Investigation and Implementation of a Robust Temperature Control Algorithm for Friction Stir Welding

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Investigation and Implementation of a Robust Temperature Control Algorithm for Friction Stir Welding

Kenneth A. Ross

A thesis submitted to the faculty of Brigham Young University in partial fulfillment of the requirements for the degree of Master of Science

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ABSTRACT

Investigation and Implementation of a Robust Temperature Control Algorithm for Friction Stir Welding

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In friction stir welding, the temperature of the process zone affects the properties of the resulting weld and has a dramatic effect on tool life in PCBN (polycrystalline cubic boron nitride) tools. Therefore an active control system that changes process parameters to control weld temperature is desirable. Mayfield and Sorensen proposed a two-stage control model that contains an inner loop that controls the spindle speed to keep power constant and an outer loop for setting the desired power based on weld temperature. This work contains the analysis and implementation of a temperature control method based on their work.

This research shows that power input to the stir zone leads tool temperature. Due to the inertia associated with the spindle, power control is best achieved by commanding torque rather than spindle speed. Heat transfer in the tool and stir zone is explored and analytical models are developed. It is shown that the temperature response to power is nonlinear. Nevertheless a first-order approximation with time delay is sufficient to select functional controller gains for a PID controller.

Standard manual PID tuning techniques can be used to achieve a desired rise time, settling time and overshoot. Gains for an H-13 tool steel FSW tool were tuned to produce a rise time of approximately 7 seconds, settling time of approximately 30 seconds and overshoot of approximately 30%. Welds were run using these gains in various plate thicknesses, commanded temperatures, backing plates and feed rates. In all cases temperature control functioned properly and the commanded temperature was held with a standard deviation of less than one degree Celsius. Similar results are presented for welds run using PCBN tools.

Keywords: friction stir, welding, temperature, power, control
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1 INTRODUCTION

Controlling the weld temperature throughout the length of the weld is an important undertaking because weld properties, such as fracture toughness and corrosion resistance, vary with weld temperature (Nelson, et al. June, 2000; Prangnell 2010; Reynolds 2007; Sato 2002). If specified properties are desired throughout the weld, the weld temperature must be adjustable and in control throughout the length of the weld.

The short tool life of PCBN tools limits the application of friction stir processing (FSP) in steels and other high-softening-temperature materials. Controlling tool temperature should increase the tool life of PCBN tools. If the temperature is too low, the tool is overstressed by forces that increase as tool temperature decreases. If the tool temperature is too high, PCBN tools fail quickly by chemical wear. High temperature can also cause creep in the locking collar allowing the PCBN insert to rotate. Point stresses will then likely exist at the corners of the insert on cooling that may lead to cracking and failure.

A temperature control algorithm has been proposed that contains an inner loop to keep power constant and an outer loop to set the power based on temperature feedback. The purpose of this research is to develop and validate a dual loop temperature control algorithm.

The foundation of the control algorithm developed in this thesis is the fact that spindle power leads tool temperature. This fact will be proven through analytical models and experimental data. Commanding spindle power to control temperature is a significant paradigm shift for some members of the FSP community.
This thesis outlines previous research and explains theory relevant to temperature control in FSW. Subsequently, additional theory governing power and temperature control in FSP is developed and applied to controller design. Heat transfer analysis is used to identify the form of the system transfer function and system identification is performed. Lastly the performance of the developed algorithm is investigated.
2 PREVIOUS WORK

The first efforts to control weld temperature used passive control techniques. Researchers considered that the weld temperature was proportional to weld pitch (spindle speed)/(travel speed) or various “pseudo heat indexes” which are functions of spindle speed and travel speed (Chimbli 2007; Kalya 2007). Passive control techniques assume that the process has reached a self-limiting equilibrium condition.

Passive control techniques are not adequate for temperature control because equilibrium conditions may not exist along the length of a weld. Causes of temperature changes along the length of the weld include: inadequate cooling of the tool or backing plate, changes in the thermal boundary condition and insufficient time to reach equilibrium. Passive control techniques are not versatile because they do not adjust for process disturbances.

Cederqvist, et al used friction stir welding to seal copper canisters (Cederqvist 2009). Due to the changing thermal boundary conditions throughout the weld cycle the power input was adjusted to keep the temperature within a specified process window. This was accomplished by manually adjusting the spindle speed as the temperature changed. The power input was recorded throughout the weld cycle. The power input from multiple runs was used to create a model of what the power input should be at each position in the weld cycle to keep the weld temperature in the middle of the process window. Cederqvist, et al. discuss the possibility of using this model along with thermocouple data to develop closed-loop temperature control for the process.

Fehrenbacher et al. present the results of using a closed-loop temperature control system in the FSW of aluminum (Fehrenbacher 2010). Spindle speed was adjusted in response to
temperature feedback. Welds were run where the backing plate material changed halfway through the weld. Without temperature control the temperature gradually increased as the plate heated. The temperature dropped about 50 degrees as the plate material changed then continued to rise gradually. Repeating this weld with temperature control resulted in a constant temperature before and after the backing plate material change and only a 20 degree drop and rise during the material change. The temperature drop began approximately 2 seconds before the center of the tool reached the point where the workpiece material changed and ended approximately 4 seconds afterward. These results indicate that closed-loop control can reject small disturbances and reduce the error associated with larger disturbances introduced by changes in backing plate material.

Mayfield and Sorensen observe torque leads tool temperature for welds run at constant spindle speed (Mayfield 2010). This is shown explicitly in experimental data presented. A common assumption in the FSP community has been that changes in temperature lead changes in torque. Data presented by Mayfield and Sorensen disproves this assumption.

In order to increase understanding of the system, a simplified thermal model of the stir zone was developed from a simplified thermal balance of the stir zone and is shown in Figure 2-1. This model suggests that the temperature of the stir zone can be controlled by the power input to the stir zone.

Power flowing into the stir zone is given by torque multiplied by the rotational velocity of the tool. Under constant spindle speed, a disturbance in torque causes a disturbance in power which changes the temperature of the stir zone. Spindle speed can be adjusted in response to disturbances in torque to maintain constant power. If thermal boundary conditions are constant, a constant spindle power results in a constant temperature.
Mayfield and Sorensen present an improved temperature control algorithm (Mayfield 2010). The model proposed is a two-stage control model containing an inner and outer loop. The inner loop adjusts spindle speed to keep power constant, while the outer loop sets the desired power based on temperature. Although this control model has not been tested, a simple feasibility test was performed. Temperature control was performed successfully by manually adjusting spindle speed in response to changes in torque.

It is expected that the temperature control algorithm presented by Mayfield and Sorensen will provide a faster response to disturbances than previously developed temperature control methods. The inner power control loop responds to thermal disturbances faster than they are registered by the temperature measurement system. Therefore, the advantage of using the improved temperature control algorithm increases as the time delay of the temperature measurement increases.

Figure 2-1: Thermal balance in an isothermal control volume representative of the stir zone presented by Mayfield and Sorensen
This section provides a summary of relevant control theory as a background for those who are not familiar with control theory. Subjects are presented in the following order: block diagrams, classical control theory, PID control theory, performance metrics and effect of PID gains.

### 3.1 Block Diagrams

A component block diagram of a dual loop control system is shown in Figure 3-1 where $CV$ is the controlled variable, $MV1$ and $MV2$ are manipulated variables, $\Delta$ is the error between the reported and desired value of a variable, subscript $ref$ represents the desired value of the variable, subscript $cmd$ denotes a commanded value, subscript $rep$ denotes a measured value and subscript $real$ denotes the actual value of a variable. The reference signal is the desired value of the controlled variable. A controller determines the systems response to error in the controlled variable. The plant, Figure 3-2, is the combination of the actuator and the process. The actuator is the device that influences the process variable being controlled by adjusting the manipulated variable. The process governs the behavior of the controlled variable.
The outer loop uses the desired value for $CV1$ as the reference. $Controller1$ responds the error in $CV1$ by adjusting $MV1$. $MV1_{cmd}$ is the reference for the inner loop therefore $MV1$ is the controlled variable for the inner loop. $Controller2$ responds to error in the $MV1$ by commanding $MV2$ which is applied to the plant. The measured values of $CV1$ and $MV1$ close the inner and outer loops respectively.

### 3.2 Classical Control Theory

Classical control theory is used for the design and analysis of the control system. To use classical control theory, transfer functions are used to model system components in the frequency
domain. A transfer function is the Laplace transform of the output divided by the Laplace transform of the input of a linear time invariant (LTI) system. A transfer function representation for components such as the plant, sensors and controllers are necessary to use classical control theory.

3.3 **PID Control Theory**

Proportional-integral-derivative (PID) controllers are the most commonly used type of closed-loop feedback controller (Åström 2008). PID control uses the magnitude, integral and derivative of the error to control the process. The proportional gain, $K_p$, determines the response to the current error. The integral gain, $K_i$, determines the response to the integral of past errors. The derivative gain, $K_d$, determines the response based on the rate of change of the error. The PID control algorithm is given below.

$$MV_{cmd}(t) = K_p \Delta CV(t) + K_p \int_0^t \Delta CV(t) + K_d \frac{d \Delta CV(t)}{dt}$$  \hspace{1cm} (3-1)

3.4 **Performance Metrics**

Setpoint tracking is the method this work will focus on for testing the temperature control algorithm. The metrics most commonly used to determine how well a controller can track setpoint changes are rise time, overshoot, settling time and steady-state error. Rise time is the time it takes the response to rise from 10% to 90% of the difference between the setpoint and current temperature. Overshoot is the maximum amount the system overshoots the setpoint divided by the difference between the setpoint and the initial temperature. The settling time is the time required for system transients to decay. This is typically defined as the time it takes for the temperature to stay within 1% of the difference between the setpoint and starting temperature.
from the setpoint. Due to the noise in temperature measurement 10% will be used for determining settling time in this thesis. This corresponds to +/- 1°C degree for the gain study in aluminum where most changes in setpoint is 10°C and +/- 2°C for the gain study in steel where the change in setpoint is 20°C.

3.5 **PID Gains**

In this work, open-loop tuning rules will be used to determine functional PID gains. A step change in the manipulated variable is commanded and the open-loop response of the controlled variable is used to estimate PID gains that will result in specified performance metrics. CHR and Minimum ITAE methods are used in this thesis (O'Dwyer 2006). Often, the performance metrics obtained achieved using calculated gains differ from the predicted intended performance metrics. Therefore it is necessary to understand the effect of each gain so that gains can be adjusted to achieve the intended performance metrics. The summary of the effects of PID gains on performance metrics is given below in Table 1 (Åström 2008; Li 2006). Although exceptions exist, relationships shown in Table 1 hold true for the vast majority of systems.

