Chapter One: Introduction

(1.1) Friction Stir Welding

Friction stir welding (FSW) is a relatively new, solid-state, metal joining process invented by Wayne Thomas et al. at the Welding Institute of England (now TWI) in 1991 [1]. A FSW butt weld is made by following the joint-line of two butted pieces of material with a rotating tool while applying a significant down-force through the tool. Heating is provided at or near the tool-work interface by the rotating tool, traditionally a single piece comprised of a shoulder, which contacts the material surface, and a submerged probe. In addition to providing heat, the shoulder also serves to contain the deforming metal beneath it. The process is simple in that it involves only the movement of a non-consumable tool through a joint. This apparent simplicity is however only superficial as the actual mechanisms of weld formation are complex and highly coupled.

Most generally, a weld is formed via the mechanical deformation of a thin layer of plasticized work material surrounding the tool. Projected along the joint-line, this becomes a channel of welded material. During a weld, tool motion and pressure combine to create material strain along with viscous and frictional heating at or near the weld interface. The result is plasticity and large scale deformation. The work material in this region is deformed to such a degree that its deformation is frequently referred to as flow, despite minimal fluid-state material at welding parameters which are reasonable for a given material and tooling.

(1.2) Computational Modeling

FSW is not unlike other industrial process in that its primary development has been made via empirical observation. Much can be learned about a process, particularly in its nascent stages, through the sometimes arbitrary adjustments to parameters and tooling. However, past advancements in our understanding of various other processes have been made with the aid of computational models and there is no doubt that this will more and more frequently be the case with computing power perpetually increasing. Computational modeling will likely guide task specific tool geometry selection and weld parameter optimization. Additionally, FSW computational modeling has potential for the determination of weld microstructure, residual stress, and defect formation [3].

Computational, or numerical models as they are sometimes called, require process specific input data for the assignment of initial conditions, boundary conditions, and material properties. Input data is obtained from experiment and mathematical models of the various process mechanisms. These process models can be analytically based,
empirically based, or based on some combination of the two. A common and acceptable approach is the use of an analytically developed equation containing parameters dependent on experimental results. This construct is often preferable to a purely empirical approach (e.g. curve fitting) as the researcher is left with more intuitively useful results. An example of one frequent application of this technique in FSW modeling is the representation of tool contact condition (particularly shoulder contact condition) for the calculation of heat input. Computational modeling is applied to a system of analytical equations whose complexity renders its solution by traditional means exceedingly tedious.

Computational modeling of the FSW process involves an intense treatment of the thin region in the vicinity of the tool-work interface. FSW model complexity lies not in the geometry or kinematics of the tool, but in the mechanisms by which a weld is created in the work material. The material flow is limited to the region enveloping the tool and process variables in this region are coupled in a manner which excludes an independent treatment. Viscosity, for example, is strongly dependent on temperature and strain rate; which are themselves dependent on the frictional condition at the interface. The frictional condition at the interface is however highly dependent on material viscosity, creating a closed loop relationship. Viscous heating (or plastic dissipation) and frictional heating at or near the tool surface produce steep temperature gradients in both the work material and the tool itself near their interface. The steep process variable gradients in this region are contrasted with the lack of such gradients throughout the remainder of an FSW model.

As discussed above, computational (or numerical) modeling is an evolved form of process modeling. The topic of process modeling in FSW can be subdivided into four somewhat distinct areas: heat generation, thermal boundary conditions, material thermophysical properties, and material flow. Heat generation or power input is arguably the starting point in any such modeling. Thermal boundary conditions are then necessary to establish a control volume. Material thermal properties in turn govern heat conduction within that volume and establish a temperature gradient. Material flow is obtained as a consequence of these forces and the tool motion. Tool motion drives the evolution of each of these concepts but is itself trivial.

Chapter Two: Process Models

(2.1) Heat Input

Experimental and Analytical Assignment of Heat Input Boundary Conditions:

The heat generated during the weld process is equivalent to the power input into the weld by the tool, minus some losses due to micro-structural effects[10] and potentially due to other effects. This total heat input or heat generation includes heat conducted up the tool and not simply into the weld material. Longitudinal travel of the tool can be neglected [2.] St-Georges et al. [63] report the tool travel contribution to be typically less than 5% for 6mm thick AA6061 plate. The power dissipated by the tool

2
(power input) can be obtained experimentally from the weld moment and spindle speed [8-11, 19, 17, 29, 44, 63]:

\[
    \text{Power} = P = \omega M + F \cdot v \quad (2.1)
\]

where \( \omega \) is the tool rotational speed (rad/s), \( M \) is the measured torque (N-m), \( F \) is the traverse force (N) and \( v \) (m/s) is the traverse velocity. The heat input near the interface is therefore:

\[
    Q = P \eta
\]

where \( \eta \) (sometimes \( \beta \)) is the fraction of power dissipated by the tool into the weld material and the tool that is directly converted into heat. This value is understood to be high. Santiago et al., [10], estimate this value to be 0.9 and De Vuyst et al. [65] quote a range of 0.9-1.0. Nandan et al. [7] refer to this as the power efficiency factor, \( C_f \). Most assume this value to be 1.0 and define the weld efficiency simply as weld power minus the portion of the heat dissipated via conduction through the tool [8, 9, 23-26].

Regardless, the percentage of mechanical work dissipated by the tool which is converted to heat is very high and likely negligible. The fraction of total heat dissipated through conduction up the tool shank is discussed extensively in the proceeding section on thermal boundary conditions.

