder, and the screw shown is the flow drill screw. Boundary conditions were applied to the bottom surface of the bottom sheet to simulate the support holding the specimen.

![Image](image.png)

Figure 5-1. Initial configuration of flow-drill screw. a) lap-shear specimen, b) close view

Figure 5-2 shows the initial drilling of the flow drill screw. It can be observed that the main challenge in modeling the FDS is that elements tend to become distorted. The figure shows
a simulation with arbitrarlan-lagrangian-eulerian (ALE) approach, which uses adaptive mesh techniques that reduce element distortion. However, element deletion, which is needed for modeling the piercing in FDS, is not compatible with this approach. Since penetration of the flow drill screw appears to be too difficult with the lagrangian approach (elements would distort drastically) and with ALE (not allowed to delete elements), a different approach like pure eulerian, or the relatively recently developed Smooth Particle Hydrodynamics (SPH), should be used for this kind of problem.

Figure 5-2. Cross-section initial stage of flow-drill screw
REFERENCES


Conditions Using Finite Element Method, Wichita State University, 2010.


[59] C.G. Pickin, K. Young, I. Tuersley, Joining of lightweight sandwich sheets to aluminium


