Diffusion bonded microchannels provide significant benefits by reducing space requirements while improving thermal efficiency when used in heat exchangers. In particular, these microchannels have potential to improve efficiencies for combining concentrated solar power with supercritical CO$_2$ Brayton cycles, where structures operate at high temperatures (760 °C) and high pressures (25 MPa). This work uses computational modeling to provide a design space for microchannel geometries under these conditions by using a Representative Volume Element approach. Additionally, low cycle fatigue results are used to develop a Manson-Coffin relation to predict the effects of plastic loading on the fatigue life of diffusion bonded specimens. Manson-Coffin relations at temperatures of both 760 °C and room temperature had respective R$^2$ values of 0.9406 and 0.676, indicating reasonable reliability in results.
Computational Modeling of a Diffusion Bonded Microchannel under High Pressure and Temperature

By

William H. Pratte

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APPROVED:

Major Professor, representing Mechanical Engineering

Head of the School of Mechanical, Industrial, and Manufacturing Engineering

Dean of the Graduate School

I understand that my thesis will become part of the permanent collection of Oregon State University libraries. My signature below authorizes release of my thesis to any reader upon request.

William H. Pratte, Author
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1 Introduction

1.1 Motivation

Greater performance requirements for heat exchangers have resulted in a need to reduce size while maintaining or increasing heat exchanger efficiencies. For improvements in thermal efficiency to occur there is a need to reduce the spatial footprint of heat exchangers, while simultaneously increasing or retaining similar efficiency as conventional heat exchangers. While it is technically challenging to meet both of these goals, it has been shown that these requirements can be simultaneously achieved via the use of microchannel technology [1].

In such heat exchangers, microchannels take advantage of scaling laws to provide beneficial heat transfer properties [2]. When considering a unit length for a channel, channel volume is a function of length cubed, while surface area is a function of length squared. Reducing channel length by a factor of 10 results in a 100-fold reduction in surface area and a 1,000-fold reduction in volume (Figure 1).

![Scaling Law effects on Volume and Surface Area](image)

*Figure 1: Scaling Law effects on Volume and Surface Area*
Reducing the ratio of volume to surface area of the working fluid allows heat to transfer into the fluid more quickly, increasing the overall thermal efficiency of the heat exchanger. Increases in pressure drop can be offset by reducing the total length of the heat exchanger, as smaller channels have a shorter residence time required for the fluid to reach thermal equilibrium, with multiple such shorter channels being multiplexed to create a larger heat exchanger.

A unique instance where such microchannel heat exchangers may be very beneficial is in Concentrated Solar Power (CSP). CSP is a method of solar power generation which uses thermal energy from sunlight to create electricity, as opposed to photovoltaic solar cells. One of the primary issues of solar power is the inherent fluctuations in the energy source, as power is only generated during the day time. For solar power to be useful on a system-wide scale, the power generated through solar energy needs to meet energy demands both during the day time and at night, indicating a need for energy storage. In photovoltaic solar cells this would take the form of batteries which are typically large, expensive and corrosive.

CSP offers an advantage over photovoltaic solar cells in that the working fluid can be used as the energy storage medium [3] [4]. Heat from solar energy is transferred into the fluid, at which point the fluid is insulated and stored until electricity is required. Once power output is required, the heat from the working fluid is used to power a steam engine, Brayton cycle engine, or other electricity producing device. Such inherent energy storage qualities make CSP more appealing for large-scale applications than photovoltaic solar cells. An additional benefit of CSP is in land usage. A study by the National Renewable Energy Laboratory found that for the direct land-area requirements, CSP requires an average of 2.7 acres to provide a GWh/yr. Meanwhile
photovoltaic requires an average of 3.1 acres to provide a GWh/yr [5]. This improvement could be crucial in areas with limited available space.

While CSP has inherent benefits, it also brings significant technological challenges. When comparing electricity sources, an important consideration is the Levelized Cost of Electricity (LCOE). LCOE is a measure which provides a summary value of the cost for developing a given type of electrical generation. Recent estimates place the LCOE of CSP at $179.9/MWh, as compared to $66.3/MWh for photovoltaics or $139.5/MWh for new coal power plants [6]. To make CSP economically viable its efficiency must be improved. Microchannel-based solar receivers (Figure 2) have recently emerged as a technology that can potentially enable this increased efficiency due to increases in thermal heat exchanger efficiency.

**Figure 2: CSP Power Plant with Microchannel Receiver**
At the same time, the design of a microchannel receiver for CSP has additional challenges. For optimal efficiency, CSP requires high temperatures and high pressures. Often CSP is coupled with supercritical carbon dioxide Brayton cycles, which use temperatures or pressures as high as 760°C and 25 MPa [7]. To design a microchannel with optimal heat transfer properties requires a material designed to function at these high temperatures. Additionally, the structural mechanics of the device becomes a significant factor. The design must ensure that structural failure due to plastic yielding, creep or fatigue does not occur throughout the lifetime of the device while retaining a desired safety factor. These considerations must be designed for without adding unnecessary size.

