

ENHANCING FRICTION STIR WELDING THROUGH PROCESS INSTRUMENTATION AND CLOSED-LOOP CONTROL

By

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NOMENCLATURE

c	Command signal generated by controller, time domain [rpm]
C	Command signal generated by controller, frequency domain [rpm]
e	Error in temperature, time domain [$^{\circ}\text{C}$]
E	Error in temperature, frequency domain [$^{\circ}\text{C}$]
E_F	Axial force error [N]
E_T	Temperature error [$^{\circ}\text{C}$]
E_{tot}	Total radiated flux [W/m^2]
F_z	Axial (downward) force [N]
F_Z	Measured axial force [N]
$F_{z,\text{avg}}$	Average axial (downward) force [N]
F^*_Z	Desired axial force [N]
G_c	Closed-loop controller transfer function
GM	Gain margin [dB]
G_p	Process transfer function
K_c	Controller gain [rpm/ $^{\circ}\text{C}$]
$K_{c,F}$	Force controller gain [mm/N]
$K_{c,T}$	Temperature controller gain [rpm/ $^{\circ}\text{C}$]
K_p	Process gain (overall) [$^{\circ}\text{C}/\text{rpm}$]
$K_{p,F}$	Process gain (force) [N/mm]
$K_{p,\text{fsw}}$	Process gain (FSW process) [$^{\circ}\text{C}/\text{rpm}$]
$K_{p,T}$	Process gain (temperature) [$^{\circ}\text{C}/\text{rpm}$]
$K_{p,\text{tc}}$	Process gain (thermocouple sensor) [$^{\circ}\text{C}/^{\circ}\text{C}$]
$K_{p,Z}$	Cross-coupling gain [rpm/mm]
$K_{p,\Omega}$	Cross-coupling gain [mm/rpm]
M_z	Spindle moment [Nm]
N	Spindle rotation speed [rpm]
PM	Phase margin [degrees]
Q_{gen}	Generated power by tool [W]
$Q_{z,\text{avg}}$	Average spindle power [W]
T	Temperature [K]
T_{avg}	Average interface temperature [$^{\circ}\text{C}$]
T_{int}	Measured interface temperature [$^{\circ}\text{C}$]
T^*_{int}	Desired interface temperature [$^{\circ}\text{C}$]
$T_{\text{int},a}$	Interface temperature (actual) [$^{\circ}\text{C}$]
$T_{\text{int},\text{tc}}$	Interface temperature (measured) [$^{\circ}\text{C}$]
$T_{r,1}$	10 - 90 % rise time [s]
T_s	Sampling time [s]
T_{sol}	Solidus temperature [$^{\circ}\text{C}$]
UTS	Ultimate tensile strength [MPa]
v_{cp}	Current probe voltage [V]
v_{travel}	Travel speed (weld speed), time domain [mm/min]
V_{travel}	Travel speed (weld speed), frequency domain [mm/min]
Z^*_{tool}	Commanded vertical tool position [mm]

Symbols

ΔT	Process delay [s]
ΔT_p	Process delay (overall) [s]
$\Delta T_{p,a}$	Process delay (actuator) [s]
$\Delta T_{p,F}$	Process delay (force) [s]
$\Delta T_{p,T}$	Process delay (temperature) [s]
$\Delta T_{p,t}$	Process delay (wireless transmission) [s]
ε	Emissivity [-]
σ	Stefan-Boltzmann constant [= $5.67 \cdot 10^{-8} \text{ W/m}^2\text{-K}^4$]
τ	Time constant [s]
τ_p	Time constant (overall) [s]
$\tau_{p,a}$	Time constant (actuator) [s]
$\tau_{p,F}$	Time constant (force) [s]
$\tau_{p,fsw}$	Time constant (FSW process) [s]
$\tau_{p,T}$	Time constant (temperature) [s]
$\tau_{p,tc}$	Time constant (thermocouple sensor) [s]
Φ	Phase angle [rad]
ω	Frequency [Hz]
ω_B	Bandwidth or break point frequency [Hz]
ω_C	Cross-over frequency [Hz]
ω_{tool}	Angular velocity of tool, time domain [rpm]
Ω_{tool}	Angular velocity of tool, frequency domain [rpm]
ω_{tool}^*	Commanded spindle speed, time domain [rpm]
Ω_{tool}^*	Commanded spindle speed, frequency domain [rpm]

Acronyms

A/D	Analog-to-digital converter
CCW	Counterclockwise
CNC	Computer numerical control
Cu	Copper
DAQ	Data acquisition
EDM	Electrical discharge machining
FFT	Fast Fourier transform
FSW	Friction stir welding
HAZ	Heat affected zone
MIMO	Multi Input Multi Output
PCBN	Polycrystalline cubic boron nitride
PI	Proportional and Integral
PPW	Partial penetration weld
RTD	Resistance temperature detector
SISO	Single Input Single Output
Ti	Titanium

ABSTRACT

The objectives of this work are to determine an accurate temperature measurement strategy and to develop a closed-loop feedback control system for Friction Stir Welding (FSW). FSW is a novel joining technology enabling welds with excellent metallurgical and mechanical properties, as well as significant energy consumption and cost savings. Numerous parameter and condition variations are present in the FSW production environment that can adversely affect weld quality. To enable large scale automation while maintaining weld quality, techniques to control the FSW process in the presence of unknown disturbances must be developed. Using a custom wireless data acquisition system, two thermocouples were placed in through-holes directly at the interface of tool and workpiece so that the tips are in contact with the workpiece material. This measurement strategy reveals temperature variations within a single rotation of the tool in real-time. It was found that the temperatures correlate with weld quality (mechanical testing and weld cross-sections) and that the highest temperatures are at the shoulder interface between the advancing side and the trailing edge of the tool, closer to the advancing side. The dynamic temperature measurements obtained with the current system are of unmatched resolution, fast and reliable and are likely to be of interest for fundamental studies, process monitoring and control of FSW. A linear multi-input-multi-output process model with transport delay has been developed that captures the dynamic relations between two process inputs (spindle speed and vertical tool position) and two process outputs (interface temperature and axial force). A closed-

loop temperature control system was implemented on a CNC mill and applied to welding of 6061-T6 aluminum. The control system was also transferred to a robotic FSW system control in a real manufacturing environment and combined with axial force control. Desired axial forces and interface temperatures are achieved by automatically adjusting the spindle speed and the vertical tool position at the same time. The axial force and interface temperature are maintained during both thermal and geometric disturbances and thus weld quality can be increased for a variety of conditions in which each control strategy applied independently would fail.

Chapter 1 Introduction

1.1. Overview

Friction stir welding (FSW) was invented at The Welding Institute (TWI) in the United Kingdom in 1991 [1]. This relatively new joining process differentiates itself from most other welding processes by typically not melting the workpiece. As a result, the joining process is energy efficient, environment friendly, versatile, and generates improved joint properties.

The basic concept of FSW can be described as follows: a non-consumable rotating FSW tool with a specially designed shoulder and pin is pressed against the workpiece surface, while a vertical downward force is applied (Figure 1). Due to friction between the rotating tool and the workpiece (plastic deformation of the workpiece also produces heat), the temperature in the weld zone increases. The generated heat is usually not sufficient to melt the material, however, the workpiece is softened in the area around the pin and the deformation resistance (*i.e.*, yield strength) of the base material decreases. The tool is traversed along the weld interface to mix the joining members in a forging action along the joining line to create a weld in the “solid state”. Friction stir welding results in intense plastic deformation and temperature increase in the weld zone, which leads to a significant microstructural evolution without causing phase changes [2], [3].

Friction stir welding was initially applied to aluminum alloys but welding of other materials such as copper, titanium and magnesium alloys as well as steels and nickel alloys have been investigated [3]. Friction stir welding is also identified as a technology that can be used to join dissimilar alloys and metals. Due to its nonmelting, higher-strength and environmentally friendly nature, the process enables cost reductions in various industrial fields and enables the joining of materials considered not weldable (e.g., highly alloyed 2XXX and 7XXX series aluminum). Friction stir welding has developed numerous potential applications in aerospace, automotive, railway, shipbuilding and other areas.

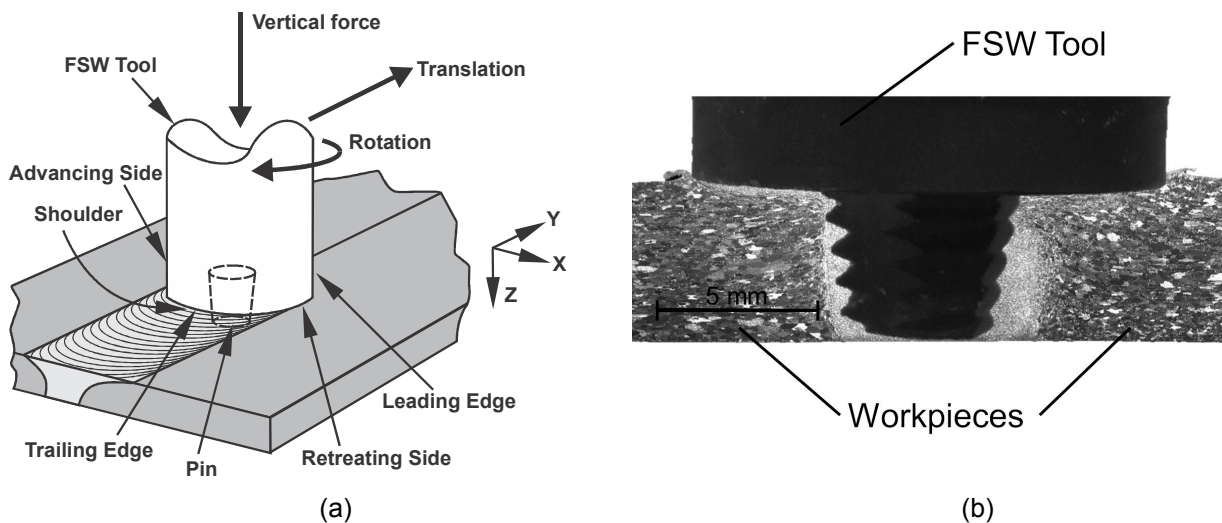


Figure 1: (a) Schematic of FSW process.
(b) FSW tool superimposed on a traverse cross-section of a weld.

1.2. Literature Review

The majority of research on control of FSW has focused on force control. The tool moves on a pre-determined three-dimensional path (position control) or the tool moves along a pre-determined two-dimensional path (x-y position) where axial position

(z-axis) is adjusted to maintain a constant axial welding force (force control). Force control has proven to be a successful technology [4–9]. When developing a robotic FSW system, Smith [4] experienced problems with the robot's inherent lack of stiffness. He found that welding with a force control algorithm produces higher quality welds than without force control. Von Strombeck *et al.* [5] also developed force control algorithms to overcome stiffness issues with robotic FSW. The system allows a defined force to be applied onto the workpiece with a maximum deviation from the default load of less than 3%. Zäh and Eireiner [6] proposed that constant welding conditions, *i.e.*, a constant axial force is required during welding to ensure a uniform weld quality. They implemented force controlled welding and presented welds with a constant axial force. Oakes *et al.* [8] developed a dynamic process model relating the travel speed, spindle speed and plunge depth to the axial force and implemented a force controller that tracks both constant and sinusoidal desired forces. As an alternative to adjusting the axial position to affect the downward force, Longhurst *et al.* [10] manipulate the traverse speed for force control. They conclude that when using traverse speed, the variation in axial force is less than when using plunge depth.

However, there are limitations: only using force control can fail when welding close to the edge of a workpiece and going around a corner (Figure 2). Heat generated by the FSW process is not able to conduct away from the weld zone as quickly as the tool approaches the corner (one direction of heat dissipation is removed), which causes the stir zone and region around it to overheat. The required axial welding force will decrease while the control algorithm tries to maintain a constant force by lowering the

vertical position of the tool. This can lead to the tool shoulder plunging below the workpiece surface and creating unacceptable welds [11]. For this reason, reliable FSW of a wide range of applications, with the inherent workpiece variability (e.g., thermal constraints, material properties, geometry) will require closed-loop control of other process parameters besides axial (*i.e.*, z-axis, plunge, forge) force. In this work, temperature is chosen as the process parameter to control. Previous studies have shown that the change in heat flow, hence weld temperature, had a measurable impact on the weld strength [12]. Relationships between temperature and weld quality have been reported in the literature for FSW. Peel *et al.* found that weld properties are dominated by the heat input (temperature) in welding aluminum 5083 [13]. Gratecap *et al.* found an influence of weld temperatures on weld quality [14]. Chao *et al.* stated that the weld quality is determined by the amount of heat conducted into the workpiece [15], [16]. Simar *et al.* stated that the thermal history of the weld material affects the microstructure in the various processed regions [17]. In order to correlate weld quality to temperature, it is important to measure temperatures as close as possible to the weld zone temperature.

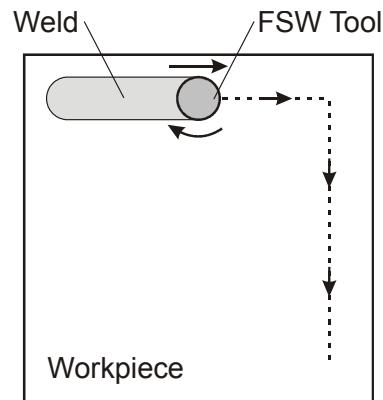


Figure 2: Schematic of welding around a workpiece corner.

Other control strategies besides force control have been developed with the goal of producing welds of continuously high quality. Wang *et al.* [18] control the spindle torque by adjusting the vertical position of the tool. The tool moves in vertical direction to keep the spindle motor current within limited values. Longhurst *et al.* [19] use plunge depth to control the spindle torque as an alternative to traditional force control because they found that spindle torque is more sensitive to plunge depth than axial force. Cederqvist *et al.* [20] are automating FSW of copper for nuclear waste canisters. A cascaded control algorithm with PI controllers was developed that determines the power input requirement and maintains the tool temperature within a process window. Ross and Sorensen [21] also employ a cascaded controller for temperature control for friction stir processing of low-carbon steel with the goal of increasing the polycrystalline cubic boron nitride (PCBN) tool life. Arbegast [22] correlates force measurements in time and frequency space directly to weld quality and concludes that magnitude and direction of welding forces are a possible basis for feedback control. Shultz *et al.* [23] developed a model that relates the joint efficiency to travel angle, plunge depth and gap width in friction stir butt welding. This groundwork is then used in the development of a shared human-computer control system that can increase weld quality by adjusting the plunge depth and travel angle for a given gap size.

Due to high instrumentation effort, relatively little work has been done in experimentally recording highly resolved weld temperatures to gain insight into the actual process and to verify the simulation results from heat transfer simulations. Infrared thermography methods are used [24], [25] but they only capture the surface

temperatures of workpiece or tool and are associated with high uncertainties due to varying target emissivity, surface condition, *etc.* Typically, thermocouples are embedded in the workpiece or backing plate [26], [27], away from the weld zone, but they only provide limited insight into the dynamics of the process due to their distance to the weld zone. Thermocouples have also been embedded into the path of the weld to obtain weld zone temperatures [28], [29], but only up to the point when the tool welds over it and physically destroys the sensor. Methods that embed temperature sensors in the workpiece require high instrumentation effort for each weld, *i.e.*, they cannot be reused on an unwelded workpiece. Alternatively, thermocouples have been inserted into the tool [30], [31], greatly reducing the instrumentation effort per weld. All of the temperature measurement methods described are not able to provide measurements with sufficiently high temporal and spatial resolution to provide insight into weld dynamics.

Great effort has been made recently in developing numerical models of FSW with the goal to improve the understanding of the complex underlying physics of the process. Models have been developed that predict temperature distributions, material flow patterns, residual stresses, microstructure evolution and others [32]. Temperature measurements used to verify such thermal models are typically lacking in the ability to provide temperatures close to the weld zone temperature (measuring workpiece surface temperatures or placing thermocouples too far away from the weld zone) or do not provide a high spatial resolution (limited number of thermocouples at fixed locations).

1.3. Motivation and Objectives

During FSW, numerous parameters and conditions can vary that affect weld quality. Changing conditions include different mechanical and thermal properties of the workpieces (Figure 3 a) and changes in the thermal capacity of the workpiece (Figure 3 b). Furthermore, variations in the thickness of the workpiece (Figure 3 c) affect the relative position of the tool shoulder to the workpiece surface and can cause voids or extensive flash. The FSW process can be sensitive to these variations, which usually are present in a production environment; hence, there is a significant need to control the process to assure high quality.

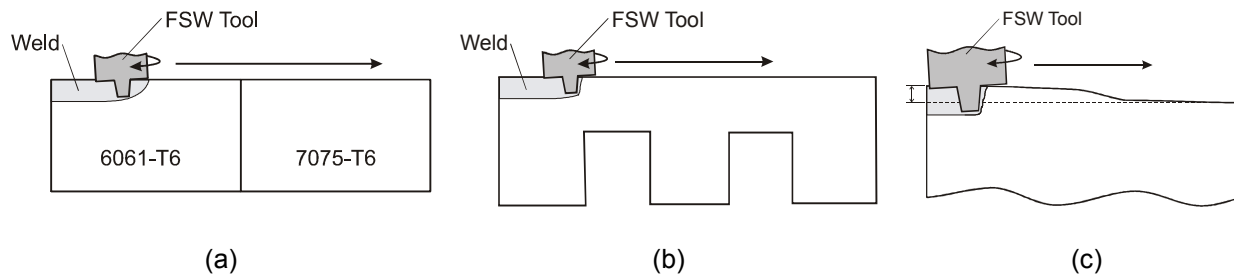


Figure 3: (a) Schematics of varying workpiece properties, (b) varying workpiece geometry and (c) varying workpiece thickness.