<table>
<thead>
<tr>
<th>Gain</th>
<th>Rise Time</th>
<th>Overshoot</th>
<th>Settling Time</th>
<th>Steady State Error</th>
<th>Stability</th>
</tr>
</thead>
<tbody>
<tr>
<td>$K_p$</td>
<td>Decrease</td>
<td>Increase</td>
<td>Small increase</td>
<td>Decrease</td>
<td>Degrade</td>
</tr>
<tr>
<td>$K_i$</td>
<td>Small Decrease</td>
<td>Increase</td>
<td>Increase</td>
<td>Large Decrease/Eliminate</td>
<td>Degrade</td>
</tr>
<tr>
<td>$K_d$</td>
<td>Small Decrease</td>
<td>Decrease</td>
<td>Decrease</td>
<td>Little effect</td>
<td>Improve</td>
</tr>
</tbody>
</table>

$K_d$ is generally increased as $K_p$ is increased because $K_d$ decreases overshoot, decreases settling time and increases system stability. Small increases in $K_d$ can improve all performance
metrics simultaneously. $K_d$ amplifies noise. The amplification of noise due to a large $K_d$ value can degrade system performance. $K_d$ can degrade stability when a transport delay exists in the system (Li 2006).

Integral control corrects steady-state error. Åström and Murray prove that any controller using integral action will have zero error when steady state is reached (Åström 2008). This proof holds true for non-linear processes and disturbances. However, if the integral gain is too high, the system will become unstable.

A large $K_i$ gain effectively attenuates disturbances (Åström 2008). Gain tuning methods that optimize disturbance rejection and robustness maximize the integral gain subject to robustness constraints (Åström 2004). Although maximizing $K_i$ produces the best disturbance rejection, a large $K_i$ gain is detrimental to tracking setpoint changes. The integral term causes the response to accelerate toward the setpoint. When a setpoint change occurs a large $K_i$ term would cause a massive acceleration toward the setpoint causing large overshoot resulting in instability or a large settling time.

3.6 Open-loop Tuning

Open-loop tuning assumes that the system can be approximated as a first-order system with time delay. The process reaction curve is used to approximate the system gain, time constant, and time delay. Open-loop tuning rules define PID gains that will result in a specified performance in terms of the system gain, time constant, and time delay.

3.6.1 Chien-Hrones-Reswick

The Chien-Hrones-Reswick (CHR) tuning provides tuning rules for 0% and 20% overshoot for both setpoint tracking and disturbance rejection. Tuning rules for disturbance
rejection are more aggressive than tuning rules for setpoint tracking. For disturbance rejection large proportional and integral gains are used to reject disturbances. The use of large proportional and integral gains during setpoint tracking causes large overshoot and could cause the system to go unstable.

3.6.2 Minimum ITAE

Open-loop tuning achieved by minimizing the integral of the time-weighted absolute error (ITAE) is known as Minimum ITAE. The ITEA criterion given by:

\[
ITAE = \int_{t_0}^{t^*} t|e(t)|dt
\]  

(3-2)

Where \(t^*\) is the current time, \(t_0\) is the initial time and \(e\) is error.

3.7 Successive Loop Closure

Successive loop closure, described by Beard and McLain, closes several simple feedback loops in succession around the open-loop plant dynamics instead of designing a single, more complicated controller (Beard 2012). The inner loop must have the highest bandwidth and each successive loop must have a bandwidth factor of at least 5 to 10 lower than the previous loop. If these conditions are met, the dynamics of the inner loop will have a minimal effect on the design of subsequent outer loops.
4 EXPERIMENTAL METHODS

This section describes the equipment, materials and procedures used to carry out the experimental work contained in this thesis. This section describes only general procedures such as clamping. Procedures specific to a particular weld or set of welds are included in their respective sections.

4.1 Equipment

The equipment used includes: the FSW machine, FSW tools and thermal management equipment.

4.1.1 FSW Machine

A TTI model RM-2 friction stir welding machine was used for all weld data presented. The PLC is programmed using Allen Bradley Logix 5000. The user interface is programmed using Allen Bradley RS View. The tool holder is liquid cooled.

The time required for the PLC to cycle through the program logic is referred to as the PLC time step. The FSW machine has a variable PLC time step that is approximately 0.01 seconds. Data is captured at a variable rate that is approximately 0.11 seconds.

The spindle motor is a Yaskawa model J-37FZ100E induction motor. The spindle driver is a Yaskawa model F7 adjustable frequency drive. The drive was re-wired to the PLC to enable torque control and allow the user to switch between spindle control and torque control modes. The driver provides spindle speed and torque data to the PLC. The torque command signal
received by the motor has a +/- 11 bit resolution and +/- 0.2% accuracy. The reported torque signal has a resolution of +/- 9 bit resolution and +/- 8% accuracy.

4.1.2 Tools

A convex scrolled shoulder step spiral (CS4) tool made of H-13 tool steel was used for welds in aluminum. The tool has a pin cone angle of 32.5 degrees, shoulder diameter of 25.30 mm and pin length of 5.08 mm (Nielsen 2009). Thermocouples are placed as shown in Figure 4-1. Material properties of H-13 tool steel include: \( \rho = 7861 \text{ kg/m}^3 \), \( c_p = 460 \text{ J/kg-K} \) and \( k = 27.7 \text{ W/m-K} \).

![Figure 4-1: Thermocouple placement for H-13 tool](image)

A CS4 PCBN tool was used for welds in steels. The tool has a pin cone angle of 30 degrees, shoulder diameter of 23.6 mm shoulder radius of 88.9 mm and the length from the tip of the pin to the bottom of the shoulder is 6.35 mm. The locking collar has a hole where a thermocouple is inserted such that it contacts the side of the PCBN insert. Material properties of PCBN include: \( \rho = 3130 \text{ kg/m}^3 \), \( c_p = 1996 \text{ J/kg-K} \) and \( k = 150 \text{ W/m-K} \).
4.1.3 Thermocouples

32 gauge type K thermocouples are used in this work. The thermocouples are ungrounded and stainless steel sheathed.

4.1.4 Backing Plate

ASTM A-36 hot rolled steel backing plates are used for all welds unless otherwise specified.

4.1.5 Workpieces

All aluminum welds use 6.35 mm (0.25 in) thick AA 7075-T735 unless otherwise specified. All aluminum plates were ground and cleaned with methanol on both the top and bottom. Material properties of AA7075-T735 include: ρ=2810 kg/m³, c_p=960 J/kg-C and k=155 W/m-K.

All steel welds were run in 6.35 mm (0.25 in) thick 1018 cold finished steel. The top and bottom of each plate was cleaned with methanol before use.

4.1.6 Clamping

A steel bar with a height of 25.4 mm (1.0 in) and width of 12.7 mm (0.5 in) is placed on both sides of the workpiece. Toe clamps apply force to the steel bars. Toe clamps are spaced 203 mm (8.0 in) or 305 mm (12.0 in) apart depending on the position of the workpiece relative to the anvil.
4.2 **Aluminum**

All welds run in aluminum have a feed rate of 150 mm/min (6.0 IPM) unless specified otherwise. All welds run in aluminum plunge 5.8 mm (0.23 in) into the material.

4.3 **Steel**

All welds run in steel have a feed rate of 150 mm/min (6 IPM) unless specified otherwise. All welds run in steel plunge 5.7 mm (0.23 in) into the material.
5 CONTROL OF SPINDLE ROTATION AXIS

The following sections present control theory for FSW that was developed as part of this thesis. The control theory presented is developed to investigate a dual loop control system for FSW where the inner loop maintains constant power and the outer loop adjusts power to maintain constant temperature.

5.1 Mayfield-Sorensen Control Model

A simplified component block diagram of the improved temperature control algorithm is shown in Figure 5-1 where $T$ is temperature, $\omega$ is spindle speed, $M$ is torque, and $P$ is power. The plant, Figure 5-2, is the combination of the spindle motor and the stir zone. For the outer loop the reference is desired temperature, the controlled variable is temperature and the manipulated variable is power. For the inner loop the reference is commanded power and the controlled variable is power.

Sorensen and Mayfield do not specify controllers for the inner and outer loop. They do specify that the inner loop adjusts spindle speed to maintain constant power. The following sections present development of controllers for the inner and outer loops.
The purpose of the inner loop is to maintain a desired power input to the stir zone. Two methods of power control are presented in this work. The first method, spindle speed control, adjusts spindle speed to control power. The second method, torque control, adjusts torque to control power.
5.2.1 Power Control by Adjusting Spindle Speed

The relationship between power and spindle speed is given by:

\[ P = \omega M \]  

(5-1)

where \( P \) is power, \( \omega \) is spindle speed in radians/s and \( M \) is torque. The conversion from spindle speed in radians/s to RPM is given by:

\[ RPM = \frac{60\omega}{2\pi} \]  

(5-2)

Power control by adjusting spindle speed uses torque feedback and Equation (5-1) to solve for the spindle speed required to produce the desired power. A block diagram for power control by adjusting spindle speed is shown in Figure 5-3 where \( M_{\text{filtered}} \) is the filtered value of the reported torque.

This control scheme assumes that torque is constant during each PLC time step. Slew limits define the maximum acceleration of the spindle. Slew limits are used to prevent the system from going unstable due to noise in torque feedback. If slew limits are set too high, the system will amplify noise in the torque feedback and become unstable.

![Figure 5-3: Spindle speed power control block diagram](image-url)
Various power control welds were run using spindle speed control to determine the appropriate slew rates. This was done by lowering the slew limits until constant power was not maintained. The lowest slew rate that maintained constant power was 0.83 RPM/s.

Results for power control by adjusting spindle speed with a slew rate of 0.83 RPM/s are shown in Figure 5-4. When power control is enabled, the torque is high and the RPM is low. As the weld progresses the plate heats and softens causing a decrease in torque. As torque decreases, the RPM increases maintaining constant power. Large spikes in power persist throughout the weld. Thesis power spikes occur because the spindle motor attempts to accelerate the spindle instantaneously to achieve the commanded RPM.

![RPM changes in response to torque maintaining commanded power](image)

- Torque decreases as material softens with increasing temperature
- Temperature increases until equilibrium is obtained
- Power spikes persist throughout weld
- Constant average power

Figure 5-4: Power control by adjusting spindle speed results. RPM, torque and temperature approach and maintain steady state in response to a commanded power.
The linear fit shows that the average power is held constant throughout the weld. The error between the linear fit of the reported power and power desired is 0.05%. Adjusting spindle speed to control power results in large power spikes throughout the weld. The average of these power spikes is the desired power value.

5.2.2 Power Control by Adjusting Torque

This control scheme uses the relationship between torque and power, Equation (5-1), to calculate the torque required to achieve the desired power. The spindle motor is a variable frequency AC induction motor. When running under torque control the motor driver commands and maintains a constant torque using flux vector control. The desired torque is obtained by controlling the current fed to the induction motor. This control method is shown in Figure 5-5.

![Figure 5-5: Power control by adjusting torque block diagram](image)

It is important to note that torque control without spindle speed feedback is unstable. A constant torque can only be maintained for a short time. The torque can only be controlled when the load supports the torque. Torque that is greater than the natural process torque leads to greatly decreasing loads causing an exponential increase in spindle speed. This is because when
both spindle speed and torque are increasing, the power increases and the material softens. Conversely, if the torque is lower than the natural process torque, the spindle speed will decrease exponentially as the material cools and hardens. Process variation causes the commanded torque to be either too low or too high to maintain equilibrium causing the spindle to rapidly decelerate until it stops or accelerate until machine safety limits are triggered.