To enable failure-proof design of microchannel heat exchangers it is necessary to model the thermomechanical properties of the base material and any bond materials while accounting for the geometry of the microchannel device. The life and thermomechanical behavior of the microchannel receiver is affected not only by high temperatures and pressures and the structural design, but also by the fabrication methods used to create them. A common methodology for manufacturing microchannels is diffusion bonding. In the diffusion bonding process, atoms are forced to diffuse across the interface between materials by applying pressure at elevated temperature [8]. Diffusion bonds are generally made in a vacuum, which aids in the diffusion of atoms and prevents incomplete bonding and crack growth, while also limiting void size [8]. While diffusion bonding is an effective joining method, it is challenging to model the mechanical properties of the bond after completion. Because of the inter-diffusion of atoms there can often be changes in the overall material properties which affect the strength of the joint. Because diffusion bonded material properties are a result of the materials used and the bonding process,
material properties must be found for a specific diffusion bonded material or set of materials under a given set of bonding parameters.

Holmquist et al. performed tensile test experiments on diffusion bonded titanium and titanium aluminide to find the yield strength, tensile strength, and properties for creep rupture [9]. Elrefaey and Tillmann found optimal bonding conditions by using lap shear tests to evaluate shear strength of diffusion bonds between titanium and stainless steel using a copper alloy as an interlayer [10]. This research was furthered by Kundu et al. by using a tensile test to determine the ultimate tensile strength when using a copper interlayer to bond titanium and stainless [11]. Work has been done to examine the material properties of diffusion bonds for aluminum alloys and magnesium alloys as well [12] [13].

One instance where limited information is known on the material properties of diffusion bonds is with nickel superalloys. Research has shown alloys such as Haynes 230 provide desirable material properties for microchannels, such as high strength and cyclic properties at elevated temperature [14]. Prior research has identified Haynes 230 and Haynes 282 as candidate materials for microchannel design [15], so to design a diffusion bonded microchannel with those materials, material properties of the diffusion bond are needed. Additionally, while previous work has examined the shear strength, tensile strength, and yield strength of diffusion bonds, the Young’s modulus for diffusion bonds is not generally examined. The elasticity of the diffusion bond may have a significant impact on stress and strain in a structure and thus is an important consideration.

For modeling microchannel heat exchangers, several different approaches have been taken. Guo-Yan and Shan-Tung used a viscoelastic approach analytically and computationally to find deflection and bending stress in a microchannel [16]. Pra et al. modeled thermal stresses of a
microchannel and compared results to ASME code to determine if the design would fail [17].

Additional papers on modeling microchannels used for heat exchangers focus on residual stresses from bonding [18] or heat transfer and fluid dynamics in the microchannel [19]. There are not significant amounts of modeling research on the solid mechanics of a microchannel heat exchanger at high temperature and pressure. The research that has been performed does not incorporate the material properties of the diffusion bond or how the diffusion bond affects the life cycle fatigue of the structure.

From this review of literature, the key challenges in modeling the solid mechanics for CSP applications are:

1. Modeling the microchannel structure’s deformation at high temperature and pressure
2. Determining the material properties of the diffusion bond
3. Analyzing the microchannel with diffusion bond properties incorporated into model

1.2 Research Goals

This thesis will address computational modelling of the mechanics of a diffusion bonded microchannel for the high temperatures and high pressures required for a CSP receiver. Goals of this research include:

1. To predict plastic failure of a diffusion bonded microchannel
2. To predict low-cycle fatigue failure of a diffusion bonded microchannel
3. Computationally predict geometric parameters of the microchannel under which 1. and 2. occur
2 Computational Approach

A pin-fin channel geometry, which was found by previous work [15] to be an effective microchannel design from a thermal efficiency point of view, is modeled using Finite Element Analysis (FEA) in Abaqus software. This pin-fin array geometry [15] consisted of a wide microchannel with pins distributed throughout the channel. The channel was a staggered pin array with circular pins (Figure 3).

![Circular Pin-Fin Array](image)

**Figure 3: Circular Pin-Fin Array [15]**

This type of channel has been found to have higher receiver efficiencies when compared to other common channel types, such as straight rectangular channels [15]. For staggered pin arrays the ratio between the longitudinal and latitudinal center-to-center distance between pins is fixed at \( \cos(30^\circ) \), or 0.866 [20]. Because the pin configuration is fixed, critical geometric parameters become: pin diameter \( D \), span between pins \( S \), and top plate thickness, \( t \) (Figure 4).
The FEA model used to examine the deformation of the microchannel includes a bottom plate, pins, and a top plate, as well as a bonded region between the top plate and the pins (Figure 5). Initial simulations were performed to examine the effects of geometric parameters on stress and strain without an explicitly modeled bond region.

**Figure 4: Pin Spacing Schematic**

**Figure 5: Computational Model of Microchannel**
A Representative Volume Element (RVE) of the microchannel geometry was used for these simulations. The RVE approach allows for computationally efficient examination of the effect of geometric parameters on stress and strain in a structure [21] by defining a RVE with boundary conditions which are representative of the entire structure. Because the volume being analyzed is significantly diminished from the entire microchannel array, simulations require significantly less computational time. Critical components of this RVE model are mesh size, boundary conditions, and loading conditions which will be detailed in the following sections. Thermomechanical testing data provided by collaborators at Oregon State University and NETL research laboratory was used to calibrate the model. Tensile test specimens were used to calibrate elasto-plastic stress-strain properties from Low-Cycle Fatigue (LCF) data and to empirically derive a Manson-Coffin relation for plastic strain and cyclic failure.