Workpiece variability, including changes in thermal constraints, material properties and geometry, could be addressed through complex calculations beforehand, *i.e.*, an open-loop algorithm. However, this would only be effective if the variations (disturbances) to the FSW process could be well characterized *a-priori*. This would add significant cost due to the metrology required for every workpiece and make the system inflexible. Workpiece variability can also be addressed through a closed-loop control strategy that allows flexible use under a wide range of conditions that do not have to be

known beforehand. For example, the thermal capacitance of the workpiece affects the rate at which heat flows away from the weld zone, hence stir zone temperatures. Workpieces with a larger cross-section (mass), higher density or higher specific heat capacity have a larger thermal capacitance: the ability to remove more energy per unit time (Watts) from the weld zone (Figure 4). For a given energy input from the FSW tool, this will dictate the weld temperature for a given set of weld parameters. Figure 4 illustrates the concept of varying thermal resistance and capacitance.

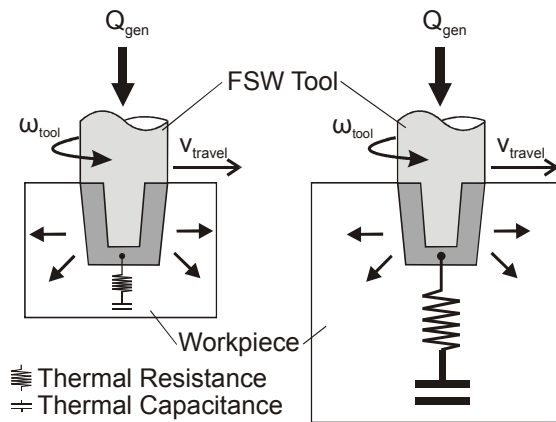


Figure 4: Schematic of change in thermal resistance and capacitance.

A simplified model of the welding process, which can be used for closed-loop control, will be developed. Several physical models have already been developed. However, these models are often complex 3D models which can predict the temperature distribution across a wide field. These models are too complex to use in closed-loop control as they cannot be solved in a short time, which is an essential requirement for the use during the process. The developed model can then be used for closed-loop control for FSW of complex aluminum parts. For any changing conditions that occur, the process should be able to adjust itself and generate welds with a uniform quality.

Both temperature and force control can help in maintaining the weld quality during the presence of inherent process disturbances. However, each control method has their limitations as follows. One possible way to implement temperature control is to change the spindle speed (or travel speed) to adjust the energy input to the weld while welding at a constant plunge depth. This strategy would fail in situations where the workpiece thickness changes as illustrated in Figure 3 (c) because proper contact of the tool shoulder and the workpiece cannot be maintained. On the other hand, when welding under axial force control only by adjusting the vertical tool position, the system might fail in certain situations: Welding over workpieces of varying thermal capacitance affects the rate at which heat flows away from the weld zone, hence changes the stir zone temperature (Figure 3 (a) and b) and can affect weld quality. In many cases, this change in temperature cannot be compensated for when welding solely under force control.

The main objectives of this work are:

- To develop a suitable temperature measurement system to provide better insight into the FSW process, benefit the fundamental process understanding and ultimately help in maintaining and improving the weld quality by developing advanced closed-loop temperature control strategies. A sophisticated temperature measurement system can be valuable for general process monitoring, verifying thermal analytical and numerical models and can provide feedback signals for process control.

- To develop a closed-loop control algorithm to maintain a constant weld zone temperature on a CNC mill testbed. Our hypothesis is that to produce welds with uniform weld quality, a constant weld zone temperature should be maintained. This does not necessarily mean that weld zone temperature is the only parameter that may need to be controlled.
- To combine closed-loop control of temperature and axial force, so that during welding both a certain temperature and a specified axial force is maintained. The developed temperature control system will be transferred from a CNC mill testbed to a robotic FSW system under real manufacturing environments. The combined control system should maintain weld quality under varying boundary conditions.

Chapter 2 Process Instrumentation and Manipulation

Since the weld zone temperature cannot be measured directly in real time without significant effort (e.g., using a neutron source [33]), the temperature at another location, close to the weld zone, must be measured.

2.1. Temperature Measurements using Infrared Pyrometers

Any object at a given temperature emits thermal radiation, which is electromagnetic radiation with wavelengths between 0.1 and 100 μm . As a comparison, visible light is in the range of 0.3 and 0.7 μm [34]. A pyrometer is a non-contacting optical device intercepting and measuring thermal radiation emitted from an object. With pyrometers (also called radiation thermometers), the temperature of an object's surface can be determined. There is no need for direct contact between the object to be measured and the pyrometer, as compared to commonly used thermocouples or resistance temperature detectors (RTDs). Pyrometers are practical for measurements in hazardous environments (such as electromagnetic fields) or for measurements of moving surfaces, like in FSW where the workpiece moves relative to the tool and also the tool itself rotates at a high speed.

Pyrometers have an optical system that focuses the intercepted radiation from an object onto a detector, which is sensitive to radiation. The output of the detector is proportional to the amount of energy radiated by the target object. Using the Stefan-

Boltzmann law (1), which states that the total energy E_{tot} radiated per unit surface area of a body is directly proportional to the fourth power of the body's temperature T , pyrometers usually internally perform a linearization to output a specific electric current per temperature based on the measured energy [34].

$$E_{tot} = \varepsilon \cdot \sigma \cdot T^4 \quad (1)$$

with the emissivity ε of the object and the Stefan-Boltzmann constant $\sigma = 5.670 \cdot 10^{-8} \text{ W/m}^2 \cdot \text{K}^4$. Pyrometers usually do not measure temperature directly. Instead, they output a current that is proportional to the radiance incident on their detector. The radiance is dependent on both the temperature and the emissivity. An emissivity of $\varepsilon = 1$ would be a perfect emitter, where the surface emissivity of extruded aluminum surfaces ranges from 0.05 to 0.2 [35].

In this research, both a single and a dual wavelength pyrometer are used. The spectral response of a pyrometer reflects the operating wavelength λ and is the width of the infrared spectrum covered. Table 1 lists the most important characteristics of the two pyrometers used in this work.

For the single wavelength pyrometer, the target emissivity needs to be known *a-priori* yielding in a limited temperature measurement accuracy. In order to improve the temperature measurement technique, a dual-wavelength pyrometer was employed for which the surface emissivity does not need to be known *a-priori*, but instead is determined and the true surface temperature is calculated from the ratio of radiation detected at two different wavelengths.

Table 1: Key specifications of pyrometers used in this work.

	Single Wavelength Pyrometer	Dual Wavelength Pyrometer
Emissivity	Fixed at 0.95	Variable
Spectral Response	8-14 μm	2.025 - 2.223 μm and 2.225 - 2.451 μm
Temperature Range	- 40 - 500 $^{\circ}\text{C}$	200 - 600 $^{\circ}\text{C}$
Model	Williamson Transtemp 290	Williamson PRO 92-20

2.2. Temperature Measurements using Thermocouples

Another strategy of measuring temperatures close to the weld zone is to insert thermocouples into the tool and to transfer the temperature measurements in real time (*i.e.*, without significant delays) via a wireless system from the tool to a stationary data acquisition (DAQ) and control system [36–38]. In general, the FSW tool is made of a material (*e.g.*, highly alloyed tool steel, H13) with a thermal diffusivity ($\alpha_{\text{H13}} = 6.8 \times 10^{-6} \text{ m}^2/\text{s}$ at 350°C) that is an order of magnitude smaller than the aluminum workpiece material ($\alpha_{\text{2024-T6}} = 6.4 \times 10^{-5} \text{ m}^2/\text{s}$ at 327°C). It is therefore desirable to place the thermocouples as close to the tool-workpiece interface as possible to minimize the time delay associated with heat flow through the tool. To the best of our knowledge, Gerlich *et al.* [36] were the first to insert thermocouples into through-holes placed into a stationary FSW tool used for friction stir spot welding. In this work, we are also making use of a through-hole to enable direct contact of the tip of the thermocouple with the workpiece material. We are employing the through-hole strategy for temperature measurements to reduce the dissipation delay during friction stir seam welding and to create an input to a closed-loop temperature controller. *I.e.*, the thermocouple is in direct contact with the surface of the workpiece material: in these tests an aluminum

alloy that has a very high thermal diffusivity. Initial results from a transient three-dimensional heat transfer model used in this work showed that the highest temperatures on the FSW tool are on the shoulder, close to the root of the pin. Another result from the heat transfer model is that thermocouples placed close to the FSW tool shoulder result in significantly shorter time delays and are closer to the stir zone temperature than workpiece or tool surface temperature measurements with a pyrometer. Cederqvist *et al.* also reduced the time delay when measuring tool temperatures by placing thermocouples closer to the shoulder-workpiece interface [39]. Placing thermocouples very close to the tool-workpiece interface region is also of interest in metal cutting, where the recent development of micro thin film thermocouples embedded in the tool lead to greater insight of temperature transients at the tool-chip contact region [40].

A wireless DAQ system (Figure 5) was built, which amplifies the small voltages (mV range) generated by two type K thermocouples and provides other signal conditioning tasks. The measurements are sampled and transmitted from the rotating tool holder to a stationary DAQ system. A second system with the same functionality has been built that is compatible with a robotic FSW setup (Figure 6). The following sections provide a more detailed description of the system design. The most important characteristics are summarized in Table 2. Figure 7 provides a schematic of the overall wireless DAQ system, illustrating the main components.

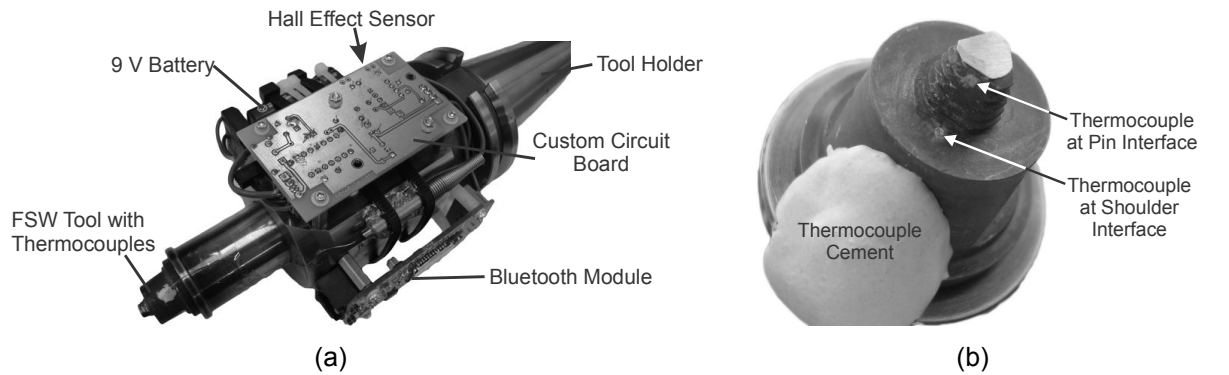


Figure 5: (a) Photograph of instrumented tool holder for use in CNC mill with (b) close-up of FSW tool.

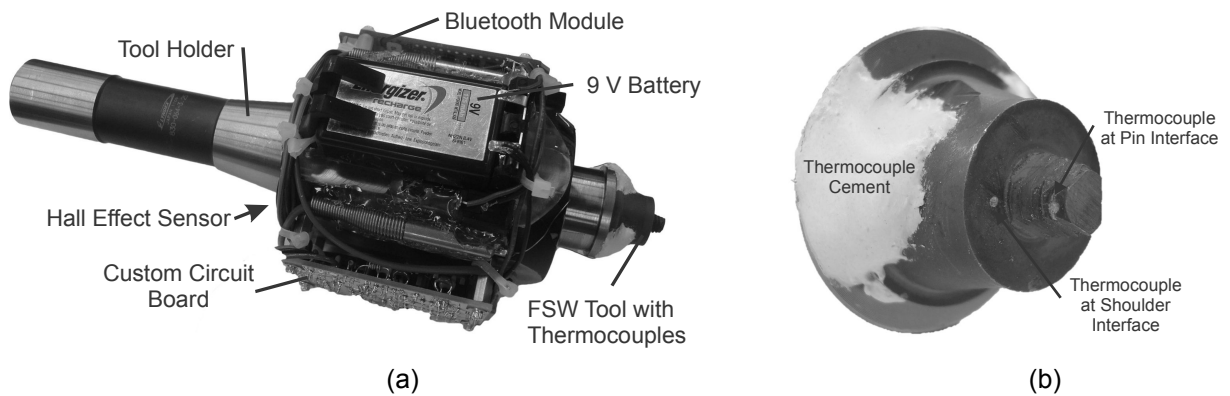


Figure 6: (a) Photograph of instrumented tool holder for robotic FSW system with (b) close-up of FSW tool.

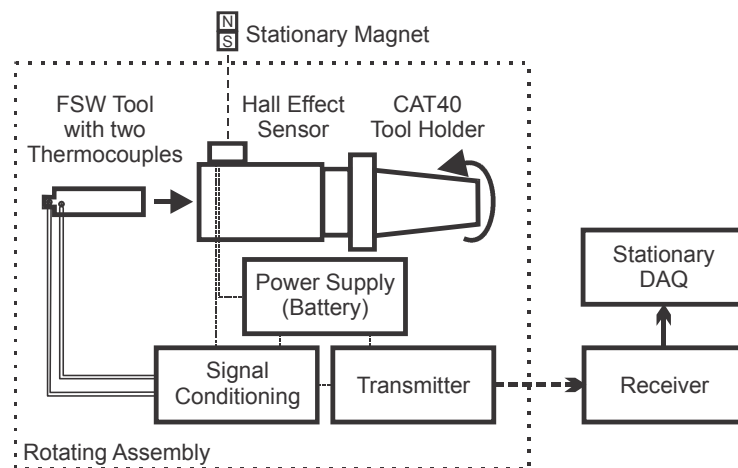


Figure 7: Schematic illustrating the main components of the wireless DAQ system used for FSW.

Table 2: Key specifications of instrumented tool holder.

Thermocouple	
Outer sheath diameter	0.25 mm
Sheath material	304 stainless steel
Calibration type	K
Junction type	Electrically insulated (ungrounded)
Response time [41]	Shoulder: 11 ms
Calibrated while embedded in tool	Pin: 37 ms
Upper temperature recommendation	600 °C (manufacturer provided)
Signal Conditioning	
Amplifier gain	154.5
Amplifier bandwidth	15 kHz
Amplifier calibration error	± 1 °C
A/D converter resolution	16 bit (0.011 °C)
Wireless transmission frequency	333 Hz

2.2.1. Mechanical Design

The tool holder is a type CAT40 end mill holder (Lyndex model C4006-1000). Three flats were machined into its shank to accommodate the three main components of the wireless DAQ system (battery, custom circuit board and wireless module). Because material is removed and parts are added, it is important that the tool holder remains balanced so as not to cause damage to the mill spindle, especially at higher rotation speeds. It is also important not to reduce the compliance of the tool holder significantly (by both drilling holes into the tool and by machining the tool holder) so that the dynamics of the process are not affected.

2.2.2. Thermocouples

The smallest possible thermocouple size was chosen to reduce the temperature response time (part no. TJ36-CASS-010U by Omega Corp.). The thermocouple sheath is made of 304 stainless steel, has an outer diameter of 0.25 mm and is electrically insulated from the thermocouple bead and leads (*i.e.*, ungrounded junction). The response time of thermocouples is a strong function of their size and the heat transfer rate from the surrounding medium.

Two 0.8-mm-diameter through-holes were fabricated using electrical discharge machining (EDM) into the tool shank (Figure 8). One 7.5-mm-deep hole exits on the shoulder, 3.4 mm from the outer edge of the shoulder. Another, 14-mm-deep hole was made that exits on the side of the pin (location of flat of the tool), 0.9 mm from the bottom of the pin, in order to obtain temperatures further down in the weld. The two through-holes are placed in the same plane and the holes exit both on the same side of the rotation axis, *i.e.*, they are located at the same angular position. The holes are placed at angles of 36 degrees and 33 degrees from the normal of the shoulder and pin surface, respectively) in order to decrease the process forces acting on the thermocouple that could push the thermocouple back out of the hole (Figure 8 (b)). Two type K thermocouple were inserted into the through-holes and secured with high temperature thermocouple cement (maximum service temperature 1426 °C).

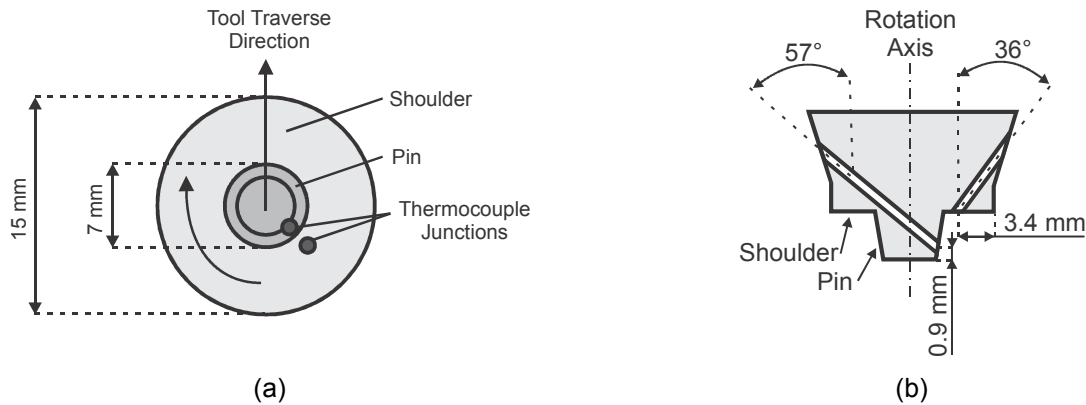


Figure 8: (a) Schematic of thermocouple locations on FSW tool (not to scale, bottom view, the pin diameter tapers from 7 mm to 5.5 mm); (b) Schematic of through-hole locations for thermocouples on the FSW tool (not to scale, section view).