Power control achieved by adjusting torque in response to changes in spindle speed is a stable process. Torque increases in response to decreasing spindle speed to maintain a constant power. Torque decreases in response to increasing spindle speed to maintain a constant power. Therefore power control achieved by adjusting torque in response to spindle speed is a stable process.

Results from a weld where torque is adjusted to maintain constant power is shown in Figure 5-6. As the workpiece is heated, the material softens. The torque and RPM signals are mirror images of each other. The spindle speed increases as the material softens. The torque decreases with increasing spindle speed to maintain the constant power. Figure 5-6 shows that power control achieved by torque control is a stable process.
5.2.3 Temperature and Power Response

A 1168.4 mm (46 in) long weld was run at 450 RPM in 6.35 mm (0.25 in) AA 7075 T-6. Temperature data was recorded to determine the variation in weld temperature during a constant-spindle speed weld. Welds were run at various spindle speeds to determine the steady-state torque and power values throughout the process window of the tool.

Figure 5-7 shows that constant spindle speed welds contain variation in power and temperature. The measured power contains power spikes that persist throughout the weld. The filtered power value varies throughout the weld in response to the changes in the process.
Figure 5-4 shows that power control achieved by adjusting spindle speed results in a constant average power. The actual power contains torque spikes throughout the weld. The temperature response shows that the tool gradually rises in temperature until thermal equilibrium is reached and the temperature is constant.

Figure 5-6 shows that power control achieved using torque control to maintain constant power produces a constant power with negligible variation. The temperature response is linear. This indicates that the tool is still heating and will reach a steady-state temperature.

A power control weld adjusting torque to keep power constant was run for 406.4 mm at 2.238 kW then 609.6 mm (24 in) at 2.536 kW in AA 7075. The temperature response is shown in Figure 5-8. Before the change in power is commanded, temperature is constant. After the change is power is commanded, the temperature increases logarithmically until a new steady-
state temperature is reached. These results show that changes in power lead changes in temperature.

![Figure 5-8: Power step from 2.238 kW to 2.536 kW temperature response](image.png)

5.2.4 Torque Resolution

When spindle speed is adjusted to control power, the commanded spindle speed is determined by the torque reported by the spindle controller. When torque is adjusted to control spindle speed, a commanded torque signal is sent to the spindle controller. The difference in resolution between reported and commanded torque affects the design of the power control loop. These effects are investigated in this section.

Torque measurements used in this work are reported by the spindle motor controller. The measurement is reported as a fraction of half the motor rated torque via a +/-10 Vdc +/-9 bit
signal. Therefore the resolution for torque measurement is 5.984 N-m and the resolution for power calculated using measured torque given by:

\[ PowerRes_{measured} = 6.270 \times 10^{-4} \times RPM \]  

(5-3)

where \( PowerRes_{measured} \) is the power resolution, in kW, for power calculated using the measured torque.

The commanded torque is sent as a fraction of the motor rated torque to the spindle controller via a +/-10 Vdc +/-11 bit signal. Therefore the resolution for commanded torque is 0.748 N-m and the resolution for power calculated using measured torque is given by:

\[ PowerRes_{commanded} = 7.837 \times 10^{-5} \times RPM \]  

(5-4)

where \( PowerRes_{commanded} \) is the resolution of power calculated using commanded torque.

The reported torque fails to capture torque dynamics. Figure 5-9 shows the torque reported by the controller and the torque calculated using load cells on the FSW machine in response to a sinusoidal torque command. The sinusoid is reported as a square wave due to limited resolution in reported torque.

The sinusoid can be seen in the torque calculated from forces measured by load cells. Torque calculated using load cells has poor signal quality due to cross talk. Because changes in torque during temperature and power control are small spindle torque will be assumed to be equal to commanded torque.
5.2.5 Torque Control vs. Spindle Speed Control

A dynamics analysis is performed to compare adjusting spindle speed to adjusting torque to maintain constant power. A free body diagram of the spindle motor and spindle is given in Figure 5-10 where $M_{mtr}$ is the motor torque, $M_{spn}$ is spindle torque, $D_{mtr}$ is the diameter of the motor pulley, $D_{spn}$ is the diameter of the spindle pulley, $M_b$ is torque lost due to bearings, $\omega_{spn}$ is the rotational velocity of the spindle, and $f$ is the mass moment of inertia of associated with the spindle. The gear ratio, $R$, is given by:

$$ R = \frac{D_{spn}}{D_{mtr}} $$

and has a value of 2.5.
The relationship between torque and spindle speed is derived using Figure 5-10 and is given by:

\[ R M_{mtr} = M_{spn} + M_b + J \dot{\omega}_{spn}. \]  

(5-6)

The effective motor torque, \( M_{mtr,eff} \), is given by:

\[ M_{mtr,eff} = R M_{mtr}. \]  

(5-7)

Reported torque in experimental data is \( M_{mtr,eff} \). Substituting Equation (5-7) into Equation (5-6) yields:

\[ M_{mtr,eff} = M_{spn} + M_b + J \dot{\omega}_{spn}. \]  

(5-8)
For the purposes comparing spindle speed control to torque control, it is assumed that $\omega_{spn}$ and $M_{spn}$ have a linear relationship as shown in Figure 5-11 and given by:

$$\dot{\omega}_{spn} = \frac{1}{B} M_{spn} \quad (5-9)$$

It is assumed that $J$, $M_{b}$ and $B$ are constant.

$M_{mtr,eff}$ has a derivative relationship with spindle speed as shown in Equation (5-8). A step change in desired spindle speed, $\omega_{spn}$, is modeled using the Heaviside step function. The derivative of the Heaviside step function is the Dirac Delta function. At the moment the step change in RPM occurs, $\dot{\omega}_{spn}$ is infinity. Therefore $M_{mtr,eff}$ is infinity when a step change in spindle speed occurs. Because the spindle motor’s maximum torque is finite, changes in commanded spindle speed result in large torque spikes, shown in Figure 5-14a, in attempt to instantaneously accelerate the spindle to the desired rotational velocity.

Spindle speed has an integral relationship with torque. Solving Equation (5-6) for spindle speed yields:
\[\omega_{spn}(t) = \frac{1}{J} \int_0^t \left[ M_{mtr,eff}(t) - \omega_{spn}(t) - M_b \right] dt \quad (5-10)\]

Equations (5-9) and (5-10) are used to develop an understanding how \(\omega_{spn}(t)\) responds to changes in \(M_{mtr,eff}(t)\). \(M_{spn}(t)\) is determined by the process. For thermo-mechanical processes \(M_{spn}(t)\) is continuous. Solving Equation (5-10) yields:

\[\omega_{spn}(t) = \frac{M_b - M_{mtr,eff}}{B} \left( 1 - e^{-\frac{Bt}{T}} \right) + \omega_0 e^{-\frac{Bt}{T}} \quad (5-11)\]

where \(\omega_0\) is \(\omega_{spn}(0)\). Therefore the spindle speed response to a step change in effective motor torque is exponential as shown in Figure 5-12b and Figure 5-13. The spindle speed response to changes in torque contains no discontinuities.

![Figure 5-12: (a) Effective motor torque response to step in spindle speed (b) Spindle speed response to step in effective motor torque](image)

Before a step change in \(M_{mtr,eff}\) is commanded, \(\omega_{spn}\) is constant therefore \(M_{spn}\) is equal to \(M_{mtr,eff} - M_b\) and the integrand of Equation (5-10) is zero. \(M_{mtr}\) is modeled using a
Heaviside step function whose integral is a ramp function. The integral of $M_{spn}$ is quadratic function until $M_{spn} - M_b$ is equal to $M_{mtr,eff}$. When this occurs the integrand is zero and $\omega_{spn}$ has reached its steady state value. This is shown graphically in Figure 5-12b and Figure 5-13.

**Figure 5-13: Graphical representation of Equation (5-10)**

Experimental results for step changes in spindle speed and torque validate that torque has a derivative relationship with spindle speed, and spindle speed has an integral relationship with torque. Figure 5-14a shows the effective motor torque response to a step change in RPM. At the instant the change in desired spindle speed is made, the motor attempts to instantaneously accelerate the spindle in order to achieve the desired spindle speed. This results in a large spike in motor torque.

Figure 5-14b shows the spindle speed response to a step increase in power where power control was obtained by adjusting torque. When the change in power is commanded, the torque instantly increases slightly to obtain the desired power value. Due to the increased power the material begins to soften causing the spindle speed to increase and the motor torque to decrease. The spindle speed response to torque contains no discontinuities.
Experimental data shown in Figure 5-14 indicate that higher spindle speeds correspond to lower torques and lower spindle speeds correspond to higher torques. These results suggest that the slope shown in Figure 5-11 should be negative. The relationship between torque and spindle speed will be addressed in more detail in later sections.

The control methods presented assume $M_{mtr, eff}$ is approximately equal to $M_{spn}$. Torque has derivative relationship with spindle speed as shown in Figure 5-12a. When a change is RPM is commanded, the spindle motor attempts to instantaneously accelerate the spindle to a new RPM causing a spike in motor torque. Equation (5-12) shows that a near instantaneous acceleration of the spindle motor would cause a large difference between motor torque and spindle torque. The motor torque is not approximately equal to spindle torque when a change in RPM is commanded.
Experimental data shows that when torque is adjusted to keep power constant, the difference between motor torque and spindle torque is much smaller than when spindle speed is used to keep power constant. The relationship between motor torque and spindle torque is obtained by rearranging Equation (5-12) and is given by:

\[ M_{mtr\_eff} - M_{spn} = J\dot{\omega}_{spn} + M_b \]  

(5-12)

Welds were run in air at various acceleration and deceleration rates in order to determine \( J \). For welds run in air, \( M_{spn} \) is zero and Equation (5-12) simplifies to:

\[ M_{mtr\_eff} = (J\dot{\omega}_{spn} + M_b) \]  

(5-13)

Air welds were run accelerating and decelerating at each of the following rates: 12.2 rad/s\(^2\), 8.6 rad/s\(^2\), 6.1 rad/s\(^2\), 2.4 rad/s\(^2\) and 1.2 rad/s\(^2\). Torque data were averaged from a spindle speed of 12.0 rad/s\(^2\) to 20.0 rad/s\(^2\) for acceleration torque and 20.0 rad/s\(^2\) to 12.0 rad/s\(^2\) for deceleration torque.

Acceleration and deceleration torques are given by:

\[ M_{acc} = (M_b + J\dot{\omega}_{spn}) \]  

(5-14)

\[ M_{dec} = (M_b - J\dot{\omega}_{spn}) \]  

(5-15)

Where \( M_{acc} \) is the effective acceleration motor torque, \( M_{dec} \) is effective deceleration motor torque. Subtracting Equation (5-15) from (5-14) yields:

\[ M_{acc} - M_{dec} = (2J\dot{\omega}_{spn}) \]  

(5-16)

Equation (5-16) is rearranged yielding:

\[ (M_{acc} - M_{dec})/2 = J\dot{\omega}_{spn} \]  

(5-17)
For each rate, \((M_{acc} - M_{dec})/2\) is plotted against \(\dot{\omega}\). The resulting slope is the mass moment of inertia associated with the spindle and is shown in Figure 5-15. The mass moment of inertia is equal to 5.45 kg·m².