2.1 Mesh Size

For accurate Finite Element Analysis (FEA) mesh size, or element size, is a critical factor. A finer mesh equates to smaller elements throughout the model. As the number of elements increases and the size of the elements decreases, the model becomes increasingly physically realistic. However, increasing the number of elements also increases the computational time for the simulation. To find a balance between model accuracy and computational time, effective FEA requires an initial analysis of mesh convergence. Mesh convergence is the point at which model accuracy in predicting stress and strain is no longer influenced by decreasing the element size. In other words, as the element size is decreased from a very coarse mesh to a very fine mesh, the model becomes more accurate. In FEA, a point exists where decreasing the element size no longer has an appreciable effect on model accuracy, while
it continues to increase processing time. When decreasing the element size no longer affects simulation results significantly, the mesh is converged.

Before beginning analysis on the effects of geometric parameters on the RVE, mesh convergence analysis was performed. This analysis was performed by performing several simulations while progressively decreasing element size, using stress as the metric for convergence. A path was incorporated into the model so stress could be compared across the same location. Figure 6 shows the path through the top plate.

![Path](image)

**Figure 6: Path for Mesh Convergence.**

After defining the path, stress measurements were taken along this path. This process was repeated for different mesh sizes until a converged mesh size was found. Figure 7 shows a comparison of stress values along the defined path. Stress values through the path followed the same trend for each mesh size. Analyzing the maximum stress for each mesh size shows a 0.99% difference in maximum von Mises stress when going from 0.01 mm mesh size to 0.015 mm mesh size, and a 0.7% difference in maximum von Mises stress when going from a mesh size of 0.015 to 0.02 mm. From this analysis 0.015 mm was found to be an appropriately fine mesh size for the RVE simulations. This mesh size was used for all RVE simulations.
2.2  Boundary Conditions

A critical component of FEA modeling with RVEs is the application of appropriate boundary conditions. Boundary conditions assist with ensuring the model is accurate to the physical constraints on the part. Additionally, boundary conditions on the RVE prevent redundant computing, i.e. for a series of identical shapes under equivalent forces.

For accurately representing the RVE behavior, several boundary conditions were used. The first boundary condition used was an encastre constraint along the bottom surface of the microchannel. The microchannel receiver will be fixed into place in some fashion during operation, so an assumption that one surface is fixed is reasonable. An additional boundary condition on the model is the application of temperature. The entire structure will be at an elevated temperature. This results in the model being a conservative scenario, as the material strength is expected to be lowest at higher temperature. Stresses due to thermal expansion were not considered for this analysis.

Figure 7: Stress vs Position for Mesh Convergence.
The RVE also included symmetries present in the microchannel design. These symmetries allow for the model size to be reduced and symmetry boundary conditions to be used to represent the microchannel. Figure 8 shows the symmetrical boundaries of the pin-fin array.

**Figure 8: Symmetrical Boundaries of Pin-Fin Array**

Symmetrical boundary conditions are found as regions where stress from the model is maintained after the symmetry is applied. As shown in Figure 8 above, symmetrical constraints are found between any two adjacent pins. Application of the symmetrical and encastre boundary conditions in the FEA model results in the RVE shown in Figure 9.
While the RVE displayed shows the most concise representation of the pin array, it is difficult to quickly interpret the stresses around the RVE. For this reason, graphical representations throughout this paper have used “mirror” functions to improve the presentation of stress and strain in the model. See Figure 10 for example, as a mirrored structure obtained by mirroring the RVE in Figure 7 about the XZ and ZY planes.
2.3 Loading

The SCO$_2$ flows through the microchannel receiver at 250 Bar (25 MPa) internal pressure. Thus a pressure of 25 MPa was applied to all interior surfaces of the model (Figure 11). This pressure applies a tensile load on the pins by pushing up on the top surface. Additionally, there is a compressive load on the pins due to the pressure on the outer surface of the pins. Pressure applied to the top plate results in stresses through the top plate.
For tensile specimen models, loading was performed using a displacement condition. One end of the model was fixed using an encastre boundary condition, while the other end of the model was pulled an appropriate distance for the test being performed. The strain rate sensitivity of the diffusion bonds was assumed to be insignificant here. For that reason, the rate of displacement for the tensile specimen does not have a meaningful impact on simulation results. Figure 12 shows loading configurations for the tensile specimen. Note that symmetry boundary conditions were used, rather than modeling the entire test specimen.

Figure 11: RVE with Loading
The key geometric variables of the microchannel that were considered were pin diameter, $D$, top plate thickness, $t$, and span, $S$, between pins. The span was determined as the shortest distance between adjacent pins in the array. Figure 4 shows the pin spacing, where each individual pin has six equidistant pins surrounding it.

A fourth design parameter, pin height, was also considered in preliminary designs. Preliminary results found that the pin height does not have a significant impact on stresses and strains in the pin array. Thus, pin height was not considered as a primary geometric parameter for further simulations.