2.2.3. Signal Conditioning

Signal conditioning tasks are performed on a printed circuit board, which was designed in-house and can be used to amplify voltages from 2 type K thermocouples. The board accommodates connectors, amplifiers, low pass filters (cut off frequency $f_c = 100$ Hz), a voltage regulator and a voltage limiting circuit to protect the sensitive A/D converter on the wireless module from voltage spikes and temperatures above the maximum limit. Thermocouple voltages are amplified using a type K thermocouple amplifier (Analog Devices AD595C), which includes cold junction compensation. It has a 3 dB bandwidth of 15 kHz and a calibration error of ± 1 °C. The gain of the device was adjusted by adding an external resistor in order to process measured temperatures from 0 to 700 °C resulting in output voltages from 0 to 4.5 V.

2.2.4. Wireless Transmission

Many commercial industrial wireless products are available, some of them already include A/D converters. The Bluetooth protocol seems to have superior success for

applications in which the transmitter is rotating [38]. The Bluetooth sensor interface used in this study (Roving Networks BlueSentry) contains a built in processor, A/D converter and a class 1 (100 m range) Bluetooth radio with an integrated antenna. It has eight 16 bit analog inputs (0 to 5 V range) and a maximum sampling rate of 3000 Hz. With dimensions of 76x41x23 mm, it fulfills the size (and weight) constraints. Power requirements are 6 to 12 VDC, 75 mA, *i.e.*, it can easily be powered from batteries. Measurements are transferred wirelessly to a PC using the serial port profile. On the receiving side, a standard Bluetooth to USB dongle is connected to the DAQ computer. Data Acquisition and prototype control software (National Instruments LabVIEW™) plots and records the measurements. The resulting temperature resolution is 0.012 °C. To determine the true thermocouple temperature correctly, the original thermocouple voltage (in the range of millivolts) is back calculated using the amplifier gain. Then, an 8th order polynomial is used to calculate the true thermocouple temperature (nonlinear with junction voltage) [42].

2.3. Welding Power, Forces and Angular Position Measurements

2.3.1. Tool Power Input

For a means of better process understanding and the development of future process models it is also of interest to measure the power generated by the FSW tool. Because the spindle of the CNC mill delivers this power, one approach is to measure this power directly. Another way is to measure the spindle moment and relate that to spindle power by using the spindle speed. Due to heat losses from friction within the

motor and drive train, measured motor power is not equal to delivered tool power. As such, calculating the weld power from the spindle torque is a more accurate measure of the power into the weld. A very elegant and accurate way of measuring the spindle moment is the use of a rotating dynamometer, where the measured spindle moment is transferred with a slip ring. Unfortunately, such a rotating dynamometer is not readily available. A relatively easy way of measuring the spindle moment is the use of a current probe using the linear relationship between the motor current and the delivered torque of the motor. A calibration was done to accurately relate measured motor currents and spindle moment and can be found in the appendix. The power needed to traverse the FSW tool can be neglected, as it only represents about 1 % of the power due to the tool rotation.

2.3.2. Welding Forces

During FSW, relatively large forces are experienced. It is therefore of interest to quantify those forces during the experiments. Welding forces are measured using a 3-axis force dynamometer (Kistler model 9265A). The 3-axis dynamometer is temperature sensitive, hence it is cooled during and in between experiments using a constant temperature bath at room temperature.

The 3-axis force dynamometer is also able to determine the 3 component moments acting on it. The device contains four individual load cells, each able to measure forces in x, y and z direction. In order to determine the moments applied to the dynamometer, the distances of the applied moment to the load cells must be known. A

calibration was performed with known positive and negative moments (using lever arms and weights) while isolating the 3 moments. Unfortunately, some issues were found that limit the use of this method during FSW. The determined moments drift during the traverse of the FSW tool as well as severe process oscillations were found, which are dependent on the distance of the welding tool to the load cells. These problems can be attributed to the fact that the load cell sensitivities used are an average of the four sensors for each axis, but each sensor might have slightly different sensitivities. Also, thermal issues play a role where the thermal expansion of the fixture could apply additional moments. Furthermore, the moment applied by the tool is a distributed moment across the cross-sectional area of tool (not a point load), which causes inaccuracies of determining the true location of the applied loads. Another issue is that the range of measurement for the moment applied by the tool is only in the order of 15 Nm. The dynamometer is able to measure moments up to 1.3 kNm. This represents only about 1 % of the full scale.

2.3.3. Angular Tool Position

In order to correlate the temperature measurements with the angular position of the FSW tool, a magnetically operated proximity sensor (Hall effect sensor, model 55140 by Hamlin) is used. A Hall effect sensor is a solid-state device with no moving parts that triggers when a magnetic field is nearby. The sensor chosen has a high enough switching speed (10 kHz) and is mounted on the rotating tool holder rather than stationary in order to synchronize the temperature measurements with the signal from the Hall sensor. One small magnet is mounted stationary ahead of the tool holder.

Compared to rotary encoders, this method has a very limited resolution (only one pulse per tool revolution), but was considered to be sufficient for the given application.

2.4. Process Inputs and Manipulation

The CNC mill allows spindle speed and feedrate overrides while a program is being executed. The overrides can be performed by either a jog dial wheel in $\pm 1\%$ increments of the nominal parameter value or by buttons on a keypad in $\pm 10\%$ increments of the nominal parameter. The adjustment range is 0% to 990% of the nominal parameter. In order to allow finer increments of adjustments with the keypad buttons, the initial spindle speed or feedrate can be set to a lower value (*e.g.*, an initial spindle speed of 200 rpm will result in a resolution of 20 rpm and a maximum speed of 1980 rpm). A custom interface allowing to simulate the jog dial wheel is available [43]. In addition, a custom interface was designed and built to activate the keypad buttons remotely from a PC. This setup allows the manipulation of both spindle speed and travel speed during a certain weld and therefore enables the implementation of more complex control scenarios, *e.g.*, combined force and temperature control. The update rate of the manipulation is limited to 40 Hz when using the jog dial interface. When using the keypad interface, there is a need to hold and release the buttons for at least 25 ms (50 ms cycle time). This limits the update rate significantly (*e.g.*, with an initial spindle speed of 200 rpm, a change from 1000 rpm (+400%) to 1500 rpm (+650%) will take 25 cycles, *i.e.*, 1.25 s). It was found from initial temperature control experiments however, that the maximum rate of change was only about 120 rpm/s. In this work, only the interface

simulating the jog dial was used for the system identification and control experiments performed on the CNC mill.

2.5. Experimental Setup

Most experiments in this work were performed on a commercial 3-axis CNC mill. Due to limitations on that machine, the measurement and control system was transferred to a robotic FSW setup. A total of three FSW tools were used. The tools are identical besides the features mentioned in Table 3. All tools are made of H13 tool steel with a concave shoulder and a threaded, conical pin with three flats. The tool shoulder diameter is 15 mm, the pin diameter is 7 mm at the base and has a 10 degree taper. The tool travel angle was held constant at 3 degrees for all welds and the tool rotation direction is always counterclockwise. The tools were machined in house on a 5-axis mill-turn center (Mori Seiki NT1000/W).

Table 3: Overview of instrumented FSW tools used in this work.

	Tool A	Tool B	Tool C
Number of through-holes	1	2	2
Interface temperature measurement locations	Shoulder	Shoulder and pin	Shoulder and pin
Pin length	3.0 mm	4.7 mm	4.7 mm
Thermocouple location at pin interface	n/a	Flat	Thread
Experimental testbed	CNC mill	CNC mill	Robot

2.5.1. Experimental Setup on CNC Mill

Using tool A (Table 3), 180 mm long bead-on-plate welds were conducted on nominal 203 mm x 76 mm x 3.18 mm (8" x 3" x 1/8") aluminum 6061-T6 workpieces at a nominal spindle speed of 1000 rpm, a constant travel speed of 200 mm/min and constant plunge depth of 3.2 mm. A nominal condition weld has an 8 mm thick low carbon steel backing plate over the full length of the workpiece. Each welding condition was repeated once, *i.e.*, each shown data point is the average of two welds performed at the same condition and the vertical bars in figures indicate the range of measured values.

With tool B (Table 3), 175-mm-long butt welds were created between two 203 mm x 102 mm x 4.76 mm (8" x 4" x 3/16") aluminum 6061-T6 workpieces (unless otherwise noted) at a constant plunge depth of 4.9 mm (position control). The abutting surfaces were milled prior to welding in order to create zero gap butt welds. An 8-mm-thick low carbon steel backing plate was used under the workpieces (unless otherwise noted). As part of a full-factorial series of experiments, the spindle speed was varied from 700 rpm to 1700 rpm in 200 rpm increments (always rotating counterclockwise). For each spindle speed, the travel speed was varied from 100 mm/min to 500 mm/min in 100 mm/min increments, resulting in a total of 29 welds (the weld with 700 rpm and 500 mm/min was not performed to prevent possible damage to the tool due to very low expected temperatures). All samples were orientated so that they were welded parallel to the rolling direction. Welds were also performed at various plunge depths, ranging from 4.6 mm to 5.0 mm in 0.1 mm increments with a constant spindle speed of 1200 rpm and a

constant travel speed of 200 mm/min. As part of an additional study to compare different aluminum alloys, 75-mm-long bead-on-plate welds were performed on alloys 2024-T3, 5083-H116, 6061-T6 and 7075-T6 at a constant spindle speed of 1000 rpm, a constant travel speed of 150 mm/min and a constant plunge depth of 4.9 mm.

The plunge rate was 20 mm/min and the dwell after the plunge was 3 s. The sampling rate for the (wireless) temperature and angular position measurements was 250 Hz to 333 Hz. In addition, welding forces and spindle torque were measured at 2500 Hz using a three-axis force dynamometer (Kistler model 9265A) and a calibrated current probe (APPENDIX D), respectively.

The mill spindle speed or travel speed was manipulated through a custom interface simulating the jog dial on the mill operator panel. Spindle speed or travel speed commands are sent at 20 Hz to the mill.

Mechanical testing was performed on a 44 kN mechanical testing machine (MTS Sintech 10/GL) according to AWS B4.0:2007, reporting ultimate tensile strengths (UTS). Macrographs of welds were prepared, polished and etched using a modified Poulton's reagent. Grain size measurements were performed according to ASTM E1382-97.

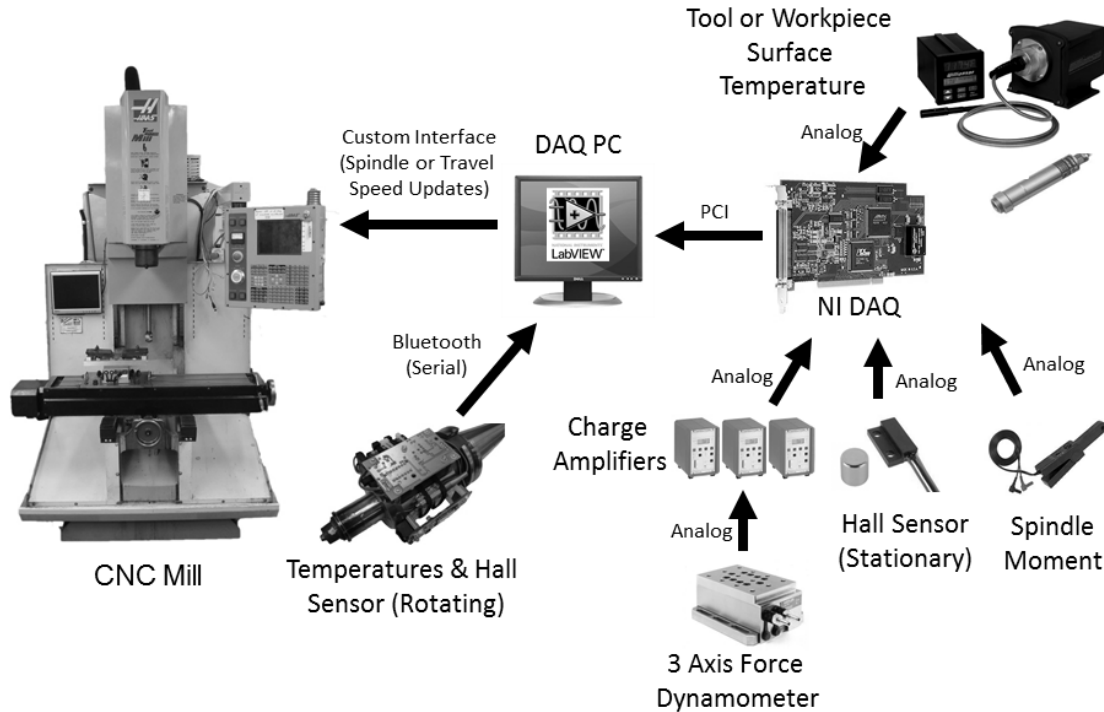


Figure 9: Signal flow schematic of experimental testbed (CNC mill).

2.5.2. Experimental Setup on Robotic FSW System

Welding was performed on a robotic FSW system (Friction Stir Link, Inc. Robostir™ system using an ABB IRB 7600 robot with an S4C+ robot controller). The spindle motor's (Control Techniques model MH-8500) maximum power rating is 9.95 kW, the rated torque is 31.6 Nm and the maximum speed is 3000 rpm. On the given setup, a gear ratio of 1:2.83 is utilized between the motor and the spindle, *i.e.*, the maximum spindle speed is 8500 rpm and the maximum torque is 11.2 Nm. The tool travel angle was held constant at 3 degrees by using an angled fixture and the spindle being orientated vertically. Tool C (Table 3) was used for all welds on the robotic system. 170-mm-long bead-on-plate welding was performed on extruded 6.35 mm thick

6061-T6 aluminum workpieces (102 mm by 203 mm), *i.e.*, all welds are partial penetration welds. An 8 mm thick low carbon steel backing plate was used under the workpieces unless otherwise noted.

Welding forces were measured using a 3-axis force dynamometer (Kistler model 9265A), which was cooled during and in between experiments using a constant temperature bath at room temperature. The spindle torque was recorded through a serial interface (Modbus communication protocol) from the spindle motor servo drive (Control Techniques model FM-3DN). Custom data acquisition, logging and control software (developed using National Instruments LabVIEW™) is running on a laptop and communicates with the robot controller through a serial interface. Compared to the robot controller, the software on the laptop offers a powerful programming environment, fast processing, various interfaces to connect other devices, real-time plotting, *etc.* Based on the measured temperature and axial force, the control software determines spindle speed and vertical tool position adjustments, which are sent to the robot. Custom code was added to Friction Stir Link's StirWare™ software (programmed in ABB's Rapid), which runs on the robot controller and receives and processes the adjustments. The setup is able to manipulate both spindle speed and vertical tool position adjustments at the same time. The spindle speed and vertical tool position commands were limited to a certain range (1700 rpm to 2900 rpm and 7 mm to 18 mm, respectively).

Force measurements were sampled at 2500 Hz. The sampling rate for the temperature measurements was 333 Hz. Spindle torque was available at approximately 5 Hz. Spindle speed and vertical tool position commands were sent at 20 Hz to the robot controller, where they were updated at approximately 10 Hz. The schematic of the signal flow of the measurement and control system is shown in Figure 10.

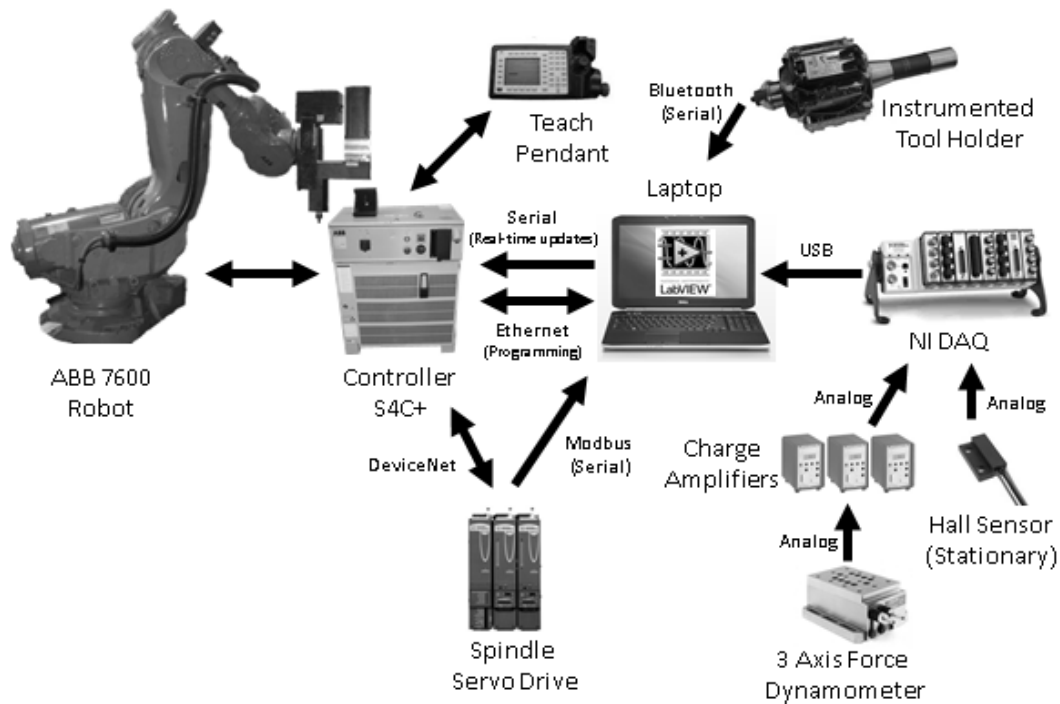


Figure 10: Signal flow schematic of experimental testbed (robotic FSW system).