Assuming \(M_b\) is negligible, Equation (5-12) is rearranged yielding:

\[
M_{mtr\_eff} - M_{spn} = J\dot{\omega}_{spn}
\]

which is the error in the assumption that \(M_{spn}\) equals \(M_{mtr\_eff}\). The sample rate of the data acquisition system is approximately one tenth of a second limiting the accuracy of acceleration calculation during the torque spike when changes in spindle speed are commanded.

When a step change in power is commanded, Figure 5-14a, the torque error during the spindle spike is 203.0 Nm (149.7ft-lb) which corresponds to a 406% error in power at the high power level and a 565% error at the low power level. For power control by adjusting torque,
Figure 5-14b, the error associated with the spindle acceleration after the change in power, where acceleration is greatest, is 7.4 Nm (5.46ft-lb) which corresponds to 10% error in power.

Adjusting spindle speed to keep power constant is undesirable because reported torque is a poor control signal. Torque spikes cause the difference between motor and spindle torque to always be at least 400%. The reported torque signal has low resolution, adding to the error.

The main advantage of using torque to control power is the avoidance of artificial torque spikes caused by attempting to change the RPM instantaneously. Using torque control to control power results in a smooth power signal with low uncertainty. The difference between the motor torque and actual spindle torque is proportional to the acceleration of the spindle. Low uncertainty exists because under torque control, power and spindle speed changes slowly. Power control by adjusting torque provides a more accurate torque measurement due to the resolution difference between the commanded and reported torque signals. Power control is achieved by adjusting torque for the remainder of the welds presented in this thesis.

5.3 **Outer Loop**

The outer loop uses PID control to adjust power to maintain a commanded temperature. The implementation of PID control in the outer loop is shown in Figure 5-16. $P_{\text{init}}$ is a constant power value. The PID controller provides a change in power that is added to the $P_{\text{init}}$ term. The $P_{\text{init}}$ term is the spindle power at the moment the controller switches from commanding a constant spindle speed to temperature control. Once temperature control is engaged $P_{\text{init}}$ is constant until temperature control is disengaged.
Figure 5-16: Inner and outer loop block diagram
In order to control a system, an understanding of the system dynamics must be developed. This section provides a background of heat transfer modeling techniques and system dynamics. Relevant theory is presented in the following order: the heat diffusion equation, one-dimensional analysis, lumped analysis, first-order systems, second-order systems and time delay.

### 6.1 Heat Diffusion Equation

It has been observed that temperature response varies with thermocouple location. It is desirable to model this phenomenon to better understand it. This phenomenon will be modeled using the heat diffusion equation:

\[ T_t = \alpha \nabla^2 T \]  

where \( \nabla^2 \) is the Laplace operator, \( T \) is temperature, \( t \) is time and \( \alpha \) is thermal diffusivity. We believe the temperature field in the tool to be axisymmetric and that the gradient in the radial direction is small. A Biot number is calculated to show that one-dimensional analysis can be used. It is desirable to use the heat diffusion equation to derive a transfer function that describes the temperature response of the tool due to changes in weld power.

#### 6.1.1 Validation of 1-D Analysis

1-d heat transfer analysis is desirable because it is simple and in many cases accurately captures the relevant physics. The Biot number is calculated to determine the validity of 1-d
analysis. If a system has a Biot number less than 0.1, 1-d analysis is considered valid. The Biot number for a cylinder is given by:

\[
B_t = \frac{h r}{2 k} \tag{6-2}
\]

where \( h \) is the heat transfer coefficient, \( r \) is the radius and \( k \) is the thermal conductivity.

The Biot numbers calculated use extreme values to obtain an artificially high Biot number. The resulting Biot numbers are much less than 0.1 for tools used in both aluminum and steel. Therefore the error associated with neglecting heat transfer in the radial direction is small for the thermal analysis of FSW tools used in this study.

The Biot number was calculated using Equation (6-2) where \( h \) is calculated using an approximate solution for heat transfer coefficients of rotating cylinders (Kendoush 1996). A spindle speed of 2000 RPM, nearly double the maximum RPM for either tool, is used for calculating \( h \). For the H-13 tool, the Biot number is 0.0086. Because the Biot number is much less than 0.1 with a spindle speed double the maximum spindle speed, uniform temperature in the radial direction is a reasonable assumption and the error associated with neglecting heat transfer in the radial direction very small.

The Biot number for the PCBN is calculated assuming the entire tool is made of PCBN. This should be a conservative approximation because the locking collar has a much lower thermal conductivity than PCBN and therefore decreases radial heat transfer. The Biot number calculated for the PCBN tool is 0.0014 which is much less than 0.1. Therefore the error associated with neglecting heat transfer in the radial direction is very small.
6.1.2 Characteristics of Transient Conduction

1-d transient conduction with constant boundary conditions is characterized by a time delay, a temperature rise with increasing slope, a temperature rise with decreasing slope and a steady state temperature. These characteristics can be approximated using exponential functions. Depending on the boundary conditions, the temperature response of a system may show some or all of these characteristics.

The error function contains all characteristics of 1-d transient conduction in the proper order as shown in Figure 6-1. Exponential functions and error functions can be combined to modify the shape of the error function to properly model a given system. Therefore, the solution to the heat diffusion equation should be a function of exponential functions or a function of error functions and exponential functions.

![Figure 6-1: Error function](image)

Closed form transient solutions of the 1-d heat diffusion equation exist for various infinite and semi-infinite boundary conditions. These equations are solved using similarity
transforms and are functions of error functions and exponential functions. At present, closed form solutions to the heat equation for finite boundary conditions do not exist in the time domain. However various series solution techniques exist to solve the heat equation for transient conduction with finite boundary conditions. It is desirable to obtain a closed form solution of the heat diffusion equation in the Laplace domain that can be used as a transfer function.

6.1.3 Analytical Solutions

Solutions to the heat diffusion equation for heat transfer in the tool are presented. Closed form solutions for relevant boundary conditions exist in the Laplace domain. These solutions cannot be used in classical control theory because they cannot be expressed as rational functions in the Laplace domain. Only models that have a semi-infinite boundary condition have a closed form solution in the time domain.

Heat transfer in the tool is diagramed in Figure 6-2 where \( T_s \) is the ambient temperature, \( A_c \) is the cross section area, \( L_{\text{eff}} \) is the effective tool length, \( \alpha \) is thermal diffusivity and \( z \) is the distance from the workpiece to the thermocouple. The boundary conditions at the tool-workpiece interface and at tool-tool holder interface are represented by BC 1 and BC 2 respectively.

The heat diffusion equation for one dimensional heat transfer in a cylinder is given by:

\[
\frac{\partial^2 \theta(z,t)}{\partial z^2} - \frac{1}{\alpha} \frac{\partial \theta(z,t)}{\partial t} = 0
\]  

(6-3)

where, \( t \) is time, \( \partial \) is thermal diffusivity, \( z \) is distance from the tool-workpiece interface and \( \theta \) is given by:

\[
\theta = T(z) - T_s
\]  

(6-4)
For the first set of boundary conditions BC 1 is a specified flux, $P_{\text{in}}$, and BC 2 is a constant Temperature, $T_s$ or $\theta(L) = 0$. The frequency domain solution to the second set of boundary conditions is given by:

$$
\theta(z, s) = \frac{\sqrt{s} \alpha}{\sqrt{s} k \left( 1 + \frac{2\sqrt{sL}}{\sqrt{\alpha}} \right)} \left( e^{\frac{z\sqrt{s}}{\sqrt{\alpha}}} - e^{\frac{\sqrt{s}(2L-z)}{\sqrt{\alpha}}} \right)
$$

For the second set of boundary conditions BC 1 is a specified temperature, $\theta_{\text{in}}$, and BC 2 is a constant temperature, $T_s$ or $\theta_L = 0$. The frequency domain solution to the final set of boundary conditions is given by:

$$
\frac{\theta(z, s)}{\theta_{\text{in}}(s)} = \frac{z\sqrt{s}}{e^{\frac{z\sqrt{s}}{\sqrt{\alpha}}} - e^{-\frac{\sqrt{sL}}{\sqrt{\alpha}}}}
$$
For the final set of boundary conditions BC 1 is a specified flux, \( \text{Power}_{\text{in}} \), and BC 2 is a constant flux, \( \text{Power}_{\text{out}} \). The frequency domain solution to the final set of boundary conditions is given by:

\[
\theta(z, s) = \frac{\left( e^{\frac{(z+L)s}{\sqrt{\alpha}t}} + e^{\frac{(-z+2L+1)s}{\sqrt{\alpha}t}} \right) \sqrt{\alpha} \sqrt{s} k + \left( -e^{\frac{(z+1)s}{\sqrt{\alpha}t}} + e^{\frac{(-z+2L+1)s}{\sqrt{\alpha}t}} \right) \alpha h}{kh \sqrt{\alpha} \sqrt{s} \left( e^{\frac{(2L+1)s}{\sqrt{\alpha}t}} + e^{\sqrt{\alpha}} \right) + k^2 s \left( e^{\frac{(L+2)s}{\sqrt{\alpha}t}} - e^{\sqrt{\alpha}} \right)}
\]

Equations (6-5)-(6-7) cannot be expressed as rational expressions due to the \( \sqrt{s} \) term in the exponentials. Padé approximations are unable to convert these functions into rational expressions due to the \( \sqrt{s} \) term in the exponentials. Equations (6-5)-(6-7) do not have closed-form solutions in the time domain.

A closed-form solution to the heat equation exists in the time domain exists when the control volume is a semi-infinite medium (Incropera 2007). These solutions can be expressed as a rational expression in the frequency domain using the Padé approximation. The semi-infinite medium model has undesirable characteristics. For a BC 1 of specified temperature, the temperature at all locations will approach that temperature as time increases. For BC 1 of specified flux, the temperature will continually increase with time, never reaching a steady state. Solutions for BC1 of specified temperature and specified power are given by Equations (6-8) and (6-9) respectively,

\[
\theta(z, t) = erf \left( \frac{z}{2\sqrt{\alpha t}} \right) (\theta_{\text{in}})
\]

\[
\theta(z, t) = \frac{2q''_{\text{in}}(\alpha t/\pi)^{1/2}}{k} e^{\left( \frac{z^2}{\alpha t} \right)} - \frac{q''_{\text{in}}z}{k} erf c \left( \frac{z}{2\sqrt{\alpha t}} \right)
\]
where \( q_{in}'' \) is the specified flux at BC 1 and \( \theta_{in} \) is given by:

\[
\theta_{in} = T_i - T_s
\]  

(6-10)

where \( T_i \) is the initial temperature of the control volume and \( T_s \) is the constant surface temperature at BC1.