Analytical models of heat input aim to estimate power input in the absence of weld moment data or to predict heat input based on material properties and weld parameters. These models also address the contact conditions at the tool interface. The so-called friction model states that an incremental heat input contribution, \( dq \), for a given area, \( dA \), can be expressed in terms of the contact shear, \( \tau_{\text{contact}} \), at a given centroid radius, \( r \):

\[
    dq = \omega \tau_{\text{contact}} r \cdot dA \quad (2.2)
\]

The above form was developed by [13] for friction welding and has since been frequently used as the basis for obtaining values of heat input at tool surfaces for FSW [10, 12, 14, 18, 19, 21, 28].

One can see that an analytical prediction of the total heat input is dependent on the tool contact condition. Of consideration are the total area of contact and the nature of contact. The contact area can include all or part of the surface and the nature of this contact can be stick, slip, or stick/slip.

In the case of a stick condition (\( \mu = 1 \)), the power is equivalent to that necessary for interfacial shearing of the material in the vicinity of the tool. For a finite area \( dA \) at the tool surface at a centroid radius, \( r \), from the tool rotational axis; the corresponding, heat input is:

\[
    dq = \omega \tau_{\text{yield}} r \cdot dA \quad (2.3)
\]
where $\omega$ is the radial velocity of the tool and $\tau_{\text{yield}}$ is the shear strength of the weld material. The shear strength of the material is obtained from the yield strength of the material by applying the von Mises yield criterion in uniaxial tension and pure shear (as is the case for the weld material at the tool surface):

$$\tau_{\text{yield}} = \frac{\sigma_{\text{yield}}}{\sqrt{3}}$$

The yield strength is strongly dependent on temperature and to a lesser extent, strain rate. This relationship will be discussed to a great extent in the material thermal properties section.

For a slip or frictional ($\mu<1$) interface condition, the torque required to overcome friction is used to define heat input. Here the incremental heat input is defined as:

$$dq = \omega \tau_{\text{friction}} r dA = \omega \mu p r dA \quad (2.4)$$

Where $p$ is the average tool surface pressure over the incremental area. In the case where $\mu p > \tau$, one must differ to the first case as interfacial shearing will occur.

Schmidt et al. define an additional variable, the contact state variable ($\delta$), for the slip/stick contact condition:

$$\delta = \frac{V_{\text{matrix}}}{V_{\text{tool}}}$$

Where $V_{\text{matrix}}$ represents the velocity of the material adjacent to the tool surface. These three cases from the following contact condition table[23]:

<table>
<thead>
<tr>
<th>Condition</th>
<th>Matrix velocity</th>
<th>Tool velocity</th>
<th>Shear stress</th>
<th>State variable</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sticking</td>
<td>$v_{\text{matrix}} = v_{\text{tool}}$</td>
<td>$v_{\text{tool}} = \omega r$</td>
<td>$\tau_{\text{friction}} &gt; \tau_{\text{yield}}$</td>
<td>$\delta = 1$</td>
</tr>
<tr>
<td>Sticking/sliding</td>
<td>$v_{\text{matrix}} &lt; v_{\text{tool}}$</td>
<td>$v_{\text{tool}} = \omega r$</td>
<td>$\tau_{\text{friction}} \geq \tau_{\text{yield}}$</td>
<td>$0 &lt; \delta &lt; 1$</td>
</tr>
<tr>
<td>Sliding</td>
<td>$v_{\text{matrix}} = 0$</td>
<td>$v_{\text{tool}} = \omega r$</td>
<td>$\tau_{\text{friction}} &lt; \tau_{\text{yield}}$</td>
<td>$\delta = 0$</td>
</tr>
</tbody>
</table>

[23]

The matrix velocity and coefficient of friction, and therefore the contact state variable and frictional shear respectively, must be adjusted to obtain an agreement with experiment.

An intense debate exists over the predominant nature of the contact condition. The sum of the heat contributions of all incremental interface surfaces is equal to the total heat input from the tool and is some large fraction of the experimentally determined weld power:

$$Q = dq_1 + dq_2 + \ldots \quad (2.5)$$
The surface of a tool with radial symmetry can be divided into horizontal sections (e.g. shoulder, cylindrical probe bottom), vertical sections (e.g. cylindrical probe wall), and inclined sections.

For horizontal sections, a heat input value can be obtained via (2.2): 

$$ Q_{\text{horizontal}} = \int_0^{2\pi} \int_{R_1}^{R_2} \omega \cdot T_{\text{contact}} \cdot r^2 \partial r \partial \theta $$

$$ = \frac{2}{3} \pi T_{\text{contact}} \omega (R_2^3 - R_1^3) \ (2.6) $$

And for vertically oriented tool faces:

$$ Q_{\text{vertical}} = \int_0^{2\pi} \int_0^{H_{\text{wall}}} \omega T_{\text{contact}} R_{\text{wall}}^2 \partial z \partial \theta $$

$$ = 2 \pi T_{\text{contact}} \omega R_{\text{wall}}^2 H_{\text{wall}} \ (2.7) $$