Chapter 3 Dynamic Process Modeling

3.1. Introduction

For proper control system development, a model of the physical process has to be generated in order to take advantage of model-based control system methodologies. As a first step towards controlling FSW, the simplest control scenario is studied. In the simplest case, feedback control is achieved with a closed-loop, single input single output (SISO) system. Because our goal is to maintain a constant weld zone temperature, we would ideally measure it, feed it back to the control device, and generate a manipulation (command) signal that affects the welding process and hence the weld zone temperature (Figure 11). Possible independent variables to manipulate in order to maintain the desired weld zone temperature must affect the heat input per unit length of weld: travel speed, tool rotation speed, and tool plunge depth are among the candidates. In general, a constant tool plunge depth is desired, which eliminates this as a manipulated variable.

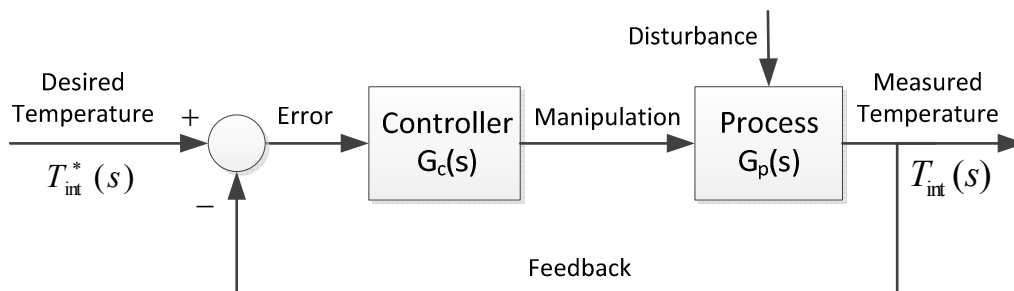


Figure 11: Generalized block diagram of entire temperature control system.

The described process model and control algorithm employs the relationship between commanded spindle speed and measured interface temperature. Another process input parameter that changes the heat input per unit weld length is travel speed. In addition to spindle speed, the effect of travel speed changes on the measured interface temperature was investigated. Compared to the relatively large temperature variations caused by spindle speed changes, changing the travel speed has a negligible effect on the interface temperature for this alloy (6061-T6). In addition, in the literature it is commonly found that spindle speed has a larger effect on temperature under the tool than travel speed [44], [45]. For these reasons spindle speed was used as the process parameter to control interface temperature for this workpiece-tool combination.

3.2. Dynamic Model for Temperature Control System

Changing the spindle speed sinusoidally leads to variations in heat generation and can cause the temperature at the workpiece-tool interface to oscillate at the same frequency as the spindle speed oscillates. Under certain conditions, the measured interface temperature can correlate with the change in process input as shown in Figure 12 for 2024-T3 and changing spindle speed at 0.1 Hz. The particular amplitude (in this work, amplitude always refers to the 'half the peak-to-peak amplitude') of these temperature oscillations is different for each alloy and is determined by performing a Fast Fourier Transform (FFT) analysis.

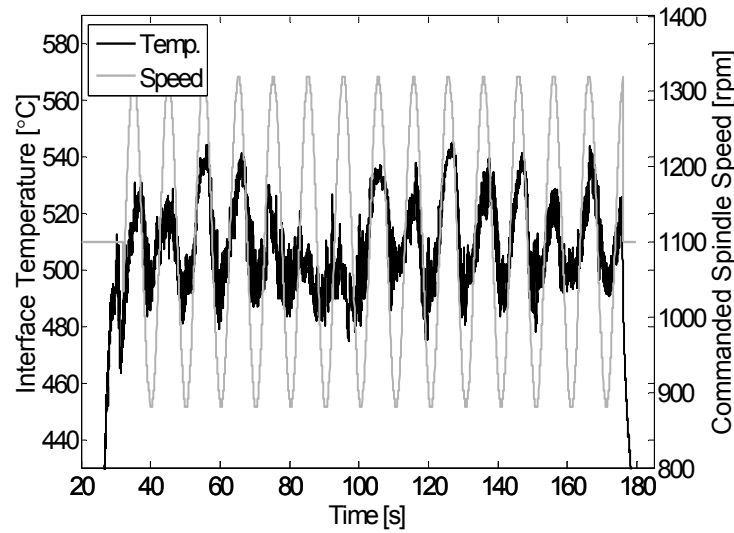


Figure 12: Measured interface temperature and commanded spindle speed during the weld traverse for 2024-T3. Changes in spindle speed occurred at 0.1 Hz and an amplitude of 220 rev/min. The resulting amplitude in interface temperature is 14 °C.

In the Laplace domain, the physical process model is denoted as $G_p(s)$. Its input is spindle speed, ω_{tool} , and its output is interface temperature, T_{int} . To gain insight into the physical process model, the frequency response of the system, relating measured interface temperature, T_{int} , to spindle speed, ω_{tool} , was experimentally determined. Specifically, during the traverse of a weld, the spindle speed was changed sinusoidally at a constant frequency and amplitude around a nominal spindle speed of 1000 rpm. Both the travel speed and plunge depth were held constant. Welds were performed at sinusoidal spindle speeds with constant frequencies ranging from 0.1 Hz to 3 Hz. The amplitude was held constant at 50 rpm for all tests. The changes in spindle speed lead to variations in heat generation and cause the temperature at the workpiece-tool interface to oscillate at the same frequency as the spindle speed oscillation. The amplitude and frequency of those temperature oscillations can be determined using fast Fourier transform (FFT) methods (assuming linearity and ignoring any harmonics).

Figure 13 shows an FFT plot of the temperature measurements during the FSW traverse. Both the spindle speed (1000 rpm = 16.7 Hz) and the oscillation of the spindle speed (1 Hz) can be identified. The experimental results of the system identification tests are shown in Figure 14 in the form of a frequency response plot. The magnitude plot displays the ratio of the amplitudes of the measured temperature and the spindle speed for various frequencies. The phase plot shows the phase difference of the measured temperature sinusoidal waveform with respect to the waveform in spindle speed.

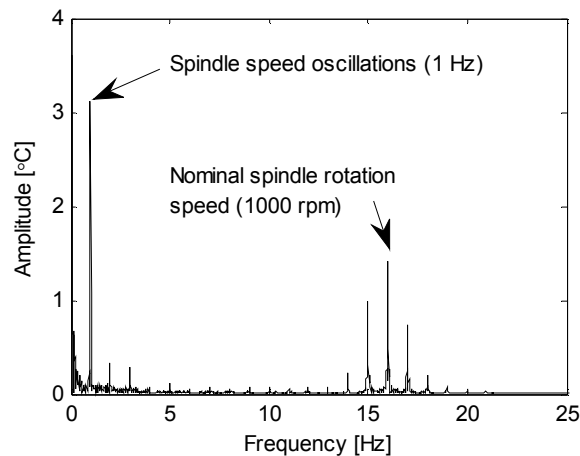


Figure 13: FFT of interface temperature measurements during FSW traverse. The spindle speed was changed at a frequency of 1 Hz and amplitude of 50 rpm around a nominal speed of 1000 rpm. The travel speed was constant (200 mm/min), the workpiece was 6061-T6.

To match those experimental values with a simple model, first and second order models were fit to the data. It was determined that the physical process can be modeled adequately as a simple first order model with a transport delay (dead time). A dead time element does not change the magnitude, but adds frequency dependent phase. Possible causes of a transport delay are delays in the physical process itself (heat conduction) and delays in communication and processing of sensed signals (the

Bluetooth protocol using the serial port profile is known to have a random latency [46], [47]). A time delay always reduces the system stability and limits the achievable bandwidth of a system. Therefore, it is important to quantify the delay and incorporate it into the controller design. The transfer function of a dead-time element in the Laplace domain is

$$e^{-\Delta T \cdot s} = e^{j \cdot (-\omega \Delta T)} \quad (2)$$

where ΔT is the delay time and ω is the frequency. The dead time transfer function is approximated using Padé approximations to generate a transfer function with polynomial numerator and denominator. A 2nd order Padé approximation was found to be sufficient to cover the frequencies used in this work. The delay time ΔT can be calculated by measuring the phase Φ of the experimental data at various frequencies ω and using the following equation

$$\Delta T = \frac{-4 \cdot \pi}{360} \cdot \frac{\phi}{\omega} \quad (3)$$

A total delay of $\Delta T = 85$ ms was determined that fits the experimental data (Figure 14). The overall first order process transfer function including the transport delay is as follows

$$G_p(s) = \frac{0.6377}{s + 9.111} \cdot e^{-0.085s} \approx \frac{0.6377}{s + 9.111} \cdot \frac{s^2 - 70.59 \cdot s + 1661}{s^2 + 70.59 \cdot s + 1661} \quad (4)$$

The first order system is characterized by a time constant of $\tau = 0.11$ s and a gain of 0.07 °C/rpm.

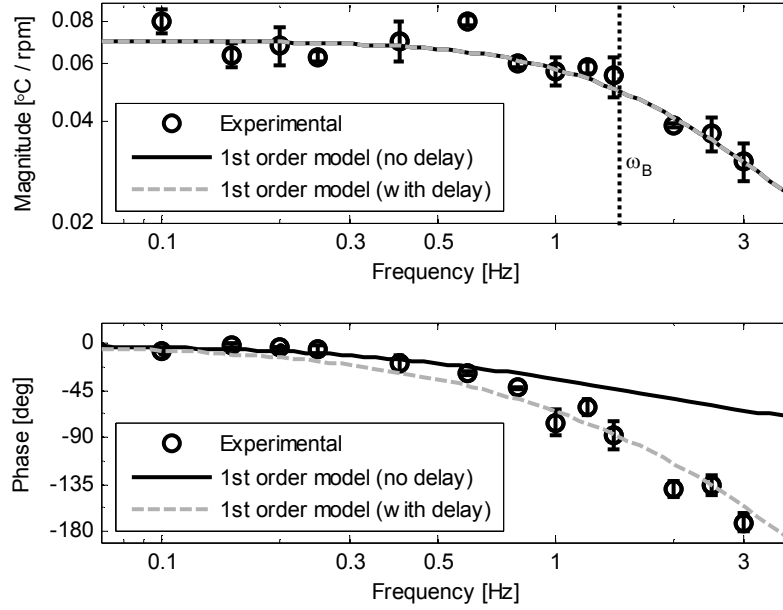


Figure 14: Frequency response plot of physical process relating spindle speed and interface temperature. The first order model has a break point of $\omega_B = 1.45$ Hz.

3.3. *Dynamic Model for Combined Temperature and Force Control System*

The closed-loop controller developed in this work is model based, hence a dynamic process model needs to be established. Other reasons for obtaining a process model are the ability to better understand the process dynamics, to get a basic insight into the physics governing the process and to help explain the limitations of the system. The process is represented by a multi-input-multi-output (MIMO) system including two inputs: the commanded spindle speed $\omega_{\text{tool}}^*(t)$ and the commanded vertical tool position $z_{\text{tool}}^*(t)$ and two outputs: the interface temperature $t_{\text{int}}(t)$ and the axial force $f_z(t)$.

The closed-loop controller is designed in a way that cross-coupling effects are decoupled, *i.e.*, nulled. For the purpose of model development it is assumed at this point that no cross-coupling exists. This allows the development of two individual models. First, a model relating the commanded spindle speed to the interface temperature is introduced. Next, a model relating the commanded vertical tool position to the axial force is discussed. Finally, a combined MIMO model using the two previous models with additional coupling terms is presented. Realizing that the system includes nonlinearities, a linear representation of the system is chosen for simplicity.

Prior system identification work of the welding process indicated that a first order model with pure delay (gain $K_{p,T}$, time constant $\tau_{p,T}$ and delay $\Delta T_{p,T}$) could be used to represent the dynamic relation between commanded spindle speed and measured shoulder interface temperature for FSW on a CNC mill. The process model was identified using frequency domain techniques. While providing good process model and parameter identification, frequency domain identification can be time consuming. Now, we can rely on the earlier process model identification and focus on parameter identification, assuming that the same process model type is also suitable for the robotic setup. As such, time domain identification, via examination of time history response to step inputs, is used to model the relationship between commanded spindle speed and measured shoulder interface temperature. The model is shown in transfer function form in equation (5).

$$\frac{T_{\text{int}}(s)}{\Omega_{\text{tool}}^*(s)} = \frac{K_{p,T}}{\tau_{p,T} \cdot s + 1} e^{-\Delta T_{p,T} \cdot s} \quad (5)$$

The relation between commanded vertical tool position $z_{\text{tool}}^*(t)$ and measured axial force $f_z(t)$ is modeled using a simple analytical, lumped parameter model of the system as illustrated in Figure 15. The single degree of freedom (DOF) model consists of one mass m , two springs with stiffness k and two dampers with damping factor c . The springs represent the flexibility of the robotic manipulator and the workpiece while the dampers add damping to the system behavior. Only displacements and forces perpendicular to the workpiece surface (*i.e.*, parallel to the tool axis) are considered in this model. $z_{\text{tool}}(t)$ represents the actual vertical position of the tool. The differential equation that governs the physics of the simple model is given in equation (6). The force $f_z(t)$ is measured using a dynamometer and is related to the actual vertical tool position as shown in equation (7). A higher DOF model with more masses could be chosen, but the described simple model is believed to capture the main dynamics of the system. The model is not intended to predict actual numerical values, but rather used to gather qualitative information about the system.

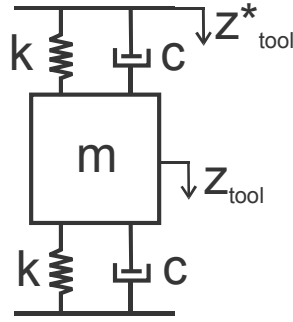


Figure 15: Schematic of simple analytical 1-DOF lumped parameter model.

$$m \cdot \frac{d^2}{dt^2} z_{tool}(t) + 2 \cdot c \cdot \frac{d}{dt} z_{tool}(t) + 2 \cdot k \cdot z_{tool} = c \cdot \frac{d}{dt} z_{tool}^*(t) + k \cdot z_{tool}^*(t) \quad (6)$$

$$f_z(t) = c \cdot \frac{d}{dt} z_{tool}(t) + k \cdot z_{tool}(t) \quad (7)$$

Using equations (6) and (7) and applying a Laplace transform yields the transfer function relating the measured axial force and the commanded vertical tool position:

$$\frac{F_z(s)}{Z_{tool}^*(s)} = \frac{(c \cdot s + k)^2}{m \cdot s^2 + 2 \cdot c \cdot s + 2 \cdot k} = \frac{K_{p,F} \cdot (\tau_{p,z} \cdot s + 1)^2}{s^2 + 2 \cdot \zeta_p \cdot \omega_{p,n} \cdot s + \omega_{p,n}^2} \quad (8)$$

with a gain $K_{p,F}$, damping ratio ζ_p , natural frequency $\omega_{p,n}$ and time constant $\tau_{p,z}$ associated with a zero of the transfer function.

Step response tests have been performed previously by Zhao *et al.* to identify nonlinear process models for the use in axial force control via plunge depth adjustments [16]. We are also estimating the model parameters by examination of the time history response to step inputs. During some welds, oscillations of the tool and robot arm were observed at approximately 0.6 to 0.8 Hz due to the low stiffness of the robotic

manipulator. The occurrence of these oscillations is dependent on the orientation of the robotic end effector, the heat input (low travel speeds are in favor of the oscillations) and other factors. The frequency of the oscillations is assumed to be a resonance frequency of the system and was used to determine the natural frequency of the model. Stiffness and mass properties change as a function of the configuration of the robotic manipulator and were determined for the particular configuration used in this work.

Both inputs of the MIMO model are assumed to each affect both outputs, resulting in a dynamic cross-coupling of the system as illustrated in the block diagram in Figure 16 and the governing equations in the Laplace domain (9) and (10). The combined MIMO model contains the two individual models as described above and two cross-coupling gains $K_{p,Z}$ and $K_{p,\Omega}$.

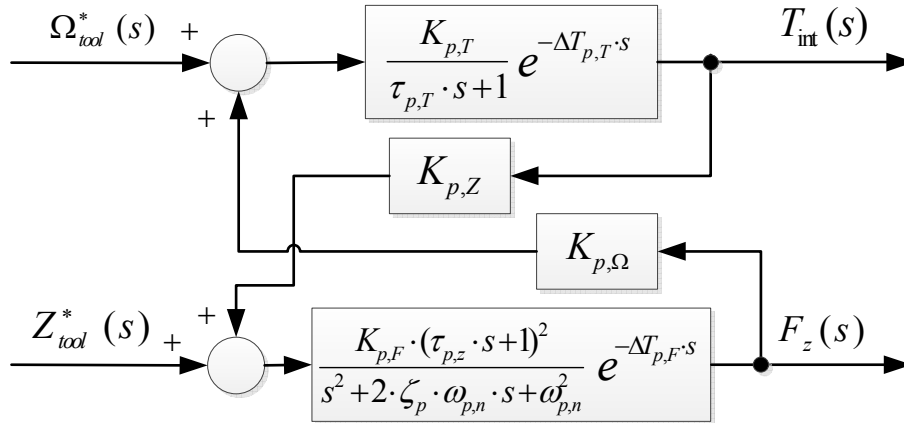


Figure 16: Block diagram of multi-input-multi-output dynamic process model with dynamic cross-coupling.

$$T_{int}(s) = \frac{K_{p,T}}{\tau_{p,T} \cdot s + 1} e^{-\Delta T_{p,T} \cdot s} \cdot (\Omega_{tool}^*(s) + K_{p,\Omega} \cdot F_z(s)) \quad (9)$$

$$F_z(s) = \frac{K_{p,F} \cdot (\tau_{p,z} \cdot s + 1)^2}{s^2 + 2 \cdot \zeta_p \cdot \omega_{p,n} \cdot s + \omega_{p,n}^2} e^{-\Delta T_{p,F} \cdot s} \cdot (Z_{tool}^*(s) + K_{p,z} \cdot T_{int}(s)) \quad (10)$$

The parameters of both the temperature and the force model were estimated empirically for the given setup on the robotic FSW system using step response tests and the System Identification Toolbox in MATLAB™. Assuming linearity enables the use of the principle of superposition for parameter estimation, *i.e.*, one input was held constant while the other was varied in steps, observing both outputs. The estimated model parameters based on experiments are summarized in Table 4.