### 6.1.4 Numerical Solutions

Using numerical methods the heat diffusion equation was solved to explore the effect of thermocouple location on the system dynamics. The 1-d heat diffusion equation and boundary conditions used is given by:

\[
\frac{\partial^2 \theta(z,t)}{\partial z^2} - \frac{1}{\alpha_{H13}} \frac{\partial \theta(z,t)}{\partial t} = 0
\]  

(6-11)

\[
\theta(z,t) = T(z,t) - T_s
\]  

(6-12)

where \( \alpha_{H13} \) is \( 7.66 \times 10^{-6} \text{ m}^2/\text{s} \) and \( T_s \) is 20°C. Boundary conditions are given by:

\[
\theta(0,t) = 500^\circ\text{C}
\]  

(6-13)

\[
\theta(L_{eff},t) = T_s
\]  

(6-14)

where \( L_{eff} \) is 0.025m. Initial conditions are given by:

\[
\theta(z,0) = 420 - 16000z
\]  

(6-15)

Equations (6-11)-(6-15) represent an H-13 tool, as shown in Figure 6-2, that is initially at steady state then exposed to a step increase in stir zone temperature, BC1. The initial condition, Equation (6-15) is the steady-state temperature for the tool where BC1 is 420°C and BC2 is 20°C.

Equations (6-11)-(6-15) are solved using the \textit{pdepe} function in MATLAB\textsuperscript{TM}. The only variables are time, \( t \), and thermocouple location, \( z \). Time extends from 0 to 70 seconds and
spanned by 1,000 nodes. Thermocouple location extends from 0 to 0.025m and is spanned by 1,000 nodes.

The temperature response at different locations will be compared using the temperature rise \( \theta_{\text{rise}} \) given by:

\[
\theta_{\text{rise}}(z, t) = \theta(z, t) - 420 + 16000z
\]  

(6-16)

Results are shown in below in Figure 6-3 and Figure 6-4.

Figure 6-3 and Figure 6-4 show that the temperature response of the tool is dominated by thermocouple location. When the thermocouple is located close to the tool-workpiece interface, time delay and time constants are small and only first-order dynamics are visible. As the thermocouple is located further from the tool-workpiece interface, time delay and time constant increase. Dynamics become increasingly second-order, immediately after the time delay, as distance increases.

![Figure 6-3: Simulated temperature response at various thermocouple locations using the heat diffusion equation with a linear initial temperature profile](image)
System identification was performed for all thermocouple locations shown in Figure 6-3 and Figure 6-4 using the System Identification Toolbox™ in MATLAB. For all thermocouple locations shown, first-order models with time delay had a fits above 96% and second-order models with time delay had fits greater than 97%. In each case the second-order fit was slightly higher. Because all the fits are above 95%, the heat diffusion equation suggest that tool dynamics can be modeled using a first-order system with time delay.

6.2 **Lumped Analysis**

The lumped capacitance method is the simplest method to model transient heat conduction. This method assumes that temperature is spatially uniform within the control volume. A thermal balance is performed on the control volume and results in a first-order transfer function.
6.2.1 First-order Systems

The transfer function for a first-order system is given by:

\[
\frac{Output(s)}{Input(s)} = \frac{K}{\tau s + 1}
\]  

(6-17)

where \( K \) is the system gain and \( \tau \) is the system time constant. First-order transfer functions do not capture the time delay and exponential growth that are typical of 1-d transient conduction. The step response of a first-order system is shown in Figure 6-5. It contains a temperature rise with decreasing slope followed by a steady state temperature. First-order models do not capture time delay or temperature rise with increasing slope.

![Figure 6-5: Step response for first-order transfer function 1/(s+1)](image-url)
6.2.2 Second-order Systems

The convolution of two first-order systems results in a second-order system. If both the stir zone and the tool are modeled separately using lumped capacitance, the resulting dynamics of the system containing the stir zone and the tool is second-order. The transfer function of an overdamped second-order system is given by:

\[
\frac{Output(s)}{Input(s)} = \frac{K}{(\tau_1 s + 1)(\tau_2 s + 1)}
\]  \hspace{1cm} (6-18)

where \(\tau_1\) and \(\tau_2\) are the system time constants and \(K\) is the system gain.

A second-order transfer function captures the temperature rise with increasing slope, the temperature rise with decreasing slope and the steady state temperature as shown in Figure 6-6. The second-order transfer function does not capture time delay. The initial temperature rise with increasing slope is due to the fact that multiplication in the frequency domain is convolution in the time domain. An overdamped second-order system is the convolution of two first-order systems. Therefore the second order model given by Equation (6-18) will always be overdamped.

If the time constant of one of the two first-order systems is greater than the other, it will dominate the response. If a time constant is much smaller than the other, it may have negligible effect on the transient response of the system. In such cases the system is approximated by the dominant root. This is called dominant root approximation (Palm 2005).
6.2.3 Time Delay Approximation

Lumped capacitance analysis does not account for time delay. Therefore time delay will be added to the lumped capacitance model by adding the transfer function of a lumped capacitance model to the transfer function of pure time delay. The transfer function for pure time delay is given by:

\[ F(s) = e^{s t_d} \quad (6-19) \]

where \( t_d \) the time delay in seconds. The value of the time delay can be approximated using closed form solutions to the heat diffusion equation with semi-infinite media boundary conditions. Time delay can also be approximated by numerical solution of the heat diffusion equation with any boundary condition. When approximated time delay is added to a second-order system, all of the features of 1-d conduction are accounted for, as shown in Figure 6-7.
6.2.4 First-order Model with Time Delay

The simplest model uses lumped volume in the tool where the boundary condition at the tool-workpiece interface is power into the tool and the boundary condition at the tool-tool holder interface is constant temperature as shown in Figure 6-8. The thermocouple is placed at the center of the lump, therefore $L_{\text{lump}}$ is twice the height of the thermocouple, $L_{\text{TC}}$. $P_{\text{tool}}$ is the amount of power entering the tool and $T_{\text{lump}}$ is the resulting tool temperature at the location of the thermocouple. Previous research (Chao 2003; Covington 2005; Ross 2009) indicates 2.8% to 5.1% of the mechanical power input into the stir zone is transferred to the tool as heat.
Solving this simple models yields:

\[
\frac{\Theta(s)}{P_{tool}(s)} = \frac{L_{\text{eff}}}{k} \left( \frac{\rho V c_p L_{\text{eff}}}{A_c k} s + 1 \right) \tag{6-20}
\]

Time delay is added by multiplying Equation (6-20) by Equation (6-19). The first-order with time delay model is given by:

\[
\frac{\Theta(s)}{P_{tool}(s)} = \frac{L_{\text{eff}} e^{td}}{k} \left( \frac{\rho V c_p L_{\text{eff}}}{A_c k} s + 1 \right) \tag{6-21}
\]

If the pin of the tool is neglected, we can assume a constant cross sectional area and simplify Equation (6-21) to obtain:
6.2.5 Second-order with Time Delay

The system is modeled using two lumped volumes. The first represents the stir zone and the second represents the tool. This models assumes that all heat is generated in the stir zone and heat is transferred to the tool via conduction. When the stir zone experiences a step increase of power, the temperature of the stir zone increases. When the rate of temperature change of the stir zone increases, the heat transfer to the tool increases. When the rate of temperature change in the stir zone decreases, the heat transfer to the tool decreases until a steady state is reached. Heat flow from the tool to the stir zone would be in violation of the second law of thermodynamics. Therefore the system is overdamped. Overdamped systems are characterized by a damping ratio greater than 1.

The stir zone is modeled with a boundary condition of convective heat loss to a backing plate at constant temperature and power input boundary condition as shown in Figure 6-9, where

\[
\frac{\theta(s)}{P_{tool}''(s)} = \frac{\frac{L_{eff}}{k} e^{td}}{\left(\frac{\rho L_{lump} c_p L_{eff}}{k} + 1\right)}
\]  

\(6-22\)

Figure 6-9: Stir zone lumped model
$P_{sz}$ is power input into the stir zone, $L_{sz}$ is the height of the stir zone, $A_{adv}$ is the cross sectional area of the stir zone in the Y-Z plane, $T_{sz}$ is the temperature of the stir zone, $v_x$ is the feedrate and $h_{plate}$ is the heat transfer coefficient for heat loss to the backing plate. The use of a convective heat transfer to model heat loss to the backing plate is common practice in friction stir welding (Lohwasser 2009; Soundararajan 2005). Due to the forces applied and plasticity of the material in the stir zone, it is assumed that contact resistance between the workpiece and backing plate directly beneath the tool is minimal. A convective heat transfer coefficient of 3,000 W/m2 is assumed (Lohwasser 2009; Soundararajan 2005). The solution to the stir zone lumped model is given by:

$$\frac{\theta_{sz}(s)}{P(s)} = \frac{1}{\left( A_c h_{plate} + A_{adv} \rho v_x c_p \right)} - \frac{\rho V_{sz} c_p s}{\left( A_c h_{plate} + A_{adv} \rho v_x c_p \right)} + 1 \quad (6-23)$$

The tool is modeled using conduction heat transfer for both boundary conditions where the tool-workpiece interface is at the temperature of the stir zone, $T_{sz}$. The temperature at the tool-tool holder, $T_s$, is constant as shown in Figure 6-10 where $T_{tc}$ is the tool temperature.
Neglecting the pin, the cross sectional area can be assumed constant and the solution can be simplified. The solution and simplified solution is given by:

\[
\frac{\theta_{tc}}{\theta_{sz}} = \frac{\frac{h_{tool}}{h_{tool} + \frac{k}{L_{eff}}}}{\frac{h_{tool}}{h_{tool} + \frac{k}{L_{eff}}} + 1}
\]

\( (6-24) \)

\[
= \frac{\frac{h_{tool}}{h_{tool} + \frac{k}{L_{eff}}}}{\frac{\rho L_{lump} c_p s}{k} + 1}
\]

\( (6-25) \)

The second-order model with time delay is obtained by multiplying the two first-order models, Equations (6-23) and (6-24), by the transfer function for time delay, Equation (6-19). The second-order model is given by:
6.2.6 Comparison of Stir Zone to Tool Time Constant

This section shows that the dynamics of the stir zone are dominated by the dynamics of the tool, therefore the system dynamics are the tool dynamics. Methods of directly measuring the temperature response of the stir zone do not exist. A lumped analysis of the stir zone will be used to calculate the largest reasonable time constant of the stir zone. It will be shown that the largest reasonable stir zone time constant is orders of magnitude smaller that the experimental time constant of the system. It will be shown that the largest reasonable stir zone time constant is an order of magnitude smaller than the smallest feasible time constant of the tool.