In the case of faces inclined with respect to the horizontal axis by an angle less than 45 degrees, Schmidt et al. introduce an inclination factor. The inclination factor treats the heat contribution as a summation of the vertical and horizontal contributions of the surface. The factor is applied to shallow angle (<45° off horizontal), concave surfaces (i.e. a concave shoulder), but is presumably meant to apply to shallow angle, convex (or conical) surfaces as well.
The inclination factor is applied as follows to equation (2.6):

\[ Q_{\pi} = \frac{2}{3} \pi T_{\text{contact}} \omega (R_z^3 - R_i^3) \tan(1 + \alpha) \quad \text{for } \alpha \leq 45^\circ \text{ off horizontal plane (2.8)} \]

This form of approximation can presumably be extended to faces inclined by an angle greater than 45° with respect to the horizontal axis by applying the same inclination factor formulation with respect to off-vertical inclinations of less than 45°. An inclination with respect to the vertical axis could be applied to the heat generation equation for vertical surfaces, equation (2.7):

\[ Q_{\pi} = 2\pi T_{\text{contact}} \omega R_z^2 H_{\text{wall}} \tan(1 + \alpha) \quad \text{for } \alpha \leq 45^\circ \text{ off vertical plane (2.9)} \]

These approximations are clearly overestimations of the effective surface area of the inclined face. In addition, the author points out the limitation of the face heat contribution approach only to surfaces with rotational symmetry. This excludes the direct treatment of threads or flutes with this approach. Simplifications must be made to the calculations for these surfaces and things such as effective surface area, radial distance, and facial orientation may need to be approximated.

Tool tilt angle, \( \theta \), is accounted for with the introduction of an additional factor expressed in terms of the tilt angle [18]. This factor is applied to equation (2.6).

\[ Q_{\text{tilt \_ surf}} = \frac{2}{3} \pi T_{\text{contact}} \omega (R_z^3 - R_i^3) \left( \frac{1}{\cos \theta} \right)^3 \quad (2.10) \]

With this factor, heat generation is increased at increasing tool tilt angles. The factor assumes that the shoulder plunge depth is adjusted to maintain shoulder contact on the leading edge of the weld as tool tilt angle increases.

Colegrove et al. [17] estimate the ratio of heat generated by a cylindrically threaded probe to be 20% of the heat generated by the shoulder and probe using the following formulation of equation (2.7):
where \( r_p \) is the probe (pin) radius, \( h \) is the probe height, \( \bar{Y} \) is the average shear stress over the probe surface, \( \mu \) is the coefficient of friction, \( F \) is the translational force during welding, and \( \lambda \) is the thread helix angle. The remaining terms are defined as follows:

\[
\begin{align*}
\theta &= 90 - \lambda - \tan^{-1}(\mu) \\
V_m &= \frac{\sin \lambda}{\sin(180 - \theta - \lambda)} V_p \\
V_{rp} &= \frac{\sin \theta}{\sin(180 - \theta - \lambda)} V_p \\
V_p &= \omega r_p
\end{align*}
\]

Russell et al. [12] estimate the contribution of the probe to the total heat generation near the interface to be approximately 2% using equation (2.7) and assuming the contact shear stress to be 5% of its room temperature value (taking into account material softening with temperature and strain.) Simar et al. [8] estimate 12% for this same value. These analytically calculated probe contributions are tool geometry, material, and parameter dependent, as are the empirical values.

Debate exists over the predominant nature of the tool interface contact condition, particularly for the shoulder, and how best to model it. Schmit et al. [24] assume full shoulder contact with a constant frictional coefficient over the shoulder surface. Under these conditions, slip occurs where the frictional shearing does not exceed the shear yield strength of the material, and stick occurs elsewhere. Colgrove et al. [22] use a stick condition with an artificially small contact radius and achieve good agreement with experiment. The authors however concede this approach to be physically unrealistic. Its use is justified as a simplification. The authors state that a slip condition likely predominates near the shoulder edge but the corresponding interfacial shear stress is too difficult to estimate.

In addition to the torque based method of empirical heat input estimation, the heat input into weld material has been estimated via inspection of the width of the weld heat affected zone (HAZ) [20.] Others have used a combination of imbedded thermocouples readings and HAZ size measurements [14, 15, 16, 19.] Although individually these studies cover a narrow range of parameters, together they show a strong correlation between heat input to the weld and the size of the HAZ. Heurtier et al. successfully correlate microhardness readings with weld temperature [21.]
These experimental measurements are required in the implementation of a realistic computational model. Heat generation and heat dissipation must be tuned and balanced to obtain agreement with experimental temperature data. This process will be discussed in more detail in the proceeding section on thermal boundary conditions.

**Model Dependent Heat Generation:**

In each of the methods above, either the heat input or the temperature of the shoulder and pin surfaces are obtained via analytical and empirical means outside of the model and applied as a boundary condition to the model. Schmidt et al. [25], De Vuyst et al. [65] and Santiago et al. [10] apply model dependent plastic dissipation heat generation. The region of volumetric type heating is experimentally confirmed and agreed upon in literature to occur in the so-called thermomechanically heat affected zone (TMAZ) where plastic dissipation occurs. The minimum extend of the TMAZ is the width of the shoulder at the material surface and the width of the pin at the weld root [4, 36-38]. Plastic dissipation heating is applied to each element of the model as the product of the deviatoric stress tensor, \( s_{ij} \), the plastic strain tensor (also called deformation rate), \( \dot{e}_{ij}^{pl} \), [43] and a plastic energy dissipation efficiency term, \( \eta_{pd} \):

\[
q_{vol} = \eta_{pd} s_{ij} \dot{e}_{ij}^{pl} \tag{2.12}
\]

This efficiency term, \( \eta_{pd} \), is known in some fields as the Taylor-Quinney coefficient, \( \beta \). Its value is typically between 0.9 and 1.0 [65]. Here the efficiency term is adjusted within the model to conform with the experimentally observed thermal gradient. He et al. [27] apply an identical formulation but do not mention the use of an efficiency term. Frictional heating on the surface of the material cannot be applied in a model dependent fashion as the pressure and frictional coefficient terms (and thus contact shear stress) in equation (2.4) are difficult to reliably reproduce in model.