The process time delays are comparable to each other (400 ms and 300 ms) and are a result of communication delays between the control system and the robot, internal delays in the robot and delays resulting from the wireless transmission of the temperature measurements. It was found that the vertical tool position has a significant effect not only on the axial force but also on the interface temperature ($K_{p,\Omega} = 0.5 \text{ rpm} / \text{N}$), *i.e.*, a change of 1 N in axial force has the same effect on the interface temperature as a variation in spindle speed by 0.5 rpm. On the other hand, the spindle speed affects the interface temperature, but the influence on the axial force is negligible ($K_{p,z} \cong 0 \text{ mm} / ^\circ\text{C}$). This may not be in agreement with previous research [48], [49], but is valid on the given welding machine, setup, workpiece alloy *etc.* combination and within a fairly small range of parameter values.

Table 4: Estimated model parameters.

	Parameter	Estimated Value	Unit
Temperature	$K_{p,T}$	0.04	°C/rpm
	$\tau_{p,T}$	0.04	s
	$\Delta T_{p,T}$	0.4	s
Cross-Coupling	$K_{p,Z}$	0	mm/°C
	$K_{p,\Omega}$	0.5	rpm/N
Force	$K_{p,F} \cdot \omega_{p,n}^{-2}$	500	N/mm
	$\tau_{p,z}$	0.07	s
	ζ_p	0.2	-
	$\omega_{p,n}$	0.7	Hz
	$\Delta T_{p,F}$	0.3	s

Using the parameter estimates found above, the open-loop frequency response plots of the model are generated and are shown in Figure 17. The bandwidth of both processes is similar and both systems experience significant phase lags at higher frequencies due to the time delays, which limit the achievable closed-loop bandwidth. The peak in the magnitude of the force model is due to the underdamped characteristics of the system, also limiting the closed-loop bandwidth.

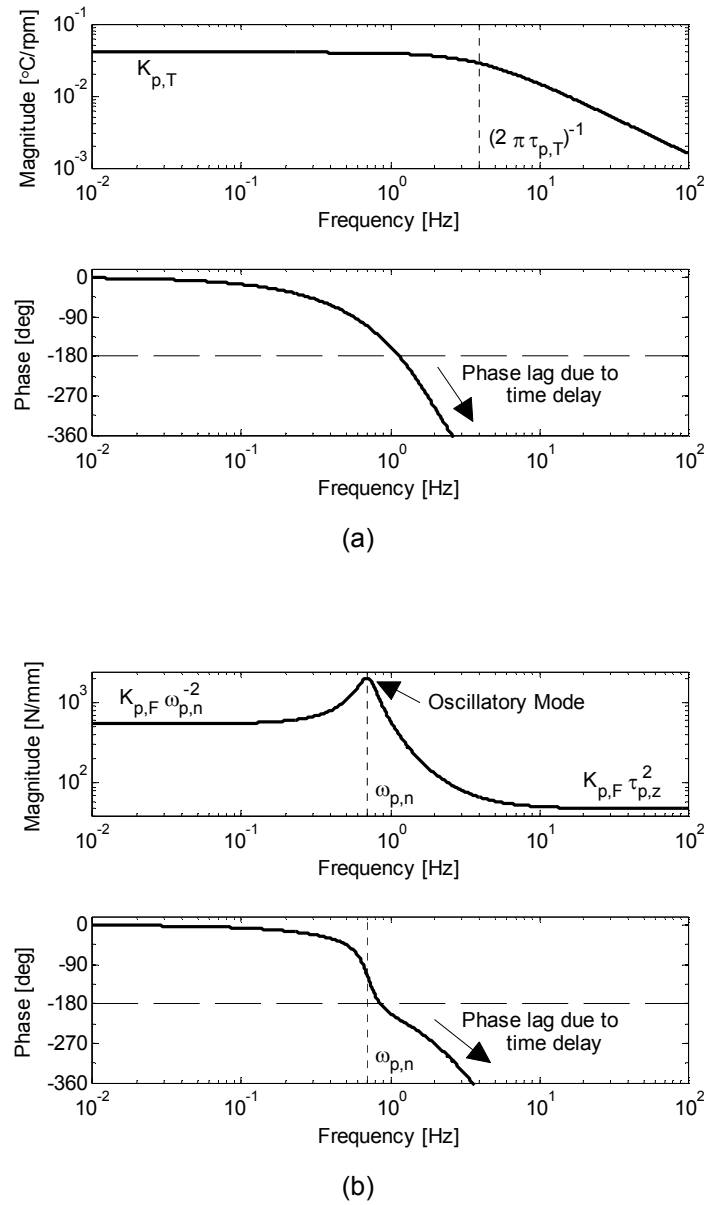


Figure 17: Frequency response plot of open-loop process model of (a) interface temperature relating to commanded spindle speed and (b) axial force relating to commanded vertical tool position.

Chapter 4 Control System Design

4.1. *Controller Design*

As a control strategy, one possible approach is to implement an integral control such that the error between the command input and the process output is reduced or eliminated. The controller design is evaluated in terms of performance requirements like good relative stability (gain and phase margins), adequate damping (to limit oscillations), a reasonably high closed-loop bandwidth (to reduce response time) and a minimal steady state error. An integral controller is given in the form of

$$G_c(s) = \frac{K_c}{s} = \frac{C(s)}{E(s)} \quad (11)$$

where K_c is an adjustable gain, $C(s)$ the generated spindle speed command and $E(s)$ the error between the desired and measured interface temperature. Figure 18 shows the frequency response plots of the uncompensated open-loop system (including only the process model) and the open-loop compensated system (including the process model and the integral controller with a gain of $K_c = 20$). The integral gain K_c was adjusted to achieve an open-loop compensated system cross-over frequency of $\omega_c = 0.22$ Hz, resulting in a closed-loop bandwidth of approximately $\omega_B = 0.32$ Hz (Figure 19). A higher cross-over frequency would require a compensation scheme that could provide significant phase to counter-act the phase delay introduced by the process delay. Given limitations on the magnitude and rate of change of spindle speed of our experimental

setup, this was deemed to be unrealistic. The gain margin of the compensated system is $GM = 19$ dB and the phase margin is $PM = 75^\circ$.

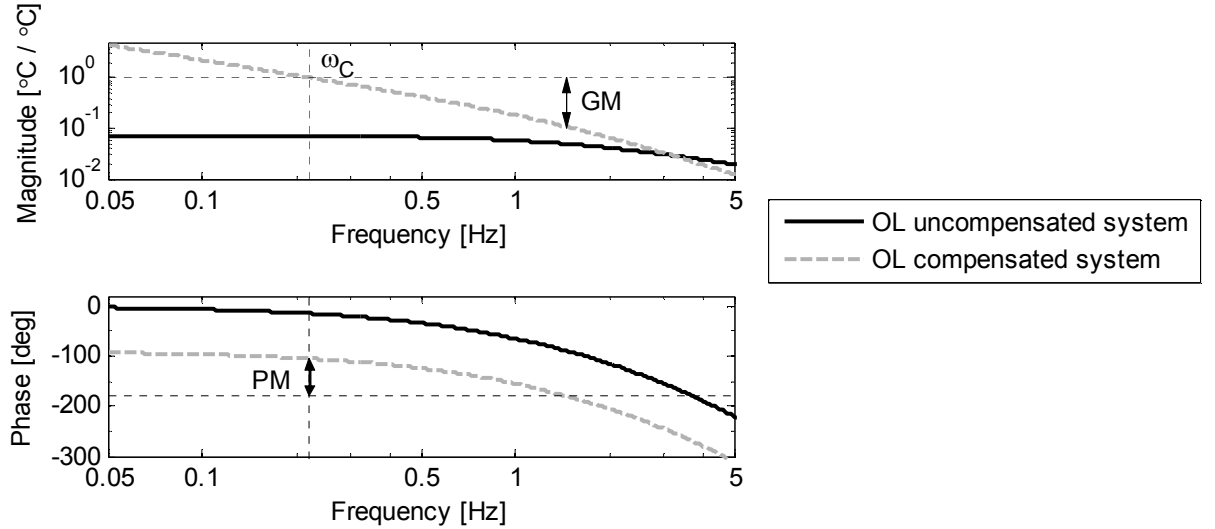


Figure 18: Frequency response plots of open-loop (OL) physical process (uncompensated system) and of OL physical process with integral controller (compensated system). Gain and phase margins are $GM = 19$ dB and $PM = 75^\circ$, respectively. The cross-over frequency of the OL compensated system is $\omega_C = 0.22$ Hz.

The integral controller was mapped to the discrete domain using a Tustin approximation where the sampling time is $T_s = 25$ ms. It generates the following difference equation, utilizing the current and previous error values ($e(k)$ and $e(k-1)$ respectively) and the previous controller output $c(k-1)$

$$c(k) = \frac{K_c \cdot T_s}{2} \cdot [e(k) + e(k-1)] + c(k-1) \quad (12)$$

4.2. Control System Simulation

The closed-loop control system was simulated in MATLAB™ to evaluate its performance. Figure 19 shows the simulated frequency response of the closed-loop

compensated system, indicating the closed-loop bandwidth of approximately $\omega_B = 0.32$ Hz.

In Figure 20, a simulated step response of the closed-loop system is plotted, showing a time constant of $\tau = 0.75$ s, zero steady state error, no overshoot and a 10 - 90 % rise time of $T_{r,1} = 1.1$ s. The control system design thus has adequate performance.

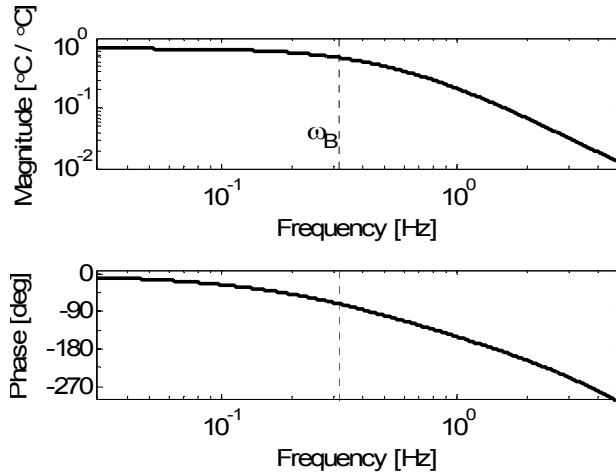


Figure 19: Simulated frequency response plot of closed-loop compensated system, showing bandwidth $\omega_B = 0.32$ Hz.

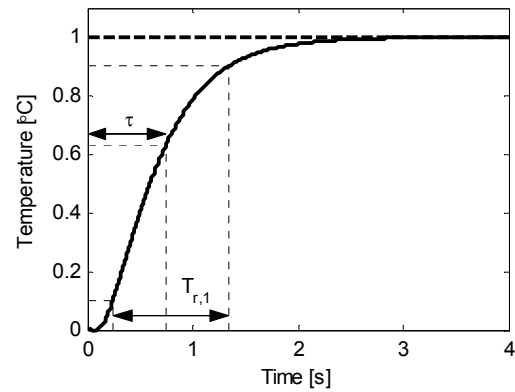


Figure 20: Simulated step response of closed-loop compensated system, showing time constant $\tau = 0.75$ s and 10 - 90 % rise time $T_{r,1} = 1.1$ s.

4.3. Test Setup for Controller Evaluation

To simulate a change in workpiece geometry or properties causing a disturbance to the control system, welds were performed over a set of two different backing plates of significantly different thermal diffusivities. Titanium and copper with dimensions of 102 mm x 76 mm x 12.7 mm (4"x3"x0.5") were selected for their significantly different thermal

diffusivities but similar stiffness (Figure 21). Backing plates of various materials have also been utilized for thermal management during welding in order to improve the hardness of the resulting weld, particularly in the heat affected zone (HAZ) [50], [51].

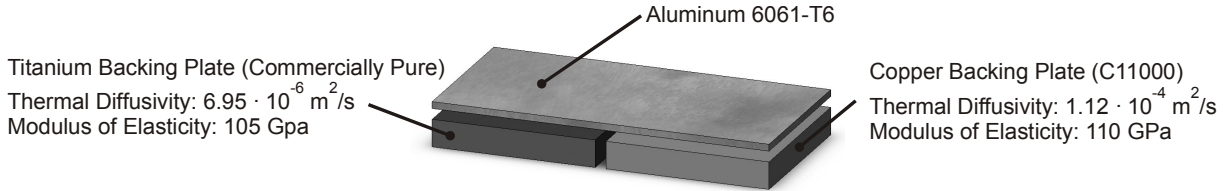


Figure 21: Schematic of different backing plates.

4.4. Controller Design for Combined System on Robotic Setup

Based on the open-loop process model found above in section 3.3, a closed-loop controller is designed in the frequency domain. Two integral controllers are chosen for simplicity and to eliminate any steady-state errors. Each controller is introduced independently assuming no cross-coupling. The temperature controller is discussed first, then the force controller. Finally, both controllers are combined including static decoupling.

Successful closed-loop control of the shoulder interface temperature has been previously implemented on a CNC mill [52]. An integral controller with one gain $K_{c,T}$ was used, which determines spindle speed adjustments. The same measurement and control system is transferred to the robotic FSW testbed in this work. Figure 22 shows the block diagram of the interface temperature control system, for which either the shoulder or pin interface temperature can be used as the feedback signal. The controller gain (20 rpm/°C) was determined utilizing the developed process model,

resulting in a gain margin of 13 dB and a phase margin of 70 degrees (Figure 23 shows magnitude and phase of the compensated open-loop system $T_{\text{int}}(s)/E_T(s)$) and an estimated closed-loop bandwidth of 0.2 Hz (Figure 24 shows magnitude and phase of the compensated closed-loop system $T_{\text{int}}(s)/T_{\text{int}}^*(s)$). This results in a reasonable tradeoff between sufficient stability and good performance.

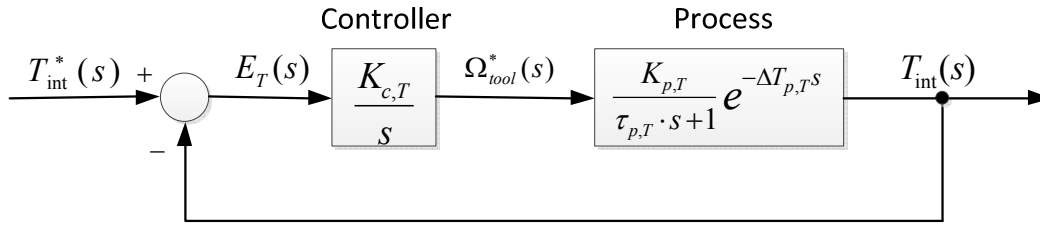


Figure 22: Block diagram of closed-loop interface temperature control system adjusting spindle speed.

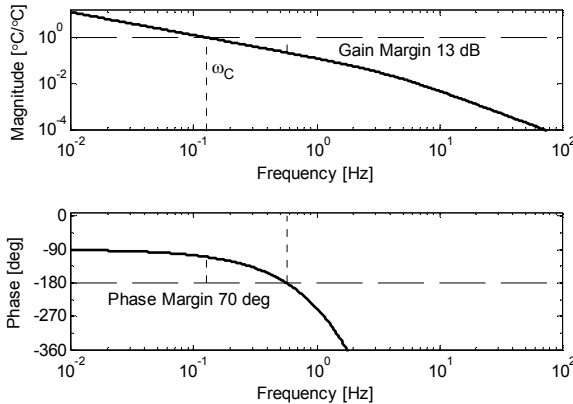


Figure 23: Simulated compensated open-loop frequency response of interface temperature control system.

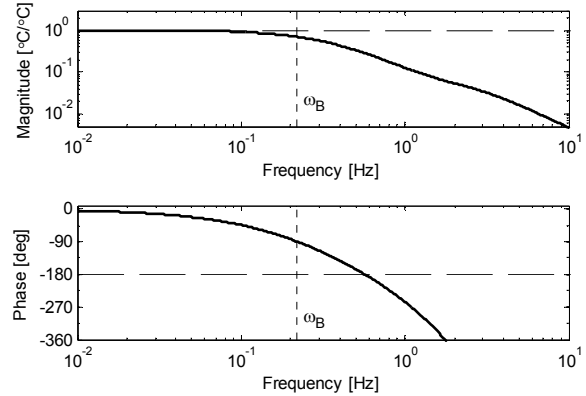


Figure 24: Simulated closed-loop frequency response of interface temperature control system.