After determining the geometric parameters considered for the microchannel design, it was necessary to understand how those parameters would affect stress and strain under loading. For this analysis two regions were found to be most likely regions of failure: the top plate and in the pins near the bonded surface. An analytical estimation of deformation provides a first approximation of these effects.
As shown in Figure 13b, a simplistic estimation of the forces on the top plate is a fixed beam under uniform loading. While this does not consider the three-dimensional geometry of the microchannel structure, the general equations for shear force and shear stress can be used to better understand stress in the top plate. Using the Jourawski formula [22] to approximate shear stress gives:

$$\tau = \frac{VQ}{I_t}$$

Where:
- V: Shear force
- Q: Statical moment of area
- I: Cross-section moment of area
- t: Thickness

Shear force in a fixed beam with uniform loading is given by:

$$V = P \left(\frac{S}{2} - x\right)$$

Where:
- P: Uniform Load
- S: Span
- x: Distance to a given point
For a cross-section of width $b$ and thickness $t$:

\[ Q = \frac{bt^2}{4} \]  

(3)

\[ I = \frac{bt^3}{12} \]  

(4)

Inputting equations (2), (3), and (4) into the equation (1), we have:

\[ \tau = P \left( \frac{S}{x} - x \right)^3 \frac{1}{t^2} \]  

(5)

From equation (5) we find that shear stress at any point across a span is a function of pressure, span length, and thickness. Shear stress increases linearly as span distance increases, while stress decreases as a function of the thickness squared. Span length and thickness thus become important geometrical considerations for failure in the top plate.

For failure in the pins, it becomes important to consider the bonded and unbonded area of the pin array. Considering the top and bottom plates to be rigid components, loading is simplified as shown in Figure 14.

\[ \text{Figure 14: (a) Schematic of Loading on Pin (b) Tensile Forces Applied to Pin} \]
Knowing that pressure is related to force and area, and that pressure will be applied to all areas of the top and bottom plates which are not bonded to pins, the following equations become relevant:

\[ \sigma = \frac{\text{Force}}{\text{Area}} \]  \hspace{1cm} (6)

\[ \text{Force} = \text{Pressure} \times A_{\text{unbonded}} \] \hspace{1cm} (7)

\[ A_{\text{unbonded}} = A_{\text{total}} - A_{\text{pins}} \] \hspace{1cm} (8)

Correlating the bonded area to geometrical considerations means defining the bonding area in terms of diameter and span. In particular, it is helpful to consider span and diameter in terms of bonding area ratio, or bonded area divided by total area. To determine the bonding area ratio, it is helpful to consider the unit cell shown in Figure 15.

\[ \text{Figure 15: Unit Cell for Bond Area Ratio} \]
When considering the area within the dashed outline, the bonding area ratio (ratio of top plate area bonded to pins vs non-bonded top plate space) of the pin array becomes:

$$Bonding\ Area = \frac{A_{pins}}{A_{total}}$$  \hfill (9)

$$A_{pins} = \frac{\pi}{2} D^2$$  \hfill (10)

$$A_{total} = XY$$  \hfill (11)

Where:

$$Y = P$$

$$X = 2Psin(60°)$$

Total area of the region becomes:

$$A_{total} = 2P^2sin(60°)$$  \hfill (12)

Resulting in bonding area of:

$$Bonding\ Area\ ratio = \frac{A_{pins}}{A_{total}} = \frac{\pi D^2}{4P^2sin(60°)}$$  \hfill (13)

$$Bonding\ Area\ ratio = 0.9069 \left(\frac{D}{P}\right)^2$$  \hfill (14)

Furthermore, span is a linear relationship between the diameter and center-to-center distance:

$$Span = P - D$$  \hfill (15)

Equations (6), (7), (8), and (14) are then combined to find the relation between Span, Diameter and Stress:
\[
\text{Stress} = \frac{8}{\pi} \text{Press} \left( \left( \frac{P}{D} \right)^2 - 0.9069 \right) \quad (16)
\]

Using equation (15) for \( \text{Span} \) gives:

\[
\text{Stress} = \frac{8}{\pi} \text{Press} \left( \left( \frac{\text{Span}}{D} + 1 \right)^2 - 0.9069 \right) \quad (17)
\]

Equation (16) and (17) show that stress in the pin increases with increasing span/center-to-center distance and decreases with increasing pin diameter. These stress increases are due to changes in the bonding area are proportional to diameter and center-to-center distance, as well as for diameter and span. A design locus was formed in terms of diameter, span, and thickness. This design locus shows the relationship of span and diameter to thickness. Because span and diameter have a linear relationship with bonding area, changing one while keeping the other constant will have the same effect as reversing the trend (i.e. changing span with constant diameter vs changing diameter with constant span) with equivalent values.Thickness, however, affects stress independently of both diameter and span, so is considered separately. Equation (18) shows safety factor, \( SF \), was defined as a function of failure at yield stress. This safety factor was used to examine design loci.

\[
SF = \frac{\sigma_{\text{yield}}}{\sigma_{\text{actual}}} \quad (18)
\]
3 Modeling microchannel heat exchangers without bonds