(2.2) Heat Dissipation and Thermal Boundary Conditions

The discussion of heat generation above applies to the thermal boundary conditions at the weld interface. For the establishment of a control volume, thermal boundary conditions must be established on all the model external surfaces in addition to heat input at and near the interface. Heat loss occurs via conduction to the tool, the backing plate, and any unmodeled portion of the weld material. Convective heat loss to the surrounding atmosphere also occurs. The heat lost through convection is considered negligible and has been shown in numerous studies to have no effect on temperatures within the weld plate [3].

Of the total heat generated near the welding interface, a majority is dissipated by way of the plate as opposed to via the tool shank. Knowing the temperature at two points along the length of the tool, the heat dissipated through its shank can be calculated:
\[ Q_{\text{tool}} = \frac{\lambda_{\text{tool}}(T_1 - T_2)}{d} \] (2.13)

where \( \lambda_{\text{tool}} \) is the thermal conductivity (e.g. W/mK) of the tool shank, \( T_1 \) is the temperature at a position near the shoulder, \( T_2 \) is the temperature at a position further up the shank, and \( d \) is the distance along the height of the shank from thermocouple position one to two. Again, convective losses associated with the rotating tool in air are believed to be negligible, but may be modeled using a surface convective coefficient. The heat dissipated through the tool can be compared with some calculated value of total heat input to produce a ratio some call the weld efficiency [8, 9, 25, 29] (\( \eta_{\text{weld,thermal}} \) is used here for clarity):

\[
\eta_{\text{weld,thermal}} = \frac{Q_{\text{shoulder}} + Q_{\text{probe}}}{Q_{\text{shoulder}} + Q_{\text{probe}} + Q_{\text{tool shank}}} = \frac{Q_{\text{weld}}}{Q_{\text{total}}} = \frac{Q_{\text{weld}}}{\eta_{\text{power to heat}} P_{\text{dissipated mechanical}}}
\]

This ratio will vary with variations in the experimental setup and parameters. Schmidt et al. [25] estimate the fraction of total heat input transferred to the work piece to be approximately 75% in a particular set of experimental welds. Simar et al. [8] arrive at a value of 95% and Dickerson et al. [29] at a value of 92% by placing thermocouples on the tool shank and throughout the weld plate.

The weld heat input, \( Q_{\text{weld}} \), can be divided into surface and volume heat contributions due to frictional or viscous (plastic dissipation) heating respectively. Simar et al. [9] introduce a term, \( \gamma \), for this purpose:

\[
Q_v = \gamma Q_{\text{weld}}
\]

\[
Q_s = (1 - \gamma) Q_{\text{weld}}
\]

Where \( Q_v \) is the volume heat contribution and \( Q_s \) is the total tool surface heat contribution. In [9], it is concluded that a value of \( \gamma = 0 \) produces best agreement with experimental thermal data for thermal computational models which take into account fluid flow. This is presumed to be due to both the thin nature of the deforming layer around the tool and the decreasing rates of strain and deformation occurring as one moves away from the tool.

The heat input on the tool surface is typically treated by defining a constant heat flux (e.g. W/m²) over the interface surface or one which varies with some spatial parameter, typically radius. While frictional heating does occur essentially at the interface, the assignment of the viscous dissipation heating contribution to the interface is an approximation based on the assumption that the layer of dissipation surrounding the tool is reasonably thin. Again, this assumption can generally be made without loss of model accuracy for thermal-fluid models. The heat generation in both the pin and shoulder is typically treated as axisymmetric since tool rotation and circumferential
material flow around the tool largely render this to be the case. The following formulation for shoulder surface heat input, $Q_{ss}$, distribution over the tool shoulder is commonly used [8,9,26]:

$$q_{ss}(r) = \left( \frac{3}{2\pi} \right) \left( \frac{Q_{ss} \cdot r}{R_s^3 - R_p^3} \right) \quad \text{with } R_p \leq r \leq R_s \quad (2.14)$$

Shoulder heat input per unit area increases with radial distance from the probe surface as the tangential velocity increases. The probe side surface heat input, $Q_{ps}$, is distributed as an even heat flux over its height:

$$q_{p\_side}(r) = \left( \frac{1}{2\pi} \right) \left( \frac{Q_{ps}}{R_p H_p} \right) \quad (2.15)$$

Simar et al. [9] neglect the probe tip surface heat input distributing the full probe heat contribution over its side surface. A probe tip surface distribution could be formulated in the manner of equation (2.14) (with $R_p=0$, $R_s= R_p$, and $Q_{ss} = Q_{probe\_tip}$) if desired. $Q_{ss}$, $Q_{ps}$, and $Q_{probe\_tip}$ can be found analytically via equations (2.6), (2.7), and (2.6) respectively. Heat generation in the pin can be modeled as a uniform, volume heat source (i.e. W/m$^3$) without loss of model accuracy.