Closed-loop control of axial force by vertical tool position adjustments has not been implemented on the previous testbed (CNC mill) in this work due to system limitations (lack of ability to adjust the vertical tool position in real-time), but has been employed previously by other researchers [4], [5], [53], [54]. Figure 25 shows the block

diagram of the axial force control system implemented in this work. The same controller structure was used as in the temperature control system presented above. The controller gain (0.001 mm/N) was determined utilizing the developed process model, resulting in a gain margin of 7 dB, a phase margin of 84 degrees (Figure 26 shows magnitude and phase of the compensated open-loop system $F_z(s)/E_F(s)$) and an estimated closed-loop bandwidth of 0.1 Hz (Figure 27 shows magnitude and phase of the compensated closed-loop system $F_z(s)/F_z^*(s)$).

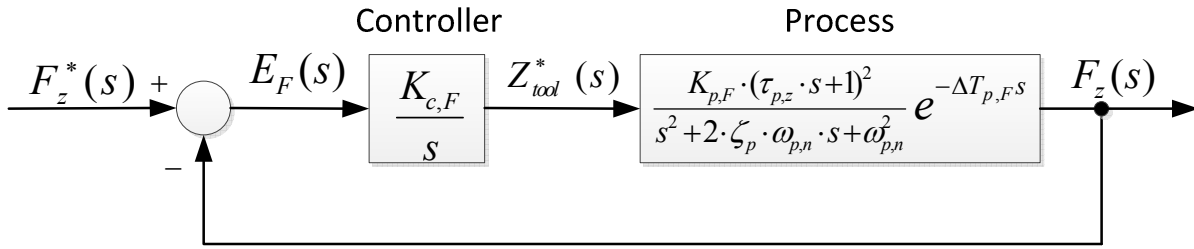


Figure 25: Block diagram of closed-loop axial force control system adjusting vertical tool position.

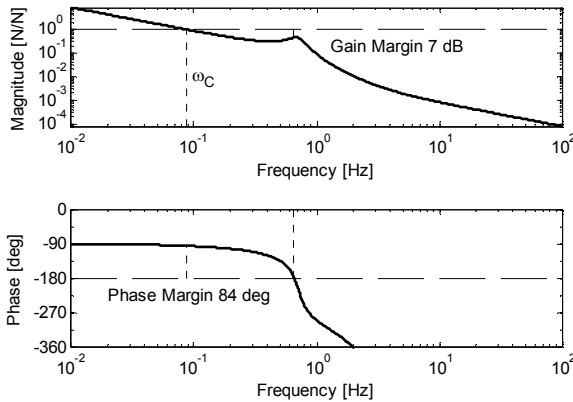


Figure 26: Simulated compensated open-loop frequency response of axial force control system.

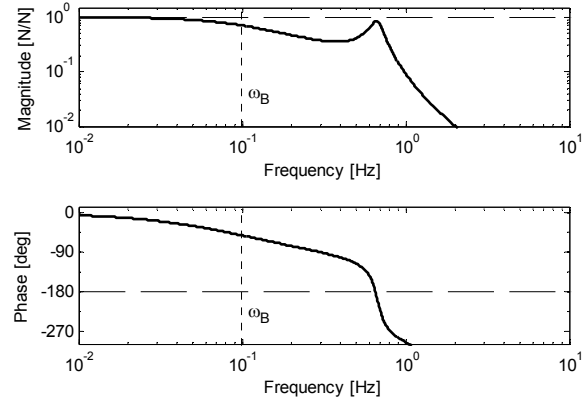


Figure 27: Simulated closed-loop frequency response of axial force control system.

In order to combine the interface temperature and axial force control systems, both controllers as described above are implemented in parallel using the same gains as

found previously. Because the two processes are dynamically cross-coupled as shown in section 3.3, a decoupling structure is added as shown in the block diagram of the overall control system in Figure 28. Only a static decoupling structure is implemented for simplicity. The static decoupling gains used are $\hat{K}_{p,Z} = 0 \text{ mm/rpm}$ and $\hat{K}_{p,\Omega} = 250 \text{ rpm/mm}$. The former gain is chosen to be zero because no significant effect of spindle speed on the axial force was observed. The latter decoupling gain improves the performance of the control system because the undesired effect of the vertical tool position on the interface temperature is nulled through this feedforward element.

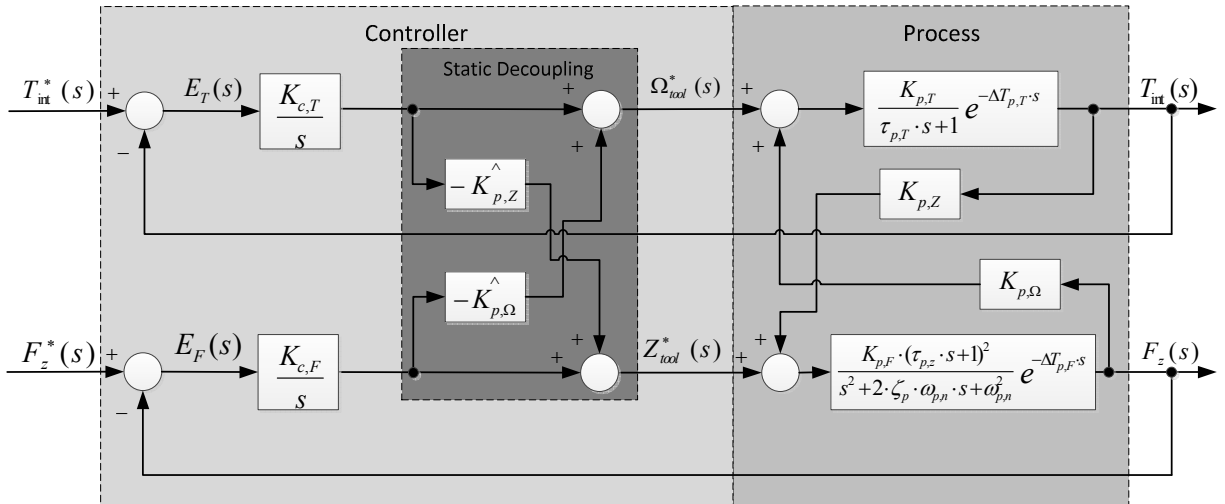


Figure 28: Block diagram of combined closed-loop interface temperature and axial force control system adjusting both spindle speed and vertical tool position.

Chapter 5 Temperature Measurements in Friction Stir Welding

5.1. Temperature Measurements Using Infrared Pyrometers

Unlike thermocouples, non-contact temperature sensors are sensitive to material properties. Due to different thermal emissivities, different materials at identical temperatures may produce different sensor signals. Also, identical materials at the same temperature but with different surface finishes or shapes can yield different temperature readings. An important factor in converting the detector output signal into an accurate temperature is the emissivity of the target object. In general, the lower the emissivity of an object, the harder it is to obtain an accurate temperature measurement using pyrometers. Additionally, the emissivity of a target object to be measured is a function of the material, temperature, geometric shape of the surface, oxidation state, surface finish and roughness, surface coating, angle and wavelength at which the measurement is taken. Other issues that make measurements difficult are external factors like sunlight or a nearby hot object that reflects off the target. The problem is magnified with aluminum alloys, due to their high reflectance.

Single and dual wavelength pyrometers were used to measure workpiece surface temperatures next to the tool in FSW of aluminum. These temperatures are well within the measuring range of the pyrometers. Repeatable measurements could not be

achieved so far as demonstrated in Figure 29. Temperatures are fluctuating at a lower frequency and also oscillating at a higher frequency (with ‘beats’) in some cases. To date, in experiments using aluminum alloys 5083-H111 and 6061-T6, repeatable measurements could not be achieved because other radiation sources (hot objects close to the target, *i.e.*, the FSW tool) impact the temperature measurement through specular and diffuse reflection from the workpiece surface at the wavelengths that were investigated. The measurement is complicated by the low and non-constant emissivity and high reflectivity of as-received aluminum surfaces as well as the fact that the workpiece and tool surfaces near the weld zone have similar temperature magnitudes.

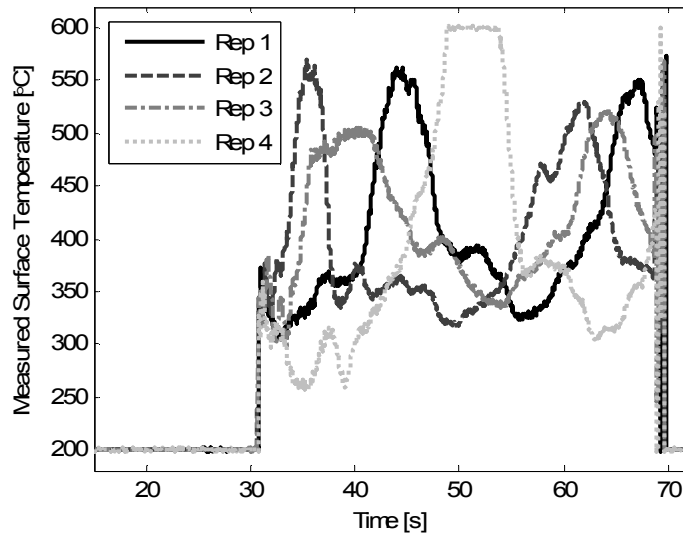


Figure 29: Measured workpiece surface temperatures of four welds at same conditions using the dual wavelength pyrometer (6061-T6).

Certain parameters (tool rotation speed, type of fixture) could reduce and eliminate the occurrence of ‘beats’ during online FSW tests. However, the temperature measurement of the highly reflective aluminum still fluctuates and thus a signal obtained in this fashion is not suitable for the use in closed-loop control at this point.

It was concluded that the use of the single and dual wavelength pyrometers was not feasible under the conditions tested for the aluminum workpieces. It might however, be feasible to employ the pyrometers during welding of other materials with higher emissivities, e.g., oxidized steel or bronze castings.

5.2. Tool-Workpiece Interface Temperature Measurements Using Thermocouples

This section demonstrates the efficacy of the developed tool-workpiece interface temperature measurement system during FSW experiments of several aluminum alloys for varying spindle speeds, travel speeds and plunge depths. It is also shown how the temperature measurements provide a better fundamental understanding of the FSW process.

5.2.1. Introduction

The wireless temperature DAQ system was applied in FSW on the described experimental setup. Figure 30 shows the measured interface temperature, axial welding force and spindle power over the course of a whole using tool A (Table 3). 0.7 s after the FSW tool pin first contacts the workpiece (a), the temperature at the measurement location increases. This delay is due to the fact that at first, heat is only generated at the pin, which needs to conduct to the thermocouple location on the shoulder. When the shoulder first makes contact with the workpiece, the temperature profile demonstrates an inflection point (b). The maximum axial force and spindle power is seen when the

tool is fully plunged (c). After this, a 3 s dwell is performed before the traverse starts (d). The maximum interface temperature is 572 °C, 98.8 % of the workpiece's solidus temperature T_{sol} (582 °C [55]) just before the weld finishes and the tool retracts (e). The average interface temperature is $T_{avg} = 561$ °C (97.6 % of T_{sol}). The average axial force during the traverse is $F_{z,avg} = 7.34$ kN and the average spindle power during the traverse is $Q_{z,avg} = 1.79$ kW. All average values are reported for the middle 75 % of the weld traverse. The variation across the averaged region is ± 7 °C, ± 272 N and ± 17 W, respectively.

Because the friction stir welds in this study are only 180 mm long (limited by the size of the dynamometer) it is known that the entire system does not reach a thermal steady state because of the large thermal masses involved. The measured interface temperature increases by approximately 8 °C over the course of the 180 mm long weld. However, there could be situations in which the weld zone temperature reached a constant value whereas tool temperatures are still increasing. Whether or not this was the case in the experiments presented here is not known. It is known that adding a cooling system to the tool or increasing the weld length can facilitate the achievement of a steady state condition [56]. In this study the same weld length and initial conditions are used; therefore, while the welds may not have achieved a steady-state, they are directly comparable to each other.

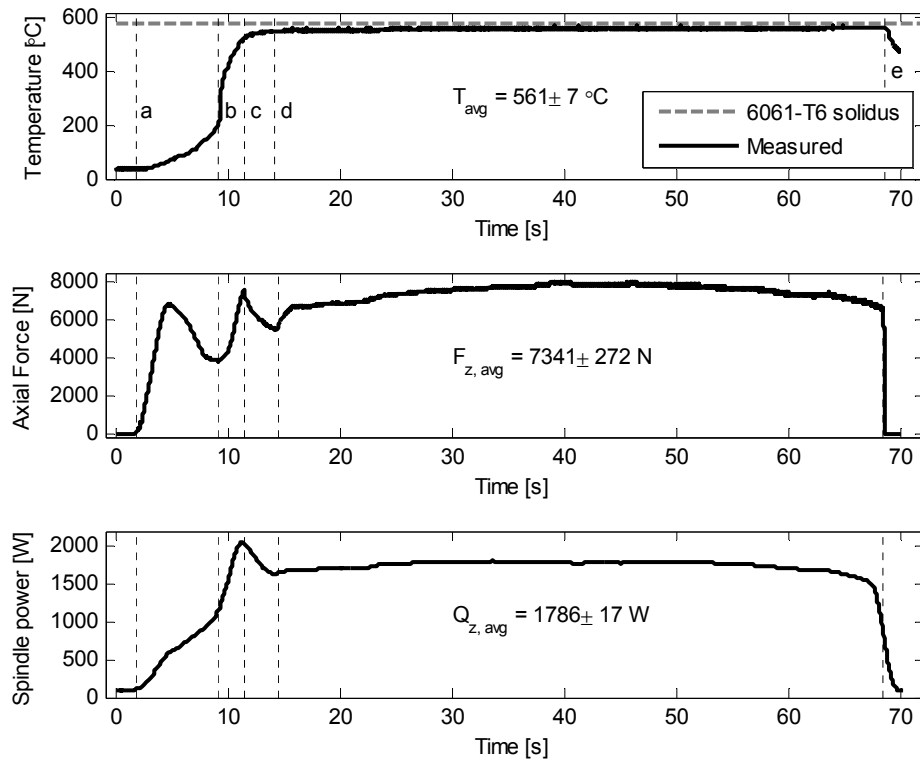


Figure 30: Measured interface temperature, axial welding force and spindle power vs. time for a weld under nominal conditions. a) Pin first contacts, b) Shoulder first contacts, c) Plunge finished, 3 s dwell starts, d) Dwell finished, traverse starts, e) Traverse finished, tool retracts. The average temperature T_{avg} , force $F_{z, avg}$ and power $Q_{z, avg}$ are shown for the middle 75 % of the weld traverse.

Figure 31 shows the average interface temperature and average spindle power during the middle 75 % of the weld traverse for spindle speeds ranging from 700 rpm to 1500 rpm, constant travel speed of 200 mm/min and constant plunge depth of 3.2 mm. The temperature is steadily increasing over the whole range of tested spindle speeds, which is an important requirement for the use in closed-loop control. The chosen spindle speeds cause a variation in interface temperature by more than 55 °C. For 1500 rpm, the average temperature is slightly above the solidus temperature, which suggests local melting of aluminum at the tool-workpiece interface.

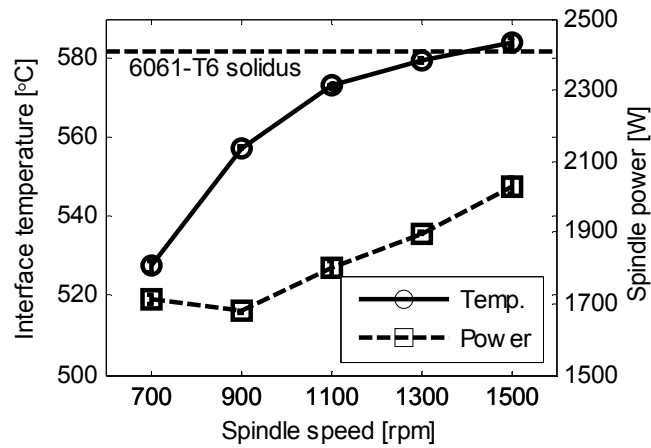
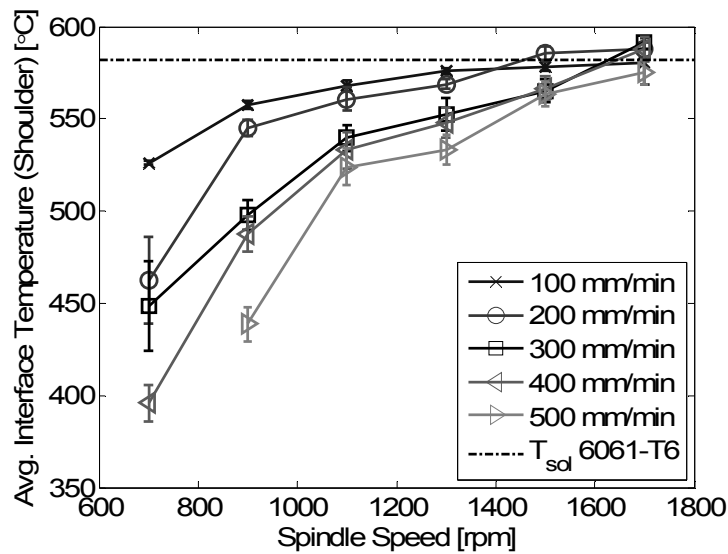


Figure 31: Average interface temperature during weld traverse vs. spindle speed. For all tests the travel speed was 200 mm/min and the plunge depth was 3.2 mm.