For initial analysis on the RVE, monotonic pressure simulations were performed without using separate material properties for the diffusion bond. In other words, the bond’s elasto-plastic mechanical properties were assumed to be the same as that of the base material. Pressure was applied on the internal surfaces of the RVE (as shown in Figure 11) in a linear ramp profile from 0-25 MPa over a 1 second time increment. The maximum pressure of 25 MPa was specified by the channel’s intended application with a Brayton cycle [7]. Strain rate dependence was not specified as part of the material properties so the time increment used in FEA does not impact the elasto-plastic response of the pressure application. The pressure was applied while maintaining constant temperature throughout the RVE. The base material yield strength decreases with increasing temperature, so applying the maximum expected temperature across the entire structure increased the stress and strain in the channel, resulting in more conservative geometric design results. Static failure, i.e., when stress in the structure exceeds the yield strength at the above temperature, was calculated. To ensure an adequately safe design, a safety factor of 1.2 was used for the analysis, as in equation (18). The above monotonic RVE based FEA were performed at temperatures of 760, 800, and 850 °C. The yield strength of H230 was obtained from the material manufacturers data book, as shown in Figure 16 [22]. Additional data from the material manufacturer was used to produce plastic stress-strain curves and determine trend-lines to approximate the plastic curves. Plastic stress strain curves for H230 at room temperature and 760 °C are shown in Figure 17 and Figure 18, respectively.
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</tr>
</tbody>
</table>

**Figure 16: Haynes 230 Yield Strength and UTS**

**Figure 17: Plastic Stress-Strain for Haynes 230 at Room Temperature**

\[ y = 1650.4x^{0.4128} \]
Power laws for plastic stress were found using power laws defined by a strength coefficient, $K$, and a strain hardening exponent, $h$, as shown in equation (19). Power laws defining the plastic curves for room temperature and 760 °C are shown in equations (20) and (21), respectively:

$$\sigma_p = K(\varepsilon_p)^h$$  \hspace{1cm} (19)

Plastic Stress @ RT = $1650.4\varepsilon_p^{0.4128}$  \hspace{1cm} (20)

Plastic Stress @ 760°C = $732.7\varepsilon_p^{0.2199}$  \hspace{1cm} (21)

The center-to-center distance of the RVE was held constant and pin diameter was varied to change the span (Figure 15). Thickness of the top plate was varied. However, top plate thicknesses below 0.1 mm were not considered as they were determined to be difficult to fabricate and would not be realistic for manufacturing the microchannel.
These simulations showed two modes of static failure in the RVE, namely:

**Mode 1:** Stress concentrates in the pins, while minimal stress is found in the top plate. High stress values are found in the center of the pin, and maximum stress is found near the joint between the pin and the top plate. An example of such a distribution in the von Mises stress and equivalent plastic strain in the RVE are shown in Figure 19 and Figure 20, respectively. This mode occurs when the top plate is thick, limiting the amount of stress that develops in the top plate.

**Mode 2:** Stress distributes between the pin and top plate. Areas of high stress are found in the top plate directly between pins, while stress in the center of the pin is significantly lower. Examining the plastic strain shows that although stress is more evenly distributed, maximum stress and plastic strain still occur primarily at the joint between the pin and top plate. This configuration occurs when the top plate is relatively thin, so stress builds in the top plate. Stress distribution and plastic strain for this geometry are shown in Figure 21 and Figure 22, respectively.
**Figure 19:** Stress Concentration with Thick Top Plate
**Figure 20**: Plastic Strain with Thick Top Plate
Figure 21: Stress Distribution for Thin Top Plate
Figure 22: Plastic Strain with Thin Top Plate
**Figure 23: Static Yield Results at 760 °C**

As shown in Figure 23, increasing the ratio of span to diameter, decreasing the top plate thickness, or a combination of each can cause the RVE to fail due to plastic loading. Data points in green indicate geometric designs which did not fail under loading. For any data point which did fail, designs with greater thickness and/or lesser span also did not fail. This information provides an estimated design space which is useful for initial microchannel designs. RVE results for 800 °C and 850 C are shown in Figure 24 and Figure 25, respectively.
Figure 24: Static Yield Results at 800 C
Figure 25: Static Yield Results at 850 °C

Comparing the results at 760 °C, 800 °C, and 850 °C, the primary difference is in the safety factor at the various temperatures for corresponding geometries. For Haynes 230, both the modulus of Elasticity and the yield strength decrease with temperature. Decreasing the modulus increases strain in the model. Decreasing the yield strength decreases the safety factor due to easier yield of the material. Note that over the range of 760 – 850 °C decreases in elastic modulus and yield strength for Haynes 230 are small, roughly 5% each [22], so decreases in the safety factor are also small.
An additional candidate material for this project was Haynes 282. The analyses used to find results for Haynes 230 were repeated for Haynes 282 to compare the materials. Figure 26 shows static yield results for Haynes 282 at 760 °C.