The majority of heat of heat is dissipated ultimately through the backing plate (through the clamps, un-modeled plate portions of the plate, and directly through the backing plate) as opposed to the tool shank and surface convection of the plate in air [3]. The primary method [22] of achieving good agreement with experimental thermal gradients is the method of adjustment of model heat input and backing plate heat dissipation used by [30-35, 16, 17]. Heat transfer between the backing plate and work material is often modeled using a uniform heat transfer coefficient over the interface. Colegrove et al. [22, 3] note however that the contact condition under the tool is more intimate than elsewhere at the backing plate and weld plate interface due to the pressure and heat present. In addition, during a full penetration weld the thermomechanically heat affected zone (TMAZ) extends to the backing plate resulting in a more intimate contact in the region passed over by this zone. Weld material fills microscale scratches and notches in the backing plate and as a result there exists a higher conductance over the interface in this area. Based on these observations two improvements over the uniform interfacial conductance have been suggested. The first method is to assign a temperature dependant conductance at the backing plate top surface. In some numerical models it may be a simple matter to increase thermal coupling with temperature on interface. The second method is to assign one coefficient of conductance to the general interface and a larger coefficient to the area under the tool and the tool path behind the tool:
The added complication of these methods is warranted due to the importance of the heat removal of the backing plate.

Conduction losses to the weld plate are accounted for within the model by assigning a uniform heat transfer coefficient over the modeled/unmodeled weld plate boundary. This boundary is generally greater than one shoulder diameter from the tool axis of rotation and where the approximation of a uniform conductance is acceptable due to small temperature gradients. The heat loss over a cylindrical boundary surrounding the weld tool can be approximated in a radial fashion with temperature, \( T \), at some radial distance, \( r \), is defined by:

\[
T(r) = T_0 + \frac{Q_{wp}}{2\pi kh} \ln \frac{R_o}{r}
\]

where \( Q_{wp} \) is the heat dissipated to the weld plate, \( T_0 \) is the temperature at a distance \( R_o \) from the axis of rotation of the tool (average temperature on an interior cylindrical face, e.g. interface temperature at probe radius), \( k \) is the thermal conductivity of the weld metal, and \( h \) is the height of the cylindrical conduction zone (usually height of the pin or the thickness of the weld plate.) Knowing the temperature at the interior and exterior surface boundaries, the total heat loss over the exterior surface and to the unmodeled weld plate can be calculated:

\[
Q_{wp} \approx \frac{2\pi kh(T - T_0)}{\ln \frac{R_o}{R}}
\]

Where \( R \) is the radius of the exterior cylindrical surface (i.e. modeled/unmodeled plate boundary.)
Heat loss from the weld plate top surface to the surroundings is governed by radiation and convection. The heat loss per unit area on the top of the plate can be approximated using the following equation:

\[ q_{top} = \sigma \varepsilon (T^4 - T_a^4) + h_i (T - T_a) \]

Where \( \sigma \) is the Stefan-Boltzmann constant, \( \varepsilon \) is the emissivity of the plate, \( T \) is the temperature of the plate top surface, \( T_a \) is the ambient temperature, \( h_i \) is the convective heat transfer coefficient of the plate in air. As discussed earlier in this section, the heat lost in this way is low and is often ignored.

(2.3) Material Thermo-Physical Properties

Heat flow within the modeled volume is dependent on material flow around the tool and on material thermal properties. It is governed by the steady-state, conductive-convective equation:

\[ \rho \cdot c_p \cdot \nabla \theta = \nabla \cdot (k \nabla \theta) + \dot{Q} \]

Where \( k \) is thermal conductivity, \( c_p \) is specific heat, \( \rho \) is density, \( \theta \) is the temperature and \( \dot{Q} \) is the internal heat generation rate. The temperature gradient is nearly symmetric (radial) about the tool axis of rotation as a consequence of material flow. Heurtier et al. [21] note a slight asymmetry about the weld line with the higher temperatures found on the advancing side. The traverse direction opposes tool motion on the advancing side resulting in a vortex velocity field. It is noted that a material element spends more time on the advancing side due to the complicated nature of the flow on this side as compared to the retreating side where material flow is largely in the direction of tool motion. Crawford et al. [52] note a similar asymmetry in Fluent CFD models of threaded pins. Higher rotational speeds are also seen to shift the HAZ further towards the advancing side.
Conductive heat flow is governed by thermal conductivity ($k$ or $\lambda$), specific heat ($c_p$), and density ($\rho$). These properties are each dependent on temperature and, for alloys, microstructure evolution [3]. The dependence on temperature for aluminum alloys is known in the absence of other effects. (Table from [5])

**Recommended values for the thermophysical properties of Al alloy - 6061-T6**

<table>
<thead>
<tr>
<th>T °C</th>
<th>Density kg m⁻³</th>
<th>$C_p$ J K⁻¹ g⁻¹</th>
<th>$H_{f-H}_{g}$ J g⁻¹</th>
<th>$10^4$ a m² s⁻¹</th>
<th>$\lambda$ W m⁻¹ K⁻¹</th>
<th>$\eta$ mPa s</th>
</tr>
</thead>
<tbody>
<tr>
<td>25</td>
<td>2705</td>
<td>0.87</td>
<td>0</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>100</td>
<td>2695</td>
<td>0.95</td>
<td>69</td>
<td>76</td>
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<tr>
<td>200</td>
<td>2675</td>
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<tr>
<td>600°</td>
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<td>596</td>
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</tr>
<tr>
<td>642°</td>
<td>2580</td>
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<td>[645]</td>
<td>-</td>
<td>90</td>
<td>[1.15]⁺</td>
</tr>
<tr>
<td>800°</td>
<td>2415</td>
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<td>981</td>
<td>32</td>
<td>91</td>
<td>[1.05]⁺</td>
</tr>
<tr>
<td>700°</td>
<td>2400</td>
<td>1.17⁺</td>
<td>1049</td>
<td>32.5</td>
<td>92</td>
<td>[1.0]⁺</td>
</tr>
</tbody>
</table>