The time constants of the stir zone and the tool will be investigated using lumped capacitance analysis. Equation (6-23) is the first-order transfer function obtained from the lumped capacitance analysis of the stir zone and Equation (6-24) is the first-order transfer function obtained from the lumped capacitance analysis of the tool. The time constant for both systems is given by:

\[
\tau = \frac{Mass * c_p}{Q_{out}} \tag{6-27}
\]

where \(Q_{out}\) is the amount of heat transferred through the lumped model. Because the most of the heat generated passes through the stir zone and only a fraction passes through the tool, it is reasonable to expect the time constant of the stir zone to be much smaller that that of the tool.
It is assumed that 5% of the energy applied to the stir zone is transferred through the tool and 95% is transferred through the stir zone (Lohwasser 2009). It is assumed that 85% is transferred through the backing plate and 10% through advection (Lohwasser 2009). The time constants of the stir zone and the tool can be defined by:

\[ \tau_{sz} = \frac{\rho_{sz} V_{sz} C_{p,sz}}{0.95 P_{sz}} \]  
\[ \tau_{tool} = \frac{\rho_{tool} * V_{tool} * C_{p,tool}}{0.05 P_{sz}} \]  

(6-28)  
(6-29)

Figure 5-8 shows that for a weld run in AA-7075 with an H-13 tool at a constant power of 2.238 kW, the steady state temperature is approximately 425°C before the step in power is commanded. The power is increased to 2.536 kW and the steady state temperature increases to 492°C. For the following analysis 2.238 kW will be used for \( P_{sz} \), 425°C for \( T_{sz} \) and 2.54 mm/s for \( v_x \) in this analysis. \( \Delta T \) is 405°C and is defined as the difference between \( T_{sz} \) and the temperature of the surroundings, 20°C.

To obtain the largest reasonable time constant for the stir zone, the volume of the stir zone is assumed to be cross sectional area of the tool multiplied by the height of the workpiece. The resulting time constant using Equation (6-28) is 0.0038 s. The measured time constant of TC1, shown in Figure 5-8, is 25.99 s. The measured time constant of the system is four orders of magnitude larger than the largest reasonable estimate for the time constant of the stir zone.

Previous research indicates approximately 1% of the heat input into the tool enters through the pin when cylindrical pins are used (Covington 2005). 1% is a reasonable estimate for the amount of heat entering the lumped volume defined. Because only 5% of the power
applied is transferred to the tool, the heat transfer through the lump will be 0.05% of the total power. Therefore the time constant for a thermocouple in the pin is approximated by:

$$\tau_{tool} = \frac{\rho_{tool} \cdot V_{tool} \cdot c_{p,tool}}{0.0005P_{sz}}$$  \hspace{1cm} (6-30)$$

The lumped volume required to obtain the experimental time constant, 25.99 s, is $8.55e^{-6}$ m$^3$, nearly two orders of magnitude greater than the volume of the pin. The experimental time constant was obtained using a thermocouple near the tip of the pin. The volume of material between the thermocouple and the tool/workpieces interface is negligible compared to the volume of the lumped mass. This indicates that the material above the thermocouple dominates the dynamics of the temperature response of thermocouples located near the tip of the pin.

A lumped analysis of the tool shows the smallest reasonable time constant calculated for the tool is much larger than the largest reasonable value calculated for the stir zone. TC1 is placed 1.27 mm (0.05 in) from the tip of the pin. The pin is used as the control volume and is $1.131e^{-7}$ m$^3$. Using Equation (6-30), the calculated time constant is 0.344 s, two orders of magnitude larger than largest reasonable time constant calculated for the stir zone.

Because the fraction of the heat input to the stir zone lost to the backing plate and to advection is known, a more accurate approximation of the volume of the stir zone can be made. The volume of the stir zone is approximated as a cylinder. The cross sectional area, area of advection and volume of the stir zone given by:

$$A_C = \frac{0.85P_{sz}}{h_{plate}\Delta T}$$  \hspace{1cm} (6-31)$$

$$A_{adv} = \frac{0.10P_{sz}}{\rho_{sz}v_xc_{p, sz}\Delta T}$$  \hspace{1cm} (6-32)$$

$$V_{sz} = A_C A_{adv}/D_{sz}$$  \hspace{1cm} (6-33)$$
where $D_{sz}$, 25.4mm, is the diameter of the stir zone. Substituting (6-33) into (6-29) yields a time constant of $6.7 \times 10^{-4}$ s. Because this approximation takes into account the relative amount of heat lost through the backing plate and advection, it is believed to be more accurate and also indicates that the time constant of the stir zone is orders of magnitude smaller than that of the tool.

The time constants calculated for the tool are much greater than the time constant calculated for the stir zone. Therefore temperature transients seen by the thermocouple will be dominated by temperature transients in the tool. Therefore transient response of the system is the transient response of the tool.
7 SYSTEM IDENTIFICATION

This section investigates the thermal response to step changes in power. First and second-order models are fit to experimental data and the validity of LTI assumptions are addressed. For the purposes of this discussion, the term non-linear will describe systems that cannot be approximated by first or second-order models, or whose response varies with step magnitude, direction or starting power.

7.1 Aluminum

This section investigates the thermal response of the system to step changes in power for welds run in aluminum. It is determined that a first-order model with time delay is an appropriate model. This section provides insight into the validity of LTI approximations and provides a model that can be used to estimate controller gains.

7.1.1 Methods

The temperature response to step changes in power is used for system identification. First the time delay is approximated using the graphical techniques shown in Figure 7-1.

The System Identification Toolbox™ in MATLAB is used for system identification. If the system is linear, first and second-order models are fit to the data using the time delay values found via graphical methods shown in Figure 7-1. The goodness of fit is calculated as the percentage of output variation that can be explained by the model. The goodness of fit will be referred to as the “fit” of the model and is used to determine the validity of the model.
A variety of power step welds are run to determine if the system dynamics are constant. The parameters for welds run with step changes in power are given in Table 2. The step changes in power are varied by magnitude, direction and starting power. Therefore any nonlinearity should be detected.

![Graphical methods for estimating time delay for 1st and 2nd order approximations for y=erf(x)](image)

**Figure 7-1: Graphical methods for estimating time delay for 1st and 2nd order approximations for y=erf(x)**

Table 2: Welds run with step changes in power

<table>
<thead>
<tr>
<th>Weld</th>
<th>Power 1 (kW)</th>
<th>Length 1 (mm)</th>
<th>Power 2 (kW)</th>
<th>Length 2 (mm)</th>
<th>Power 3 (kW)</th>
<th>Length 3 (mm)</th>
<th>Power 4 (kW)</th>
<th>Length 4 (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>2.24 (3.00 HP)</td>
<td>404 (16.0 in)</td>
<td>2.54 (3.40 HP)</td>
<td>600 (24.0 in)</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>2.54 (3.40 HP)</td>
<td>559 (22.0 in)</td>
<td>2.24 (3.00 HP)</td>
<td>559 (22.0 in)</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>2.09 (2.80 HP)</td>
<td>292 (11.5 in)</td>
<td>2.24 (3.00 HP)</td>
<td>292 (11.5 in)</td>
<td>2.39 (3.20 HP)</td>
<td>292 (11.5 in)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>2.54 (3.40 HP)</td>
<td>292 (11.5 in)</td>
<td>2.39 (3.20 HP)</td>
<td>292 (11.5 in)</td>
<td>2.24 (3.00 HP)</td>
<td>292 (11.5 in)</td>
<td>2.09 (2.80 HP)</td>
<td>292 (11.5 in)</td>
</tr>
</tbody>
</table>

**7.1.2 Results**

Results for a power step from 2.24 kW (3.00 HP) to 2.54 kW (3.40 HP) are shown in Figure 7-2. This figure shows that as distance from the tool-workpiece interface to the
thermocouple increases, the time constant increases. Steady state temperature increases with thermocouple distance. The time constants for thermocouple locations 1, 2 and 3 are 25.9 s, 38.5 s and 58.3 s respectively. The steady-state temperature for thermocouple locations 1, 2 and 3 are 493°C, 467°C and 438°C respectively. This suggests that the time constant and steady state temperature are dominated by the thermocouple location.

![Figure 7-2: Power step from 2.24 kW (3.00 HP) to 2.54 kW (3.40 HP) temperature response](image)

The temperature response is used to calculate the time delay at each thermocouple location. The time delays for thermocouple location 1, 2 and 3 are 0.80, 1.26 and 2.06 seconds respectively. The plot used to calculate the time delay is shown below in Figure 7-3. This figure indicates time delay is dominated by thermocouple location.
Figure 7-3: Time delay in H-13 tool

A graph showing the first and second-order approximations and response data is shown in Figure 7-4. The System Identification Toolbox™ in MATLAB is used to obtain first and second-order approximations of the temperature response to changes in power. The goodness of fit is calculated as the percentage of output variation that can be explained by the model. Both first and second-order models had fits of 95.72%. Therefore the response can be classified as first-order and the model is given by Equation (7-1).
Figure 7-4: 1st and 2nd order fits of temperature response to step change in power from 2.24 kW (3.00 HP) to 2.54 kW (3.40 HP)

\[ G(s) = \frac{222.32}{25.993s + 1} e^{-0.8s} \]  

(7-1)

To check for non-linearity, power steps were run from 2.09 kW (2.80 HP) to 2.24 kW (3.00 HP) to 2.39 kW (3.20 HP) to 2.54 kW (3.40 HP). The temperature response is shown in Figure 7-5. First-order models for the step response from 2.09 kW (2.80 HP) to 2.24 kW (3.00 HP) and 2.24 kW (3.00 HP) to 2.39 kW (3.20 HP) are given by Equations (7-2) and (7-3) respectively. The step response from 2.39 kW (3.20 HP) to 2.54 kW (3.40 HP) is significantly different from the previous temperature responses. When the temperature is approximately 470°C, an inflection point exists. This response cannot be modeled by a first or second-order system.
The step responses from 2.09 kW (2.80 HP) to 2.24 kW (3.00 HP) and 2.24 kW (3.00 HP) to 2.39 kW (3.20 HP) are approximately linear because their transfer functions are almost the same. Observing the shape of the step response from 2.39 kW (3.20 HP) to 2.54 kW (3.40 HP) it can be seen that it has a significantly higher gain and significantly different shape than the other two responses. Therefore the system is nonlinear in the region from 2.39 kW (3.20 HP) to 2.54 kW (3.40 HP).

A large step change in power from 2.54 kW (3.40 HP) to 2.24 kW (3.00 HP) was run to determine if the system responded differently between step increases and step decreases in power.

\[
G(s) = \frac{174}{15s + 1}e^{-0.8s} \quad (7-2)
\]

\[
G(s) = \frac{171.79}{11.07s + 1}e^{-0.8s} \quad (7-3)
\]
power. The temperature response is shown in Figure 7-6. The temperature response is non-linear. Neither a first or second-order model can be fit to this temperature response. For first and second-order systems that decrease in response to a step change, the response should be concave up for a first or second-order system. This indicates that temperature response is affected by the direction of the power step and is nonlinear for large step decreases in power.