**Figure 26: Static Yield results for Haynes 282 at 760 °C**

Comparing results in Figure 26 to results for Haynes 230 at 760 °C in Figure 23 shows that Haynes 230 has a maximum span of approximately 0.425 mm, while Haynes 282 has a maximum span of approximately 0.550 mm. This increase is because Haynes 282 has a higher yield strength than Haynes 230. An increased span could be valuable for concerns with channel blockages and could provide designs with improved thermal efficiency. While Haynes 282 provides a larger design space, Haynes 230 was chosen as the material for this application as it is less affected by corrosion and is more feasible to diffusion bond than Haynes 282. Therefore, all
other results in this work utilize material properties for either Haynes 230 or diffusion bonds made between multiple pieces of Haynes 230.
4 Prediction of low cycle fatigue failure

With collaborators at NETL and Dr. Brian Paul’s group at Oregon State University, low cycle fatigue (LCF) data was found for Haynes 230. Two types of diffusion bonded tensile specimens were used for the LCF tests. The first was a bonded cylindrical tensile specimen, shown in Figure 27.

![FEA Model of Bonded Tensile Specimen](image)

**Figure 27: FEA Model of Bonded Tensile Specimen**

The second type was a tensile specimen with a microchannel geometry embedded in the middle of the specimen. Figure 28 shows an FEA model which represents one quarter of such an embedded tensile specimen.
For the tensile specimen, a universal testing machine (UTS machine) performed tensile stress and strain calculations while assuming the test specimen was a homogenous structure. Since the specimen is a bonded structure, rather than a homogenous one, the calculated values for stress and strain do not directly apply to the material. Rather, force and displacement obtained directly from the UTS machine were used to compare FEA results to experimental data and calibrate the material properties of the bond material. Calculations of force and displacement from experimental data will be described in greater detail later in this section. Test data is given for room temperature and 760°C. LCF tests were performed at room temperature to provide a baseline strength value, and at 760°C to determine material properties at the microchannel design temperature. LCF data is shown for tensile tests without the embedded microchannel geometry (Table 1) and with the embedded microchannel geometry (Table 2). For LCF testing, several non-embedded tensile specimens were corroded by researchers at NETL prior to testing. This
The corrosion tests found that 500hrs sCO2 exposure effects a 5-10 μm region, beginning at the outer surface of the metal. For a 1.59 mm radius LCF specimen, 5-10 μm represents a maximum of 0.63% of the total radius, and 1.26% of the total cross-sectional area. Due to the small affected zone, the specimens were not expected to be noticeably affected by corrosion. LCF results confirmed this result; for this reason, corroded specimen results were included with the non-corroded specimen results. Corroded specimens have been marked in Table 1. The data from these tests have been abbreviated to highlight critical data used for model calibration.

**Table 1: Non-embedded tensile test results**

<table>
<thead>
<tr>
<th>TARGET STRAIN RANGE (%)</th>
<th>TEST TEMP. °C</th>
<th>SELECTED CYCLE</th>
<th>ELASTIC STRAIN RANGE</th>
<th>PLASTIC STRAIN RANGE CALC.</th>
<th>PLASTIC STRAIN RANGE MEAS.</th>
<th>CYCLES TO FAILURE</th>
<th>CORRODED SPECIMENS</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.500</td>
<td>Room</td>
<td>0.4993</td>
<td>5,000</td>
<td>0.495</td>
<td>0.478</td>
<td>0.018</td>
<td>0.017</td>
</tr>
<tr>
<td>0.700</td>
<td>Room</td>
<td>0.4992</td>
<td>600</td>
<td>0.697</td>
<td>0.450</td>
<td>0.246</td>
<td>0.247</td>
</tr>
<tr>
<td>0.700</td>
<td>Room</td>
<td>0.4993</td>
<td>600</td>
<td>0.695</td>
<td>0.493</td>
<td>0.198</td>
<td>0.202</td>
</tr>
<tr>
<td>0.200</td>
<td>760</td>
<td>0.5062</td>
<td>8,000</td>
<td>0.197</td>
<td>0.197</td>
<td>0.007</td>
<td>0.000</td>
</tr>
<tr>
<td>0.145</td>
<td>760</td>
<td>0.5060</td>
<td>10,000</td>
<td>0.143</td>
<td>0.142</td>
<td>0.006</td>
<td>0.001</td>
</tr>
<tr>
<td>0.145</td>
<td>760</td>
<td>0.5054</td>
<td>6,000</td>
<td>0.140</td>
<td>0.140</td>
<td>0.000</td>
<td>0.000</td>
</tr>
<tr>
<td>0.145</td>
<td>760</td>
<td>0.5056</td>
<td>10,000</td>
<td>0.137</td>
<td>0.137</td>
<td>0.010</td>
<td>0.000</td>
</tr>
<tr>
<td>0.300</td>
<td>760</td>
<td>0.5048</td>
<td>800</td>
<td>0.295</td>
<td>0.282</td>
<td>0.004</td>
<td>0.013</td>
</tr>
<tr>
<td>0.095</td>
<td>760</td>
<td>0.5051</td>
<td>10,000</td>
<td>0.093</td>
<td>0.093</td>
<td>0.002</td>
<td>0.000</td>
</tr>
</tbody>
</table>