⁻ = melting range  
⁺ = extrapolated value  
⁺⁺ = estimated value
The complex and chaotic evolution of weld microstructure including the dissolution of precipitates and local melting has a significant effect on the thermal properties of the metal. A number of studies \[40–42, 45–48\] show that precipitates are significantly coarsened in the HAZ in comparison to those in the unaffected base plate and weld nugget. Su et al. \[50\] report on precipitate evolutions occurring in AA7050 FSW. Their findings showed a coarsening of precipitates from the base plate into the TMAZ, with increasing dissolution and re-precipitation occurring from the TMAZ to the interior weld nugget. Sato et al. \[49\] looked at a number of locations in the HAZ and the weld nugget of AA6063 FSW and observed that the precipitates experienced increasing dissolution toward the weld center.

The computational cost of modeling the effect of microstructure evolution is however too high and its effects are therefore not modeled. This unmodeled contribution is masked by comparable uncertainties elsewhere in the model \[3\]. Temperature dependent values like in the table above are often used. Colegrove et al. \[22\] report better agreement with thermocouple temperature gradients using a constant thermal conductivity value, noting that data book values represent an equilibrium state rather than the rapid thermal cycles present in FSW.

Flow stress in aluminum alloys is dependent on temperature and strain-rate. Sellars and Tegart \[6\] proposed an initial formulation which represented the TMAZ region as a rigid, visco-plastic fluid. Sheppard and Wright \[39\] modified their formulation into the following commonly used form:

\[
Z = \dot{\varepsilon} \exp\left(\frac{Q}{RT}\right) = A(\sinh \frac{\alpha \sigma_e}{A})^n
\]

or equivalently \[43\]

\[
\sigma_e = \frac{1}{\alpha} \sinh^{-1} \left( \left( \frac{Z}{A} \right)^{\frac{1}{n}} \right) \quad \text{or equivalently} \quad Z = \dot{\varepsilon} \exp\left(\frac{Q}{RT}\right)
\]

where \(\alpha, A, n\) are material constants (\(\alpha = 0.045\) (Mpa))\(^{-1}\), \(Q = 145\) kJ mol\(^{-1}\), \(A = 8.8632E6\) s\(^{-1}\), \(n = 3.55\), \(\sigma_e\) is the equivalent steady state flow stress (Mpa), \(R\) the gas constant (\(R = 8.314\) mol\(^{-1}\)K\(^{-1}\)), \(T\) the absolute temperature (K), \(Q\) is the activation energy (J mol\(^{-1}\)) and \(Z\) is the Zener-Hollomon parameter. The material constants used are determined using a standard compression test. The material viscosity is defined in the following manner:

\[
\mu = \frac{\sigma_e}{3\dot{\varepsilon}}
\]

The visco-plastic model holds at temperatures reasonably below the metals solidus temperature. As the material approaches the solidus temperature significant softening occurs. Seidel and Reynolds \[51\] proposed reducing the flow strength in a linear fashion.
from its value at some arbitrary temperature, $T_m$, to zero at the solidus temperature, $T_s$.

[51] and [22] define the so-called empirical softening regime by setting $T_m$ at 50°C below the solidus temperature.

![Graph showing Constitutive behavior of Al-7449 (T_s-T_m=50°C and T_s=500°C)](image)

*Figure from [22]: Constitutive behavior of Al-7449 (T_s-T_m=50°C and T_s=500°C)*

Increasing the range of the empirical softening regime is found to have the effect of increasing the size of the TMAZ in thermo-mechanical models. A Carreau viscosity model is employed by Atharifar et al. [67] and is discussed later with computational modeling studies.

(2.4) Analytical Models of Material Flow

Schnieder and Nunes [57] attempt to dissect the FSW metal flow into three separate incompressible flow fields:
Figure from [57]: Three flow fields a) rotation b) translation c) ring vortex

It is postulated that the summation of these three fields results in a thru-current on the retreating side and a maelstrom current surrounding the probe:

Figure from [57]: Summation of three flow fields yields two flow currents

The downward spiral exhibited in the maelstrom current is said to be a result of the ring-vortex which is in turn a consequence of the downward effects of probe threading (left-hand threaded probe spun clockwise or right-hand threaded probe spun counterclockwise.) This downward forcing is claimed to be reversible with opposite threading. One consequence of this deconvolution model is that material in the
advancing side flow current resides in the flow region longer than that on the retreating side. The model further attempts to explain the onion ring surface microtextures and the entrained oxide film defect found.

An inverted x-ray tomography study [58] provides some confirmation of the two flow theory. The figure below shows the post-weld distribution of two streams of lead marker material placed in two positions parallel to the weld line. The marker material along the center of the weld line is deposited in a chaotic fashion behind the tool whereas the marker material placed further to the retreating side is deposited in a linear fashion consistent with the thru-current flow path.