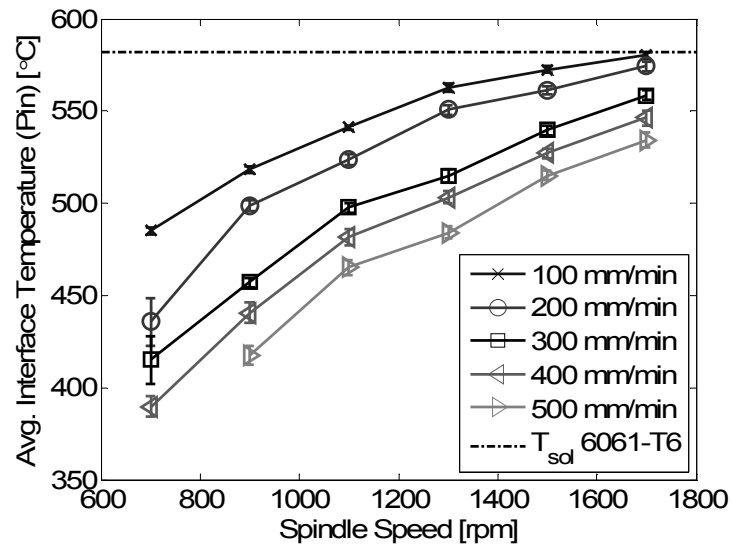
5.2.2. Average Interface Temperatures

Figure 32 shows the average temperatures experienced at the tool-workpiece interface when varying the spindle speed and travel speed, using tool B (Table 3). It can be seen that both the shoulder and pin temperatures increase for higher spindle speeds due to greater heat generation per distance traveled by increased friction and plastic deformation. The temperatures also increase for lower travel speeds due to more heat being deposited per unit weld length. By varying the spindle speed and travel speed in the given tests, the average shoulder temperature varies from 395 °C to 591 °C and the average pin temperature from 389 °C to 580 °C. For all cases tested, the shoulder temperature is greater than or equal to the pin temperature. This is in agreement with the theory that the majority of heat is generated at the tool-shoulder interface. The measured temperatures approach the solidus temperature of 6061-T6 (582 °C) for higher heat inputs (higher spindle speeds and lower travel speeds) and in a few cases

the shoulder temperature is above the solidus temperature, which suggests localized melting at the tool-workpiece interface. Evidence of localized melting has also been reported by Yang *et al.* [57]. The approach of the average shoulder interface temperature toward the solidus temperature as seen in Figure 32 supports the self-limiting theory during FSW [58], [59], *i.e.*, for higher temperatures, the metal flow stress decreases, resulting in lower spindle torque, reduced friction at the tool-workpiece interface and reduced heat generation. However, Figure 32 also shows that it is possible to exceed the solidus temperature at the tool-workpiece interface. The data also shows that for the parameter window chosen, varying the spindle speed results in a larger variation in interface temperature than varying the travel speed, which is an important result for developing closed-loop temperature control for FSW. Figure 33 shows how higher temperatures are associated with higher spindle powers due to more heat generation.



(a)



(b)

Figure 32: Average temperatures during weld traverse at (a) shoulder and (b) pin interface for various spindle speeds and travel speeds for 6061-T6.

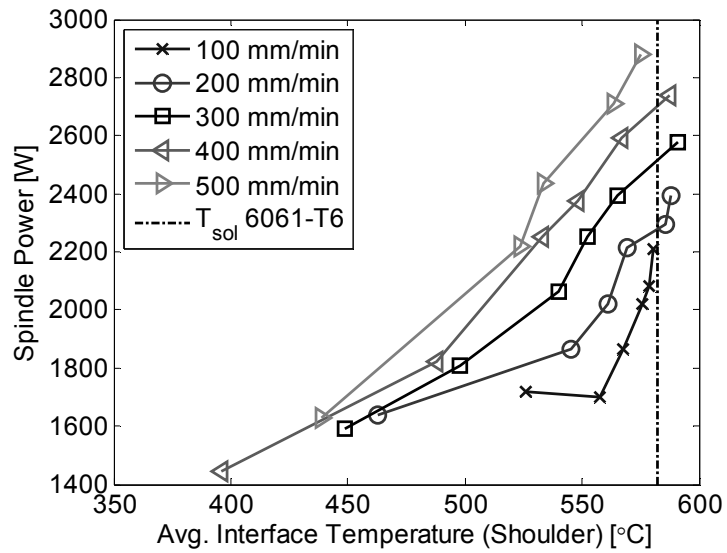


Figure 33: Average spindle power during weld traverse for various average shoulder interface temperatures (*i.e.*, spindle speeds) and travel speeds.

5.2.3. Interface Temperature Dynamics

Figure 35 displays the measured interface temperatures of one of the welds performed using tool B (Table 3). It can be seen that the pin temperature rises first, due to the pin contacting the workpiece prior to the shoulder during the tool plunge. While the tool traverses along the joint line, the average temperatures did not increase significantly (quasi steady-state) and as the tool retracts from the workpiece, the tool temperature cools down.

The temperature measurement approach chosen in this work captures the dynamics of the process very well, because the thermocouple sheaths are in direct contact with the aluminum workpiece at the tool-workpiece interface. The measured temperatures are not constant, but rather oscillating as the tool traverses under constant operating conditions (Figure 35). The frequency of these oscillations is found to match the frequency of the spindle rotation, *i.e.*, the thermocouple is capturing temperature variations within 360 degrees of tool rotation [52]. For the various spindle speeds used in this study and a sample rate of 250 Hz, the system is capturing 8.8 to 21 temperature measurements per rotation of the tool (angular resolution of 17 to 41 degrees, Figure 34).

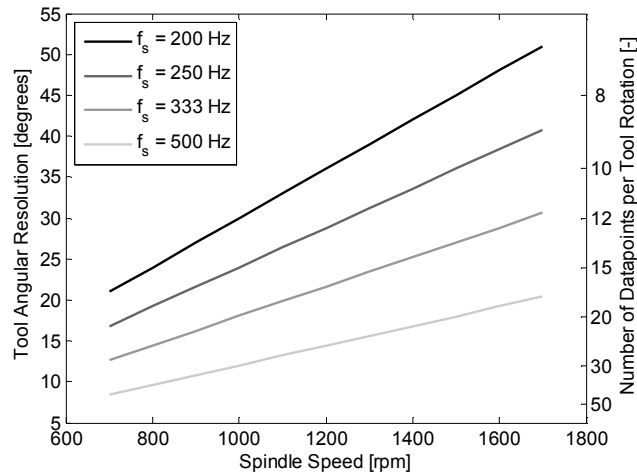


Figure 34: Angular resolution of temperature measurements for various sampling rates and spindle speeds.

There are several potential explanations for the observed interface temperature oscillations. The thermocouple tips are located off centered with respect to the tool rotation axis and in direct contact with the workpiece material. The 3 degree travel angle, the weld traverse motion and the asymmetric nature of FSW (differences between advancing and retreating side) cause a varying temperature field under the tool. A nonzero travel angle can cause uneven pressure distributions under the tool, resulting in differences in heat generation and thus temperature under the shoulder. When the tool is traversing across the workpiece, lower temperatures are experienced ahead of the tool than behind the tool because colder (unwelded) material enters the weld zone ahead of the tool. Both experimental and analytical work reported higher temperatures on the advancing side as compared to the retreating side of the weld due to higher shearing on the advancing side [60–64].

Figure 36 shows the amplitude (in this work, amplitude always refers to the 'half the peak-to-peak amplitude') of the measured interface temperatures at the frequency of the spindle rotation over time (window size of 100 samples, *i.e.*, 0.4 s) using Fast Fourier transforms (FFT). It can be seen that the amplitude at the shoulder interface is sharply increasing towards the end of the plunge when the shoulder starts contacting the workpiece (large temperature variations under the tool shoulder solely due to nonzero travel angle). The temperature amplitude then decreases to almost zero during the dwell period of 3 s (material well mixed and hence temperature distribution nearly uniform) and increases again during the weld traverse (temperature differences due to weld traverse and process asymmetry). The temperature amplitude at the pin interface is rather uniform during the plunge, goes to zero during the dwell period and then reaches a value of less than half of the amplitude at the shoulder interface during the weld traverse.

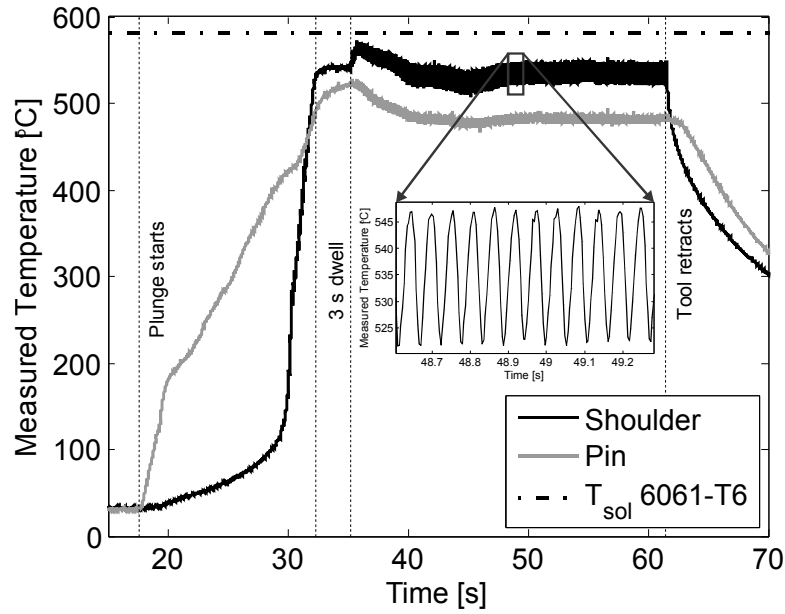


Figure 35: Measured temperatures for shoulder and pin location during welding of 6061-T6 (solidus temperature 582 °C) at 1100 rpm and 400 mm/min.

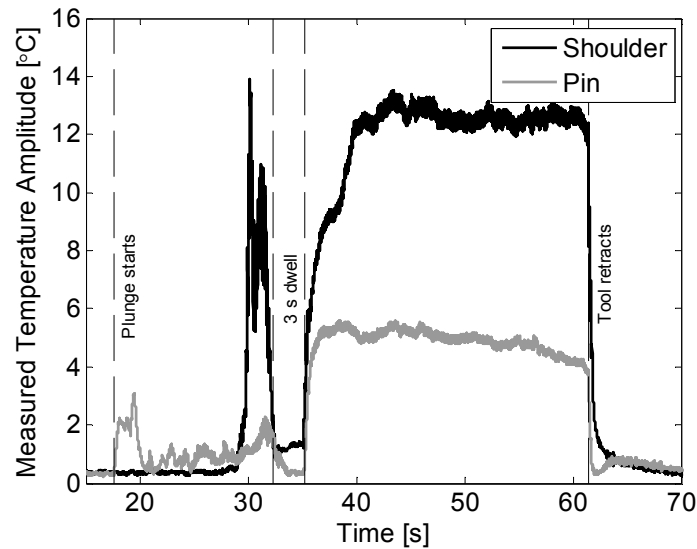


Figure 36: Measured temperature amplitudes obtained from FFT over time during welding of 6061-T6 at 1100 rpm and 400 mm/min. A moving window of 100 samples (0.4 s or 7.3 tool rotations) was applied.

Due to the thermocouple's limited response time and inherent delays due to physical heat conduction, the temperature response is experiencing attenuation in magnitude and a phase lag (spindle frequency is well beyond the thermocouple's

bandwidth). A sensor calibration method was developed to correct for this issue and determine the actual tool-workpiece interface's temperature history [41], [65]. The calibration method is based on the ASTM E-1461 standard to measure the thermal diffusivity of a substance. The thermocouples embedded into the tool were subjected to very short pulses of heat flux provided by a laser. The thermal response of the thermocouples was recorded with respect to the applied heat flux and diffusive time constants were determined that are used in this work to calculate the interface temperatures.

Using the thermocouple measurements, the angular position data from the Hall effect sensor and the magnitude and phase information from the sensor calibration, the true temperature distribution under the shoulder and around the pin can be estimated. Both the measured (uncorrected) temperature data and the interface (corrected) temperature data are shown in Figure 37. The data shows that the differences due to the thermocouple response limitations are significant: for a spindle speed of 1300 rpm, the phase lag at the shoulder is 49 degrees (pin: 91 degrees) and the attenuation factor is 0.43 (pin: 0.20). *I.e.*, even though a <0.25-mm-diameter thermocouple bead was located within 250 μm of the tool surface, the amplitude of the measured temperature oscillations were as small as 20% of the magnitude occurring at the tool-workpiece interface. Using these corrections it was found that the highest temperatures are experienced between the advancing side and the trailing edge of the tool (approximately 60 degrees from the trailing edge toward the advancing side as seen in Figure 37). On the advancing side the velocity gradients are highest, resulting in higher strain rates and

more heat generation than on the retreating side. In addition, colder workpiece material coming in from the leading edge (advection heat transfer) is transported further to the retreating side than to the advancing side due to the counterclockwise rotation of the tool and the resulting flow pattern [64]. In Figure 37 it can be seen that there is a phase difference of approximately 20 degrees in the measured (uncorrected) temperature obtained with the sensor at the shoulder vs. at the pin location. Since the response characteristics are different for the two sensors (Table 2), the corrected data does not show this phase difference anymore: the time constant for the thermocouple at the shoulder location is shorter.

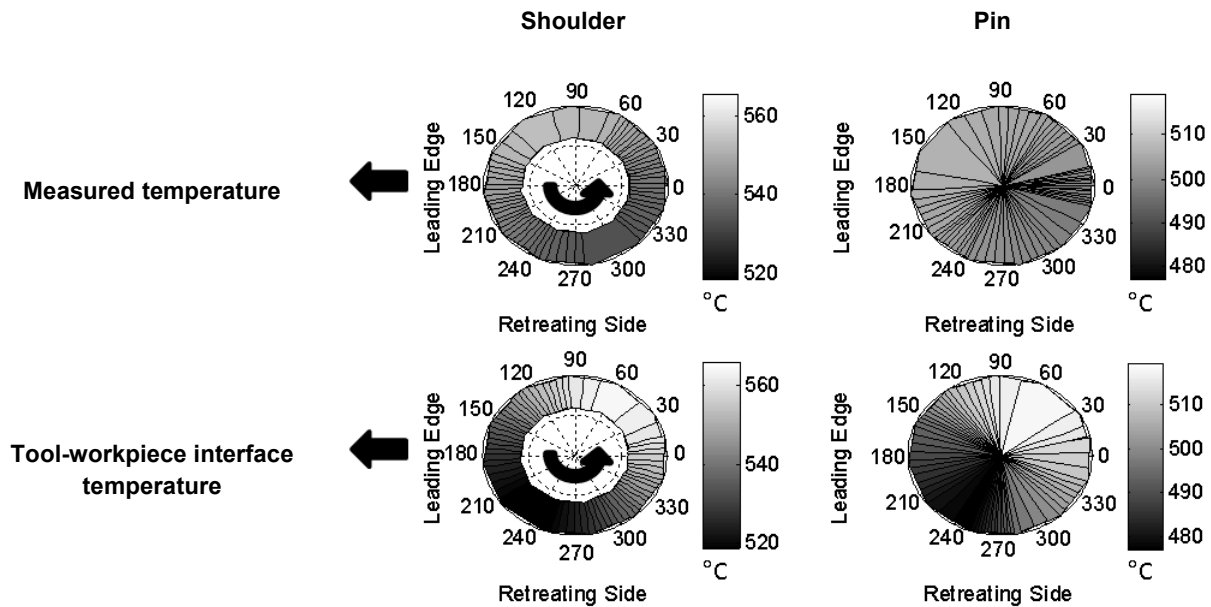


Figure 37: Measured temperature [°C] and calculated tool-workpiece interface temperature distributions at the shoulder and pin (1300 rpm, 400 mm/min, 6061-T6).

As seen in Figure 36 and in Figure 38 for various plunge depths, the as-measured temperature amplitudes are higher at the shoulder interface than at the pin interface. The thermocouple at the shoulder is located further away from the tool

rotation axis than the pin thermocouple, thus capturing a larger temperature variation. However, when correcting for the magnitude attenuation based on the developed heat transfer calibration [41], [65], the temperature amplitude at the pin is higher than at the shoulder interface for the two highest plunge depths (Figure 38). At the shoulder interface, higher plunge depths result in lower interface temperature amplitudes, *i.e.*, smaller temperature variations between the advancing and retreating side, and leading and trailing edge, hence a more uniform temperature distribution under the shoulder. Low plunge depths result in insufficient engagement of shoulder and workpiece, especially on the leading edge due to the travel angle, resulting in less heat generation there, causing a larger temperature difference. Higher plunge depths result in a more uniform shoulder engagement on leading and trailing edge, *i.e.*, a smaller temperature difference. At the pin interface, higher plunge depths result in higher interface temperature amplitudes, *i.e.*, higher temperature variations between the advancing and retreating side, and leading and trailing edge, hence a less uniform temperature distribution around the pin. A possible explanation is: for all the plunge depths shown here, the pin is fully engaged into the workpieces, *i.e.*, the major change in average temperature results from the contribution of the shoulder. The thermal diffusivity of the workpieces decreases with increasing temperature [66], caused by higher plunge depths (Figure 39), which results in less heat removal and a higher temperature difference, *i.e.*, a larger temperature amplitude at the pin interface. The lowest plunge depth (4.6 mm) is a special case, in which there was insufficient contact between shoulder and workpiece, creating an unacceptable weld.

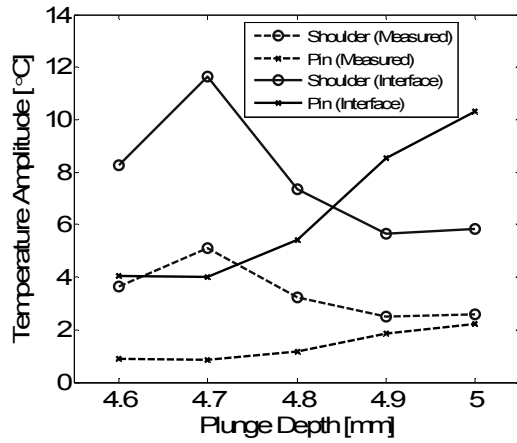


Figure 38: Amplitudes of measured interface temperature for different plunge depths. Constants: 1200 rpm, 200 mm/min, 6061-T6.