**Table 2: Embedded tensile test results**

<table>
<thead>
<tr>
<th>TARGET STRAIN RANGE (%)</th>
<th>TEST TEMP. °F</th>
<th>SELECTED CYCLE</th>
<th>ELASTIC STRAIN RANGE</th>
<th>PLASTIC STRAIN RANGE CALC.</th>
<th>PLASTIC STRAIN RANGE MEAS.</th>
<th>CYCLES TO FAILURE</th>
<th>CORRODED SPECIMENS</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.600</td>
<td>Room</td>
<td>0.4994</td>
<td>50</td>
<td>0.598</td>
<td>0.426</td>
<td>0.178</td>
<td>0.172</td>
</tr>
<tr>
<td>0.040</td>
<td>760</td>
<td>0.5047</td>
<td>1,000</td>
<td>0.038</td>
<td>0.038</td>
<td>0.000</td>
<td>0.000</td>
</tr>
<tr>
<td>0.471</td>
<td>760</td>
<td>0.5052</td>
<td>10</td>
<td>0.425</td>
<td>0.309</td>
<td>0.083</td>
<td>0.116</td>
</tr>
</tbody>
</table>
From the data in Table 1 and Table 2, the strain range and gauge length were used to calculate the crosshead displacement, of each LCF test. FEA models of the same tensile geometry were then examined to find the maximum plastic strain and von Mises stress at the calculated displacement for the given strain range. The stress and strain were plotted with the number of cycles to failure ($N_f$) to find a Manson-Coffin relation. Manson-Coffin relations are used to determine the relationship between plastic strain and cycles to failure for low cycle fatigue. A Manson-Coffin relation will determine if the bonded H230 modeled in the RVE meets cyclic design constraints after plastic deformation; furthermore if the cyclic criteria is met while undergoing plastic deformation this will confirm the RVE will be expected to meet cyclic criteria when the structure undergoes purely elastic deformation. An example of a Manson-Coffin relation for S20C low carbon steel is shown in Figure 29. Results on the Manson-Coffin relation will be described further after the discussion of model calibration using LCF data.
Figure 29: Example Manson-Coffin relation for S20C carbon steel [23]

Further, the above calculated stress and strain from the first cycle of the LCF data was converted to force and displacement to use for calibration of the bonded layer elasto-plastic properties. Figure 30 shows True Stress-True Strain data for the first five cycles from an LCF test at room temperature. Figure 31 shows the first five cycles of LCF data at 760 °C.
Figure 30: First 5 Cycles of LCF test at Room Temperature
Figure 31: First 5 cycles of LCF data at 760C

Stress-strain data from Loop 1 (Figure 30) was converted to force-displacement using equations (22-25).

\[ \varepsilon_t = \ln(1 + \varepsilon_e) \]  (22)

Where:

\[ \varepsilon_t = True \ Strain \]
\[ \varepsilon_e = Engineering \ Strain \]

\[ \varepsilon_e = \frac{\Delta L}{L} \]  (23)

Where:

\[ \Delta L = Displacement \]
\[ L = Length \]
\[
\sigma_t = \sigma_e (1 + \varepsilon_e)
\]

(24)

Where:

\( \sigma_t \) = True Stress

\( \sigma_e \) = Engineering Stress

\[
\sigma_e = \frac{F}{A}
\]

(25)

Where:

\( F \) = Tensile Force Applied

\( A \) = Cross-sectional Area

Force and displacement calculated from true stress and true strain is shown in Figure 32.

**Figure 32:** Force-Displacement of First Cycle LCF at RT
This force-displacement data obtained experimentally was then compared to force-displacement from FEA. To calibrate the bond layer in FEA the base material properties were held constant, while the bond material properties were changed iteratively. The yield strength of the diffusion bond layer was iteratively edited manually until the force-displacement curve from FEA matched the force-displacement curve from the LCF data. Figure 33 shows force-displacement for the LCF data and for FEA simulation results. For the initial simulation the bond was given material properties identical to that of the base material at room temperature: a yield strength of 375 with hardening exponent, \( h \), equal to 0.4128 and strength coefficient, \( K \), of 1650.4 as shown in equation (20). Following simulations offset plastic stress values to an equivalent amount as that of the yield stress (i.e. for a simulation with yield stress equal to 275 MPa, plastic stress values were lowered by 100 MPa). Legend values indicate the yield strength used for that simulation.
Figure 33: Force-Displacement Calibration at RT

From Figure 33, the calibrated point at 375 MPa results in a curve that matches well with the plastic portion of the experimental data. This matches with the yield strength of the base material at room temperature, 375 MPa. These steps were repeated for LCF data at 760 °C to determine if using material properties of the base material provided a reasonable estimation of the overall properties. Force displacement data for the LCF bonded specimen is shown in Figure 34, and bond material parameter calibration results are shown in Figure 35. For the initial simulation the bond was given material properties identical to that of the base material at 760 °C: a yield strength of 254 with hardening exponent, \( h \), equal to 0.2199 and strength coefficient, \( K \), of 732.7 as shown in equation (21).
Figure 34: Force-Displacement of first cycle LCF at 760°C
From Figure 35 a yield strength in the bond layer of 254 MPa provides a reasonably accurate representation of the experimental data. 254 MPa is equivalent to the yield strength of the H230 base material at 760 °C.

By using LCF data and FEA simulations as above, a Manson-Coffin relation was constructed to relate plastic strain to fatigue life. The gauge length and maximum gauge displacement were found for each LCF test. This gauge length and maximum displacement were then used to find the maximum plastic strain in FEA for a monotonic tensile specimen at those conditions. This plastic strain was plotted with the number of cycles for failure for the Manson-
Coffin relation. Maximum plastic strain for an embedded and non-embedded tensile FEA are shown in Figure 36 and Figure 37, respectively.