![Image](image_url)

*Figure from [58]: Inverted x-ray radiography a) centerline marker dispersal (consistent with maelstrom flow path) b) retreating side marker dispersal (consistent with thru-current flow path)*

Abregast [59] presents a metalworking model of FSW flow dividing the process into five sequential processes: preheat, initial deformation, extrusion, forging, and post-weld cool-down. This model has the probe forcing material downward during the initial deformation zone except near the shoulder where it is initially forced upward. The material is then rotated around the pin and deposited in the cavity behind the moving tool during the extrusion period. The forging period encompasses consolidation of the extruded material by the downward pressure of the shoulder heel.
Chen et al. [66] use a probe breaking technique to observe in-process weld dynamics. The author notes that this technique results in the probe slowing to a stop over some unknown number of revolutions after breaking from the shoulder.

It can be seen that shear layers are deposited sequentially along the trailing edge of the weld from the retreating side to the advancing side. The author uses the rotational speed of the tool, the travel speed, and the distance traveled by each successive shear layer to
calculate the shear velocity. The shear velocity is calculated to be ~16mm/s while the periphery of the pin is traveling at ~235mm/s. This results in a shear/pin periphery velocity of 0.07 (where 0.0 is pure slip and 1.0 is pure stick.) The authors therefore conclude a slip condition to be dominant in this situation. It is mentioned that this ratio compares well with results obtained by Schmidt et al. [24] who arrive at a values in the range of 0.1-0.3. The authors of [66] further contend that a liquid film exists at the shear interface which facilitates the slip condition.

Chapter 3:

Computational Modeling Studies:

Williams et. al [60] use a 2D-axisymmetric flow model coupled with a 3D translating thermal model and COSMOL multi-physics software to simulate the FSW process. The figure below depicts their methodology.

Figure from [60]: Input/output flow diagram for the 2D and 3D models

Using a slip condition at the shoulder interface, a good agreement was made with experimental macrosections on the extent of the TMAZ.

Figure from [60]: Rotational flow diagrams from 2D model and corresponding weld macrosections. Welds made in AA2014 at 200mm/min (Left: 1600rpm, Right: 400rpm).

The paper further attempts to integrate a residual stress, microstructure development, and hardness prediction submodels within the same software package. It is concluded by the authors that this technique has the potential for streamlining FSW modeling by obtaining many outputs related to weld performance from a single input/output process.
Schmidt and Hattel [61] use computational tomography (CT) to observe the flow of marker material around the tool and varying depths and compare this with a CFD model of flow paths. The weld is stopped mid-process and the weld sample is sliced horizontally. The deflection of marker material towards advancing side behind the probe is noted in slices near the shoulder (first slice is just below the shoulder and slice depth increases successively.) The slices show a reduced shoulder influence with depth.

Figure from [61]: 2D overhead CT scans of horizontal slices showing the dispersal of marker material by a FSW tool. The inner and outer rings represent the outline of the pin and shoulder respectively. Slices are in order of increasing depth from shoulder and are in 0.25mm increments.

Experimental results are compared with an analytical streamline model and a COSMOL 3.2 FE model streamline. A good agreement is found between the three data sets:
Sato et al. [62] use Acusolve™ CFD software based on the Galerkin/Least-Squares FE method to obtain temperature and velocity field data at various parameters for a self-reacting FSW tool. An Eulerian (or ALE) framework is used. A visco-plastic model is used along with a slip-stick boundary condition at the shoulder. A schematic of the model mesh is shown below with increasing refinement with tool proximity.
A rotating mesh is used around the tool with a sliding interface at the mesh exterior surface. Temperature contours are presented over a horizontal cross-section in the vicinity of the tool and over the modeled portion of the A6NO1 alloy aluminum plate.
Figure from [62]: Temperature contour over the plate(left), and in the vicinity of the tool(right).

St.Georges et al. [63] use CosmosFlow CFD code to develop a thermal-fluid model of FSW in 6mm thick AA6061 plates. A liquid with temperature dependent viscosity is used with adjustment based on experimental material behavior. Other material properties (\( \rho \), \( c_p \), \( k \)) are assigned constant values. Model temperature distributions are presented for the weld conditions below. Tool rotation is initially not considered.

<table>
<thead>
<tr>
<th>Material</th>
<th>AA6061-T6</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thickness</td>
<td>12.7 mm</td>
</tr>
<tr>
<td>Shoulder radius</td>
<td>25 mm</td>
</tr>
<tr>
<td>Pin radius</td>
<td>6 mm</td>
</tr>
<tr>
<td>Pin length</td>
<td>11 mm</td>
</tr>
<tr>
<td>Rotational speed</td>
<td>637 rpm</td>
</tr>
<tr>
<td>Traverse speed</td>
<td>1.59 mm/s</td>
</tr>
<tr>
<td>Vertical force</td>
<td>25 kN</td>
</tr>
<tr>
<td>Friction coefficient</td>
<td>0.4</td>
</tr>
<tr>
<td>Shear stress</td>
<td>5.40 MPa</td>
</tr>
<tr>
<td>Shoulder Torque</td>
<td>174.41 Nm</td>
</tr>
<tr>
<td>Pin Torque</td>
<td>0.09 Nm</td>
</tr>
<tr>
<td>Total torque</td>
<td>176.94 Nm</td>
</tr>
</tbody>
</table>

Table from [63]: CFD and empirical weld parameters and average process force data.