![Figure 7-6: Power step from 2.54 kW (3.40 HP) to 2.24 kW (3.00 HP)](image)

Smaller step decreases in power were run from 2.54 kW (3.40 HP) to (3.20 HP) to (3.00 HP) to (2.80 HP). The results are shown in Figure 7-7. The step between 2.54 kW (3.40 HP) and 2.39 kW (3.20 HP) is first-order and the model is given in Equation (7-4). The step between 2.39 kW (3.20 HP) and 2.24 kW (3.00 HP) is non-linear. The step between 2.24 kW (3.00 HP)
and 2.09 kW (2.8 HP) is linear if we assume the temperature rise at the very end of the weld is due to thermal reflection and neglect it. The first-order fit is given in Equation (7-5).

\[
G(s) = \frac{32.09}{10.21s + 1} e^{-0.8s} \tag{7-4}
\]

\[
G(s) = \frac{125.58}{5.75s + 1} e^{-0.8s} \tag{7-5}
\]

Figure 7-7: Power steps from 2.54 kW (3.40 HP) to 2.39 kW (3.20 HP) to 2.24 kW (3.00 HP) to 2.09 kW (2.80 HP)

Temperature response to steps in power indicates that the temperature response is non-linear because it varies with step size, direction and starting power. The response at various settings cannot be modeled as a first or second-order system. It is assumed that the large power step up, from 2.24 kW (3.00 HP) to 2.54 kW (3.40 HP), will be the best transfer function to use in estimating gains because the response is first order and spans the process window of the tool.
It is assumed that these gains will provide good control for temperatures that fall between the steady state temperatures at the high and low power levels, approximately 425°C to 490°C.

7.2 Steel

One power step weld is run that spans the usable region of the tool. First and second-order models are fit to the temperature response. The second-order model has a slightly better fit. Second-order dynamics in the temperature response is discussed.

7.2.1 Methods

A step in power was commanded from 4.48 kW (6.00HP) to 5.07 kW (6.80 HP) that spans the majority of the process window. The time delay is approximated using techniques described in Figure 6-2. First and second-order models are fit to the response using The System Identification Toolbox™ in MATLAB™.

7.2.2 Results

The step response with first and second-order fits is shown in Figure 7-8 and Figure 7-9. The first and second-order models are given in equations (7-6) and (7-7) respectively.

When using the PCBN tool, the temperature response contains second-order dynamics that can be seen in both Figure 7-8 and Figure 7-9. Although the fit is better for the second-order model, the first-order model will be used. This is a good approach because the fit for the first-order model is over 95% and most open-loop tuning rules approximate the function as first-order. Therefore, when using open-loop tuning rules, it is advantageous to model the system as first-order when possible.
Figure 7-8: First and second-order fits to temperature response to step change in power from 4.48 kW (6.0 HP) to 5.07 kW (6.80 HP) in ASTM A-36 steel

![Graph showing temperature rise over time with first-order fit (95.38) and second-order fit (98.33)]

Figure 7-9: View of second-order dynamics that occur at the start of the temperature response to a step change in power from 4.48 kW (6.00 HP) to 5.07 kW (6.80 HP) in ASTM A-36 Steel

![Graph showing temperature rise over time with first-order fit (95.38) and second-order fit (98.33)]
Second-order dynamics observed in the temperature response of PCBN tools may be due to the thermal mass of the locking collar and shaft of the PCBN tool. PCBN has a high diffusivity and represents a small portion of the tool’s mass. The material used in the locking collar and shaft have low thermal diffusivities. Therefore, when there is a significant change in temperature at the tool-working interface, the PCBN will have a fast response while the boundary conditions imposed on the PCBN by the locking collar and the shaft will have a slow response. The convolutions of these two thermal systems could be the cause of the second-order dynamics.

7.3 Successive Loop Closure

The system identification performed indicates that the conditions for successive loop closure are met. Bandwidth can be approximated by:

\[
G(s) = \frac{121.19}{(38.758s + 1)}e^{-2.644s}
\]

\[
G(s) = \frac{110.18}{(28.374s + 1)(7.09s + 1)}e^{-0.7s}
\]

Second-order dynamics observed in the temperature response of PCBN tools may be due to the thermal mass of the locking collar and shaft of the PCBN tool. PCBN has a high diffusivity and represents a small portion of the tool’s mass. The material used in the locking collar and shaft have low thermal diffusivities. Therefore, when there is a significant change in temperature at the tool-working interface, the PCBN will have a fast response while the boundary conditions imposed on the PCBN by the locking collar and the shaft will have a slow response. The convolutions of these two thermal systems could be the cause of the second-order dynamics.

\[
f_{BW} = \frac{1}{2\pi T}
\]

where \(f_{BW}\) is bandwidth. The smallest time constant for the outer loop is given in Equation (7-1) and is 25.99 s. Using Equation (7-8), the largest bandwidth of the outer loop is 6.12E-3 Hz.

Transients in the inner loop are not present in data collected by the data acquisition system. The sample rate of the data acquisition system is approximately 9 Hz. The PLC time step is approximately 0.01 s. Therefore the PLC sampling frequency is 100 Hz and the Nyquist frequency is 50 Hz. The bandwidth cannot exceed the Nyquist frequency therefore the maximum possible bandwidth for the inner loop is 50 Hz.
The bandwidth can be estimated using the kinematic model of the spindle, Equation (5-6). This model assumes that the spindle speed and spindle torque have a linear relationship as shown in Figure 5-11. Expressing spindle torque as a function of spindle speed and neglecting the bearings in Equation (5-6) becomes:

\[ RM_{mtr} = C_1 \omega_{spn} + J \dot{\omega}_{spn} \]  

(7-9)

where \( C_1 \) is a constant. The transfer function of (7-9) is:

\[
\frac{\omega_{spn}(s)}{M_{mtr}(s)} = \frac{R}{C_1 s + 1}.
\]  

(7-10)

The time constant of the system is

\[ \tau = \frac{J}{C_1}. \]  

(7-11)

The constant \( C_1 \) can be approximated using Figure 5-14a or Figure 5-14b. In both figures the spindle speed decreased approximately 10 RPM for every 1 N-m increase in torque. Therefore -10 RPM/N-m is a reasonable approximation of \( C_1 \). For the purpose of calculating the transfer function, \( C_1 \) will be 10 RPM/N-m because the time constant is determined by the magnitude, not the direction of \( C_1 \).

Using this approximation for \( C_1 \) and the previously calculated value for \( J \), Equation (7-11) yields a time constant of 0.545s. Using Equation (7-8), the bandwidth the inner loop is approximated as 0.292Hz. This is orders of magnitude larger than the bandwidth of the outer loop. The fact that the estimated time constant of the inner loop is orders of magnitude larger than the measured time constant suggests that the conditions of successive loop closure are met despite simplifications in modeling the inner loop.
8 VALIDATION OF CONTROLLER

8.1 Aluminum

This section presents results for the implementation of the temperature control algorithm shown in Figure 5-16. It is demonstrated that PID gains affect performance metrics as anticipated. The steady state error is less than 0.003°C for unfiltered temperature data. The standard deviation is 0.72°C during steady state for unfiltered temperature data. Nonlinearity can be seen by differences in the performance metrics depending on the direction of the change in temperature.

Unless otherwise specified, all temperature control welds run in aluminum are 1143mm (45in) long. The first 76mm (3.0in) are run at 600 RPM. Temperature control is enabled and the setpoint switches from 470°C to 480°C every 267mm (10.5in).

8.1.1 PID Gains

In order to properly tune the controller, the effects of the PID gains must be understood. Open-loop tuning along with trial and error were used to determine functional gains. The gains were varied to determine the effect of changing each gain.

Open-loop tuning was used to determine initial PID gains. Using Equation (7-1), CHR tuning with 20% overshoot for disturbance rejection was performed yielding PID gains of Kp=0.186, Ki=0.119 and Kd=0.062. Using these PID gains the system quickly went unstable and the weld was aborted. In order to avoid instability, the integral gain was set to zero and the
proportional and derivative gains were reduced to 0.07 and 0.03 respectively. These gains produced stable welds and the results are shown in Table 3 as weld 0. The control was not able to correct bias because the integral gain was set to zero.

The values of the gains were adjusted to determine if changes in each PID gains affects the performance metrics as indicated in Table 1. A summary of welds run in this PID gains study are shown in Table 3.

Data shown in Table 1 are from welds to investigate controller gains in aluminum.

Table 3: Gain study in aluminum.

<table>
<thead>
<tr>
<th>Weld#</th>
<th>(K_p)</th>
<th>(K_i)</th>
<th>(K_d)</th>
<th>High Temp Stable</th>
<th>Rise Time Up (s)</th>
<th>Settling Time Up (s)</th>
<th>Overshoot Up (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>0.07</td>
<td>0</td>
<td>0.03</td>
<td>Yes</td>
<td>08.763</td>
<td>21.100</td>
<td>22.73</td>
</tr>
<tr>
<td>1</td>
<td>0.07</td>
<td>0.01</td>
<td>0.03</td>
<td>Yes</td>
<td>05.268</td>
<td>30.844</td>
<td>40.30</td>
</tr>
<tr>
<td>2</td>
<td>0.04</td>
<td>0.005</td>
<td>0.03</td>
<td>Yes</td>
<td>07.781</td>
<td>80.006</td>
<td>43.00</td>
</tr>
<tr>
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<th>(K_i)</th>
<th>(K_d)</th>
<th>Low Temp Stable</th>
<th>Rise Time Down (s)</th>
<th>Settling Time Down (s)</th>
<th>Overshoot Down (%)</th>
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The effect of changing PID gains can be seen in Figure 8-1. The only difference between weld 1 and weld 6 is that weld 6 has a higher proportional gain. As expected, the rise time is shorter, overshoot greater and settling time longer in weld 6. The only difference between weld 1 and weld 7 is the integral gain is higher in weld 7. As expected, the rise time is shorter and both settling time and overshoot are greater. The only difference between weld 1 and weld 3 is that weld 3 has a derivative gain of 0.06 and weld one has a derivative gain of 0.03. As expected, weld 3 has less overshoot and a faster settling time. These results indicate that standard PID gain tuning techniques can be used to achieve a desired temperature response.

![Figure 8-1: Effect of PID gains on temperature response](image)

Nonlinearities in the system are manifest by differences in the performance metrics between a step up and a step down in commanded temperature. The fact that various gains are stable at 470°C and unstable at 480°C and vice versa demonstrates non-linearity.
Data from a weld run in thicker material at a slower speed rate shows that temperature response to a step change is similar across varying plate thicknesses and feed rates. The PID gain from weld 3 was tested using a 9.53 mm (0.375 in) plate and a feed rate of 101.6 mm/min (4.0 IPM). A graph is shown in Figure 8-2. The weld started in RPM control mode and reached a temperature over 51°C higher than the setpoint. When temperature control was engaged, the system responded with an overshoot of 29%, rise time 3.33 seconds, 10% settling time of 17.30 seconds and 1.0°C settling time of 29.5 s. The temperature response of this weld is similar to that of weld 3 despite a different material thickness, feed rate and larger difference between the setpoint and measured temperature previous to the setpoint command. The similar temperature response of the two welds is significant because it indicates that a given tool with a given thermocouple location can use the same gains with varying thermal boundary conditions and obtain similar temperature responses.

Figure 8-2: Kp=0.06, Ki=0.01, Kd=0.03 in 0.375” AL 7075 at 101.6 mm/min
Data collected from the weld shown in Figure 8-2 indicates that there is no steady state error. After the 1.0°C settling time, the temperature was held at 425°C with a standard deviation of 0.72°C for non-filtered temperature and 0.31°C for filtered. The average value for all temperature data collected after the 1.0°C settling time is 424.997°C for unfiltered temperature data and 425.020°C for filtered data. Both values can be expressed as 425.0°C.