**Figure 36:** Plastic Strain Distribution across Embedded Microchannel Geometry
Figure 37: Plastic Strain Distribution across Non-Embedded Tensile Specimen

The Manson-Coffin relation derived from this correlation is shown in Figure 38. The relationship shows values of plastic strain and cycles to failure for room temperature and 760 °C results on a log-log scale. Trend-lines for both 760 °C and room temperature were plotted from these results.
Figure 38: Manson-Coffin relation of diffusion bonded Haynes 230 at 760 °C and RT

Figure 39: Manson-Coffin relation of Haynes 230 [24]
When comparing the Manson-Coffin relation of bonded tensile tests with that of the Haynes 230 base material, as shown in Figure 39 [24], it is immediately noted that the number of cycles to failure is dramatically reduced. Because the material strength of H230 decreases with increasing temperature, the plastic strain for a certain number of cycles is expected to decrease as temperature increases. This, however, is not the case when comparing bonded H230 tested at 760 °C with base material H230 at 816-927 °C. For example, base H230 at 816-927 °C and 1,000 reversals (500 cycles) correlates to a plastic strain amplitude of 0.2655 [24]. For diffusion bonded H230 at 760 °C, 1,000 reversals to failure relates to a plastic strain amplitude of 0.0066. This decrease in plastic strain for a given number of cycles, despite a decrease in temperature, indicates that the diffusion bonded H230 specimens have a significantly lower fatigue life than specimens made solely from H230 base material.
5 Conclusions

From this work, a preliminary geometrical design space for a microchannel with a pin-fin array was created. From Figure 23, Figure 24, and Figure 25 we have a design space for thickness, span, and pin diameter at 760, 800, and 850 °C, respectively. This design space can then be used along with thermal analysis to determine optimum geometric parameters that meet both heat transfer and mechanical life goals. Because this design space is within the elastic limit of the material, it is anticipated that a microchannel designed with these geometric parameters will not fail under low cycle fatigue. A structure with loading cycles in the elastic region is expected to withstand more cycles prior to failure than a structure which plastically deforms through each cycle, assuming similar loading conditions. For this reason, analysis was performed to determine the expected life of bonded H230 under plastic loading.

To determine the effects of plastic strain on the expected cycles to failure a Manson-Coffin relation was developed, as shown in Figure 38. Understanding the effects of plastic strain may be useful in the event of unexpected high pressure, potentially due to system start-up and shut-down procedures or due to internal blockages in the microchannel. Under such operational conditions the alteration in the pressure and in the effective geometry of the RVE might result in additional plastic stresses beyond the designed elastic point. Additionally, it may allow for geometric designs in the plastic deformation region if a design in the elastic deformation regime is not able to meet thermal efficiency design goals. While exact geometric parameters were not found, they could be found easily using the modeling techniques presented in with the section on the Manson-Coffin relation. This meets the research goal of predicting permanent plastic deformation and low cycle fatigue failure as a function of the geometric parameters of the RVE, temperature and pressure.
A note should be made on the method used to determine plastic strain for the Manson-Coffin relation. Generally plastic strain in a tensile specimen is found from stress and strain data as calculated by strain gauges spaced across the reduced section of the tensile specimen. The resulting plastic strain is an average of the plastic strain distributed across the entire region, rather than one maximum value. The Manson-Coffin relation derived for the bonded tensile specimens, however, used FEA to find the maximum plastic strain value. This was done because the specimen was a mixture of bonded and non-bonded regions, and to allow for comparisons between specimens with and without embedded microchannel geometry. The maximum value from FEA is higher than an average plastic strain value across the region, resulting in an upwards shift in the Manson-Coffin relation. Due to the method used, for design purposes these results should primarily be used in conjunction with maximum plastic strain from FEA.
6 Future Work

Future work includes further material testing, as well as using additional modeling techniques to improve results. For this work, it was assumed that material properties of the H230 base material after diffusion bonding and heat treatment were equivalent to that of hot-rolled H230. It is unlikely that those material properties are identical, so material tests of H230 which has been treated under equivalent conditions to the diffusion bonding and heat treatment of the diffusion bonded tests would provide more accurate data and help us understand if the processing history needs to be accounted for. The H230 base material properties could then be used for more accurate material properties of the bonded joint. For the Manson-Coffin relation, additional LCF testing would provide further verification of the model.

Further modeling efforts could be made by analyzing the expected temperature gradient across the structure and applying that temperature gradient to the FEA model. For this work, a generalization was made that the entire microchannel is at the highest expected temperature. While this is a generally conservative estimate, if the temperature gradient is significant it could result in additional thermal stresses or stresses due to thermal expansion.

Future work also includes using additional modeling methods for determining the cyclic life of the RVE. Modeling methods such as Elastic-Perfectly Plastic analysis are currently being developed to find fatigue limits in pressure vessels and account for creep strain. Applying analysis such as EPP would provide additional information on the expected life of the microchannel for a respective geometry, and would give a more well defined design space.
7 Bibliography


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Tennessee, Noxville, 2005.