Figure from [63]: Modeled temperature distributions without considering tool rotation.
Temperature contours produced by Song and Kovacevic [64] using identical parameters and similar process forces using a different CFD model are presented for comparison:

*Figure from [64]: Modeled temperature distributions without considering tool rotation.*

It is noted in [63] that CFD models presented in [63] and [64] achieve similar results. St. Georges et al. [64] goes on to present temperature contours with consideration for tool rotation:

*Figure from [64]: Temperature distribution with consideration of tool rotation.*

De Vuyst et al. [65] use MORFEO finite element software and an Eulerian formulation with mixed velocity-pressure discretization for the fluid flow. The work material is modeled with a visco-plastic fluid law. Empirical studies of embedded copper
foil and wire marker material are used for model validation. A simplification of the Norton-Hoff material law is used:

\[ s = 2K(\sqrt[3]{3\dot{\varepsilon}})^{m-1}\dot{\varepsilon} \quad \text{Norton-Hoff Material Law} \]

Where \( m=1 \) results in a Newtonian fluid of viscosity \( K \), \( s \) is viscosity, and \( \dot{\varepsilon} \) is the strain rate. For this model, \( K \) and \( m \) are chosen as constant values averaged over the full temperature range. This results in a weak coupling between the flow and temperature fields. The temperature field is dependent only on viscous dissipation and thus on the flow field. The flow field is however independent of temperature in this model.

The authors have a supercomputer available to them (28 Intel Itanium 2 processors with 2GB per processor RAM memory and 1.4TB available disk space) and their mesh is composed of nearly 1.5 million linear tetrahedral mesh elements and 250,000 nodes. Nonetheless a simplified approach is used with respect to the tool features (shoulder scroll and probe threading.) It was determined that explicit modeling of tool features would be too computationally costly. Unique boundary conditions are therefore established within predefined feature zones. A boundary zone is therefore created around the tool as shown:

Figure from [65]: Finite element mesh with boundary zone around tool surface.

An approach used by Ulysse et al. [43] is applied to the tool boundary zone to account for the effect of tool the tool features. In this technique the velocity field component of the feature effects is added to the velocity field in these zones to account for the downward forcing of the tool threads and the inward radial forcing of the shoulder scrolling. The velocity profile around the tool mesh is presented:
The authors note a larger velocity magnitudes on the retreating side of the weld (parameters are not given.)

The models are compared with a series of marker material imbedded horizontal weld slices. This is similar to the experiments run in [61] with the difference here being the lateral orientation of the marker material plane. Here the method of analysis is specified as metallographic (i.e. macrosections) as opposed to CT scans. The horizontal slices are presented in order of increasing depth(distance below shoulder plane) and the model
predicted marker dispersal is superimposed (gold) over the experimental marker material (white.)
As in [61] one sees the decreased shoulder influence with depth in a reduced deflection of marker material. The authors note a good agreement with experiment.

The following lateral macrosections show the dispersal of marker material imbedded parallel to the weld-line(longitudinally) compared with three model based predictions:
The authors present these macrographs as evidence for the necessity of a 3D thermo-fluid model (as opposed to 2D) which includes some accounting for feature effects. It is noted that the model which does not include imposed velocity fields to account for feature effects results in the simulated longitudinal marker material being deposited in a simple linear fashion, similar to its pre-welded position.

Atharifar, Lin and Kovacevic [67] use Fluent CFD software to identify the occurrence of a downstream stagnation point. The downstream stagnation point has been indicated by Shinoda [67] as the likely point of initiation of the tunnel-like defect in FSW.

The authors use the Carreau model for pseudo-plastics to assign material viscosity:

$$\mu = \mu_0 + (\mu_\infty - \mu_0) \left[ 1 + (\frac{T}{T_0})^{\frac{n-1}{2}} \right]^\frac{1}{\nu}$$
where $\lambda$ is the time constant, $m$ is the power law non-Newtonian fluid index, $\mu_\infty$ and $\mu_0$ are the infinite and zero shear viscosities, $\dot{\gamma}$ is the shear strain-rate, and $T_0$ is a reference temperature. The Carreau viscosity model is found as an embedded function in Fluent. The required constants have been obtained through experiment by Sheppard and Jackson [68.]. A stick condition is used at the tool interface and a uniform heat flux is assigned to the interface surface. The heat input is calculated using equation (2.4).

The downstream dynamic pressure along the traversing direction of the tool is examined at various heights relative to the tool for welds at various parameters. Experimental welds are performed to verify the occurrence of tunnel-like defects and to record the depth at which the defects occur (on the surface or submerged at some depth, $y$). The dynamic pressure trend lines below represent welds and weld heights which do and do not contain defects:

![Dynamic pressure trend lines](image)

*Figure from [67]: Dynamic pressure trend lines for spindle speed $N$ and depth $y$ at weld speed a) $v=30\text{mm/min}$ b)90 mm/min.*

Defects can be identified in the trend lines above by spikes, which correspond to perturbations. In chart a) defects occur in experimental welds of $N=900$ and $N=1400$ at a depth of $y=3.5$. In chart a) defects occur in experimental welds of $N=900$ at $y=3.5$ and $N=1400$ from $y=3.5$ to the surface. By correlating defects in experimental welds with dynamic pressure trend lines from Fluent CFD models the authors are able to accurately predict the parameters at which a tunnel-defect occurs for a given tooling and material along with the depth at which the defect occurs.

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