8.1.2 Discussion

Welds run at constant RPM contain significant variations in temperature. A weld run at constant RPM in AA 7075 is shown in Figure 8-3. The first 152.4 mm of the weld are not shown so that is can be compared with temperature control welds that were run at constant RPM for the first 152.4 mm. The weld data shows that the temperature during the weld varies over a range of 7°C. The last two thirds of the graph are increasing. This suggests that the temperature would continue to increase if the weld were run longer.
Figure 8-1 and Figure 8-2 suggest that under temperature control, the temperature will stay with +/- 1°C despite the length of the weld. Figure 8-2 maintained a constant temperature for over 635 mm.

Torque, spindle speed power and temperature response to step increases in desired temperature are shown in Figure 8-4. When a higher temperature is commanded, the torque is increased to increase the power. This causes an increase in temperature and spindle speed. As the material softens the spindle speed continues to increase and the torque must be reduced to maintain constant power. When the steady state at the higher temperature value is reached, the power and spindle speed are higher and the torque is lower than at the previous steady state temperature.

The converse is true if a lower temperature is desired. When torque decreases, the power drops and material cools. This causes increase in torque that is offset by a larger reduction in spindle speed. The steady state at a lower temperature is a higher torque, lower spindle speed and lower power. The non-intuitive finding is that in order to achieve a desired temperature, the torque must initially increase in the direction opposite of its steady state value.

The reported torque and the commanded torque are approximately the same except for the instant a new temperature setpoint is commanded. Immediately after that step, there is one data point in reported torque that is extremely high. There is no physical justification for the single point spike in reported torque. Therefore it will be treated as an error and ignored until the anomaly is further investigated.
Figure 8-4: Torque, spindle speed power and temperature response to step changes in desired temperature

8.2 Steel

Initial gains are calculated using the minimum ITAE. Gains are adjusted to verify the assumed effects of PID gains. Gains are then tuned manually to achieve a rise time less than 15 seconds, overshoot less than 30%, a settling time less than 2 minutes.

8.2.1 PID Gains

Using the 1\textsuperscript{st} order transfer function open-loop gains were calculated using the Minimum ITAE open-loop tuning rule and are given by:

\[ K_p=0.079, \; K_i=0.0016, \; K_d=0.0779 \]  

\textbf{(8-1)}
A weld was run with gains found using minimum ITAE tuning rules and is shown as weld#1 in Table 4 below. The gains for weld#1 are slightly lower than those given in Equation (8-1) due to computational error. The results for the weld are typical of ITAE gains in that they produce a very slow controller that is very stable. Although the minimum ITAE gains did not have enough power to reach the target temperature within the short weld space, the weld was stable. Minimum ITAE is a good place to begin tuning because it is stable.

Table 3 contains performance metrics for various PID gains used in steel. This table shows that PID gains affect the transient as indicated in the theoretical background.

<table>
<thead>
<tr>
<th>Weld#</th>
<th>Kp</th>
<th>Ki</th>
<th>Kd</th>
<th>Low Temp Stable</th>
<th>High Temp Stable</th>
<th>Rise Time (s)</th>
<th>Settling Time (s)</th>
<th>Overshoot (%)</th>
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<td>U</td>
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Manual tuning techniques are used to obtain a rise time less than 15 seconds, overshoot less than 30% and settling time less than 2 minutes. Weld#9 met these criteria and weld#11 came very close to meeting these criteria.

Nonlinearity in temperature response is manifest in welds 9 and 11 where the gains are stable at one temperature region but not the other. As with aluminum, less aggressive gains tend to be stable and robust. Due to the increased time delay in PCBN tools, temperature control in
steel is more likely to go unstable. Therefore less aggressive gains must be used and the rise and settling times are significantly longer.

Figure 8-5: Weld in steel with $K_p=0.0616$, $K_i=0.003$ and $K_d=0.06$. P, I and D are the contributions of the proportional, derivative and integral terms to the commanded power shows a weld run with $K_p=0.0616$, $K_i=0.003$ and $K_d=0.06$. The temperature is increasing quickly when temperature control is engaged. When temperature control is engaged, the upward temperature trend is quickly reversed due to the proportional term. As the temperature slowly approaches the setpoint, the contribution of the integral gain grows much larger than the other terms. This causes significant overshoot and increased settling time. Figure 8-5 indicates that the proportional gain should be increased and the integral gain decreased to improve setpoint tracking.

Figure 8-5: Weld in steel with $K_p=0.0616$, $K_i=0.003$ and $K_d=0.06$. P, I and D are the contributions of the proportional, derivative and integral terms to the commanded power.
8.2.2 Discussion

Large temperature variation occurs when running welds in steel without temperature control. Figure 8-6 show a weld run with a constant spindle speed. The temperature varies by over 20°C in only 254 mm (10.0 in).

![Figure 8-6: 254 mm segment of constant RPM control weld in steel demonstrating large temperature variation](image)

The temperature control algorithm can maintain a temperature within a degree and a half of the setpoint in steels, as shown in Figure 8-7. While temperature remains constant, the RPM changes randomly throughout the weld, indicating that the algorithm is successfully rejecting thermal disturbances. After the settling time for the weld shown in Figure 8-7 the standard deviation for temperature was 1.27°C and 1.42°C for filtered and unfiltered temperature respectively. The average values during steady state are 899.692°C and 899.726°C for filtered and unfiltered temperature respectively. Steady state error is negligible.
8.3 Limitations

This thesis is a study of the temperature control algorithms performance when the tool is already heated to a temperature near the setpoint. Experimental data obtained indicates that the temperature control algorithm could be used for H-13 tools immediately after the plunge but not during the plunge. The algorithm should be not used for PCBN tools during or immediately after the plunge.

The temperature and spindle speed response of the weld shown in Figure 8-2 is shown in Figure 8-8. This weld was run in 9.52 mm (0.375 in) thick plate at a feed rate of 101.6 mm/min (4.0 IPM). The plunge was run at a constant spindle speed of 500 RPM and the weld traversed for 152.5mm (6.00 in) at 550 RPM before temperature control was engaged. The temperature at the end of the plunge is within 10°C of the temperature at the end of the weld traverse at constant
RPM. When temperature control is engaged, the temperature is 51°C higher than the setpoint. Spindle speed drops from 550 RPM to 180 RPM in order to cool the tool.

Figure 8-8: Temperature and RPM response \(K_p=0.06, K_i=0.01, K_d=0.03\) in 0.375" AL 7075 at 101.6 mm/min

The fact that the tool temperature does not change significantly from the end of the plunge to when temperature control is engaged indicates that if temperature control had been engaged immediately after the plunge, the temperature response would be similar to the temperature response obtained when temperature is engaged after the weld has traversed at constant RPM for some distance.

The temperature and spindle speed response to the weld run in steel shown in Figure 8-7 is shown in Figure 8-9. The temperature difference at the end of the plunge is 530°C less than the temperature when temperature control is engaged and 600°C less than the setpoint. The actual temperature of the stir zone is much hotter than indicated by the thermocouple. In PBCN tools, the thermocouple location is far from the tool workpiece interface and is wedged between
the PCBN insert and the locking collar. Although PCBN has low thermal mass, the locking collar has high thermal mass. Because the thermocouple is in contact with both the PCBN insert and the locking collar, both must be heated for the thermocouple to reach steady state.

At the end of the plunge, the visible portions of the tool and the argon shroud are glowing red, indicating that the stir zone is much hotter than indicated by the thermocouple. Because the thermocouple registers only 300°C at the end of the plunge, if temperature control is engaged, the proportional term would cause a large increase in power. The power increase would cause the stir zone to overheat. The steel would become increasingly soft causing the RPM to rapidly increase triggering machine safety limits. Engaging the temperature control algorithm during the plunge on initial traverse in steel could cause the stir zone to overheat and trigger machine safety limits.

![Figure 8-9: Temperature and RPM response of weld in steel with $K_p=0.05$, $K_i=0.0015$ and $K_d=0.09$](image-url)

**Figure 8-9: Temperature and RPM response of weld in steel with $K_p=0.05$, $K_i=0.0015$ and $K_d=0.09$**
9 CONCLUSIONS

Power control is best achieved by controlling torque rather than spindle speed. When spindle speed is used to control power, motor torque feedback is used as the control signal. Motor torque feedback is a poor control signal due to the large spikes in motor torque that occur when changes in spindle speed are commanded. When these torque spikes occur, the motor torque cannot be used to approximate spindle torque. When torque is adjusted to control power, spindle speed is used as the control signal. Uncertainty in spindle speed measurement is small. Motor torque can be used to approximate spindle torque when spindle torque is adjusted to keep power constant.

Power leads temperature. Experimental results show that when a weld is run at constant power a steady state temperature is achieved. When the power is changed, there is a time delay, followed by a characteristic change in temperature resulting in a new steady state temperature.

First-order with time delay is an appropriate model for temperature response. Experimental and numerical results indicate that the temperature of the tool can be approximated by a first-order transfer function with time delay.

The temperature response is dominated by thermocouple location. Experimental data and numerical modeling show that both time delay and time constants increase with distance from the thermocouple to the tool-workpiece interface. Second-order dynamics could not be seen in welds run in aluminum. Using the PCBN tool, the second-order model had a slightly higher fit than the first-order model. Numerical solutions indicate that second-order dynamics become
increasingly significant as distance between the tool-workpiece interface and the thermocouple increases.

Conservative open-loop tuning rules can be used to obtain functional controller gains. The use of aggressive loop tuning can cause the process to go unstable or results in poor weld quality. Conservative tuning rules such as Minimum ITEA result in controller gains that provide a slow but stable temperature response. Gains calculated using Minimum ITEA are a good starting point for manual PID tuning.

Standard PID gain tuning can be used to achieve a desired temperature response. Experimental data shows the changes in PID gains affect the temperature response as expected for welds in aluminum and steel. Using manual PID gain tuning, the desired temperature response was achieved for welds in steel

The algorithm has negligible steady state error. For a weld run in aluminum, after the settling time, the standard deviation from the setpoint was 0.717°C for non-filtered temperature and 0.312°C for filtered temperature. The difference between the average temperature and the setpoint is 0.003°C for unfiltered temperature data and 0.020°C for filtered data. Using unfiltered temperature data, the difference between the setpoint and the average temperature at steady state is 0.003°C for aluminum and 0.274°C for steel. The steady state error for aluminum is 7.06e⁻⁴% of the setpoint and 0.0304% of the setpoint of steel.

Noise seen for air welds is similar to noise seen in temperature control welds. For air welds used to calculate spindle inertia, the standard deviation in temperature was 0.775°C for non-filtered temperature and 0.217°C for filtered temperature. These values represent instrumentation noise and are approximately the same as the temperature deviations reported for
temperature control in aluminum after the settling time. The performance of the temperature control algorithm may be limited by instrumentation noise.
REFERENCES