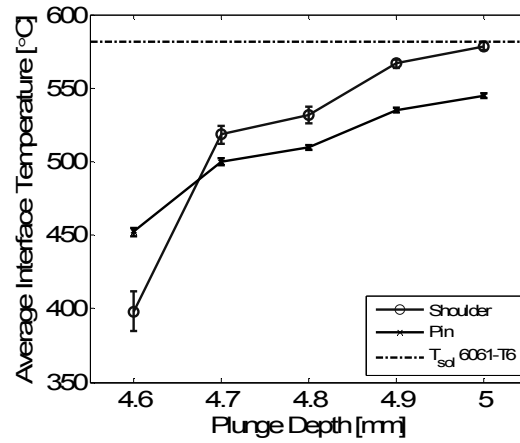


Figure 39: Average measured interface temperatures for different plunge depths. Constants: 1200 rpm, 200 mm/min, 6061-T6.

Figure 40 and Figure 41 show how the temperature amplitudes at the shoulder and pin interface, respectively, are strongly dependent on the travel speed and varies between 3 °C and 29 °C (corrected values). Higher travel speeds cause higher advection heat transfer and hence a higher asymmetry between advancing and retreating side due to the flow pattern described above. This is reflected in the larger amplitudes being measured. This phenomenon was also observed experimentally and from a numerical model by Simar *et al.* [62]. The spindle speed does not show any discernable effect on the interface temperature amplitudes.

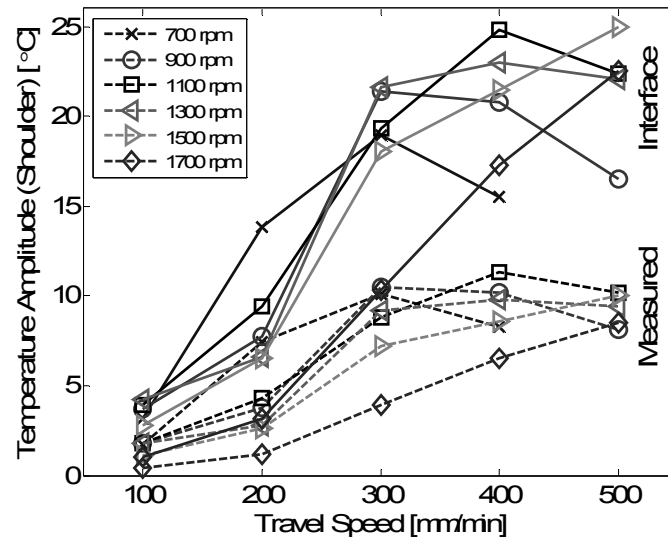


Figure 40: Amplitudes of interface temperature at shoulder interface during the weld traverse (6061-T6) for various spindle speeds and travel speeds. Plunge depth 4.9 mm (constant).

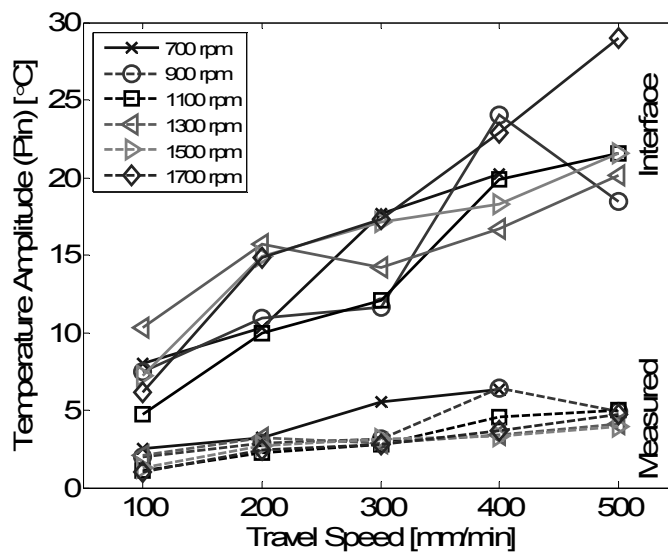


Figure 41: Amplitudes of interface temperature at pin interface during the weld traverse (6061-T6) for various spindle speeds and travel speeds. Plunge depth 4.9 mm (constant).

5.2.4. Comparing Different Aluminum Alloys

In order to investigate the effect of different workpiece thermal properties on the measured interface temperatures, welds were performed on aluminum alloys 2024-T3,

5083-H116, 6061-T6 and 7075-T6. To directly compare the four alloys, all were bead-on-plate welds and were welded with the same tool, under the same welding parameters of 1000 rpm spindle speed, 150 mm/min travel speed and 4.9 mm plunge depth. The authors realize that these parameters will not result in an optimal weld quality for each alloy. No weld showed any detrimental flaws, *i.e.*, surface voids. The average interface temperatures during the weld traverse vary significantly and increase for higher spindle power due to higher heat input (Figure 42). This emphasizes how different aluminum alloys require different welding parameters to maintain desirable weld temperatures. Figure 42 also shows how for most alloys, the tool shoulder-workpiece interface temperature is just below the solidus temperature [55]. The average shoulder interface temperature is $0.975 T_{\text{sol}}$, $0.974 T_{\text{sol}}$ and $0.980 T_{\text{sol}}$ for alloys 2024-T3, 5083-H116 and 6061-T6, respectively. However, for alloy 7075-T6 the average shoulder and pin interface temperatures were more than 25 °C above the solidus temperature ($1.04 T_{\text{sol}}$). This is in agreement with published work that recommends lower spindle speeds, smaller shoulder diameters, or lower shoulder rotation rate (as compared with pin rotation rate) for this alloy to reduce the heat input and limit weld temperatures [2].

The tool-workpiece interface temperature amplitudes for the four aluminum alloys investigated are shown in Figure 43. In order to explain the differences observed, the amplitudes are plotted over the approximate thermal diffusivity of each alloy. The thermal diffusivities are temperature dependent and are shown for elevated temperatures (all alloys between 500°C to 545 °C) [66]. For alloy 2024-T3 the thermal diffusivity of a different temper, T4, is shown due to lack of data for the T3 condition.

Also, for alloy 5083-H116, the thermal diffusivity of alloy 5182 was used, since this alloy is very similar in composition and the needed data could be found in the open literature. An increasing thermal diffusivity causes lower temperature amplitudes, *i.e.*, smaller temperature differences between the angular location of highest and lowest temperatures measured with the thermocouples. This is expected, since a material with a higher thermal diffusivity is able to conduct thermal energy more quickly relative to its volumetric heat capacity, causing smaller temperature gradients at the interface.

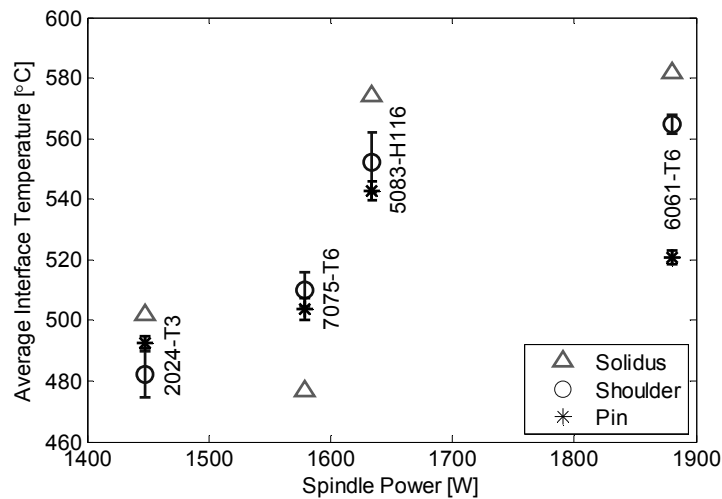


Figure 42: Average interface temperatures vs. spindle power for various aluminum alloys, welded at 1000 rpm spindle speed, 150 mm/min travel speed and 4.9 mm plunge depth.

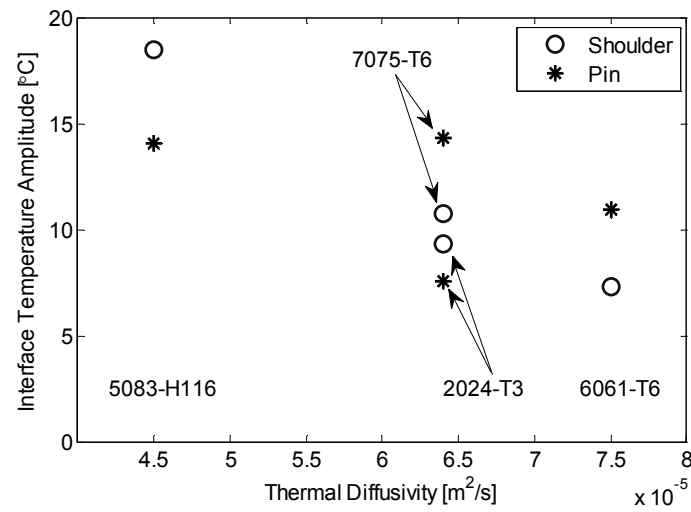


Figure 43: Interface temperature amplitudes (corrected) vs. thermal diffusivity of workpiece for various aluminum alloys, welded at 1000 rpm spindle speed, 150 mm/min travel speed and 4.9 mm plunge depth.

Chapter 6 Closed-Loop Process Control

6.1. Evaluation of Temperature Control System

6.1.1. Command Tracking Performance of Temperature Controller

To evaluate the feasibility of manipulating spindle speed in order to control the shoulder-workpiece interface temperature the integral controller described above was implemented in LabVIEW™ software (National Instruments). For evaluation of the simple integral controller on the real FSW system, steps in desired temperature were commanded during bead-on-plate welding of 6061-T6 on a nominal low carbon steel backing plate. The spindle speed commands were limited between 700 rpm and 1200 rpm in this test to maintain the inputs to the mill in a reasonable, safe range. Figure 44 shows the desired temperature in step changes and the measured temperature response. The spindle speed commands generated by the controller are also plotted. It can be seen that the measured temperature follows the desired temperature fairly well. The time constant of the temperature response is approximately 0.8 s and matches the result found from the simulated step response of the closed-loop system very well ($\tau = 0.75$ s, Figure 20). Towards the end of the weld when the spindle speed was saturated at 1200 rpm, a small amount of steady state error can be noticed.

Another test for evaluating the performance of the controller was performed by commanding a desired temperature in the form of a sine wave. The frequency of the

desired temperature waveform was 0.1 Hz, the amplitude 10 °C. The spindle speed commands were limited between 700 and 1500 rpm in this case. The desired and measured temperatures are shown in Figure 45, along with the spindle speed commands. The measured temperature tracks the desired temperature reasonably well with an amplitude of 8.8 °C and a phase lag of approximately 30 degrees.

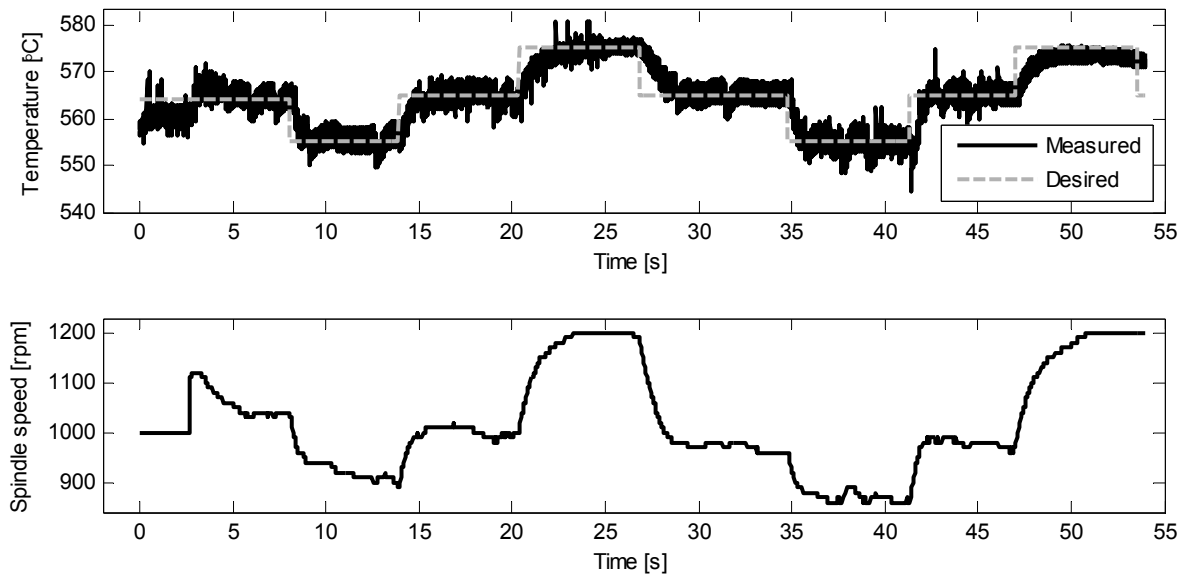


Figure 44: Step commands in desired interface temperature, measured interface temperature and commanded spindle speed using integral compensation.

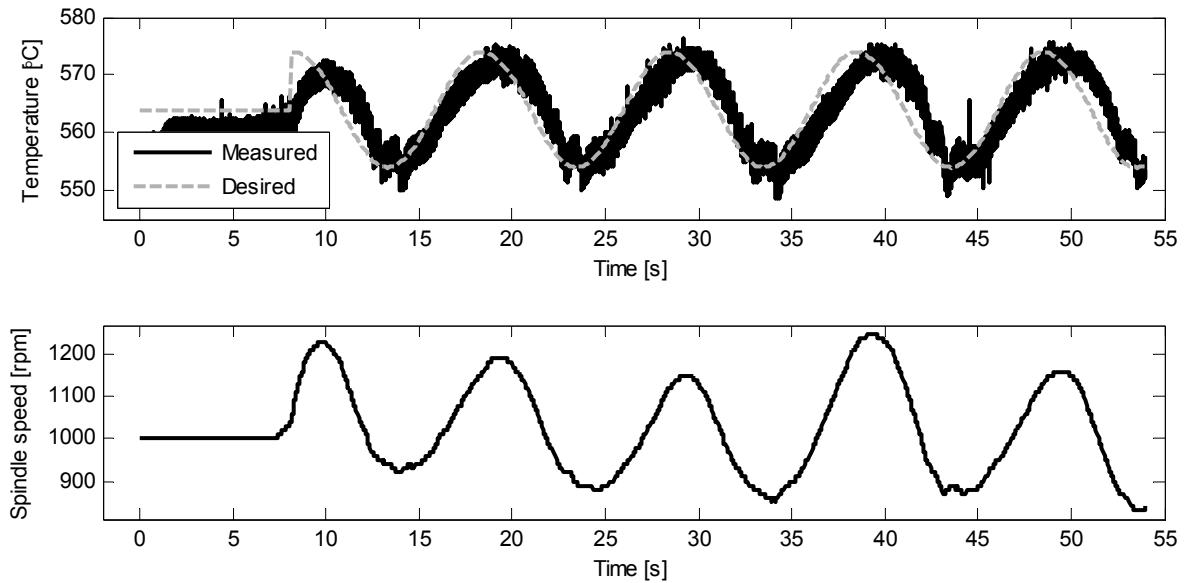


Figure 45: Sinusoidal commands in desired interface temperature, measured interface temperature and commanded spindle speed using integral compensation.

The closed-loop transfer function used for the simulation of the control system in section 4.2 was experimentally verified by comparing the step responses shown in Figure 44 with the simulated step response of the same magnitude. Figure 46 shows this overlaid plot. It can be concluded that the simulation matches well with the experimental step response data. Another way of verifying the closed-loop transfer function was to perform more welds with sinusoidal commands of various frequencies in desired interface temperature as in Figure 45 and overlaying the magnitude and phase with the simulation as illustrated in Figure 47. Again, the experimental and simulated data matches fairly well.

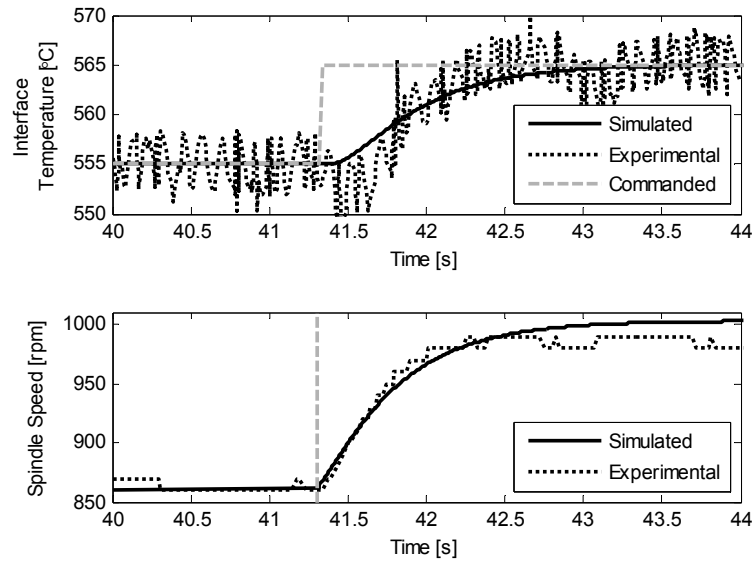


Figure 46: Simulated and experimental step response of closed-loop compensated system.

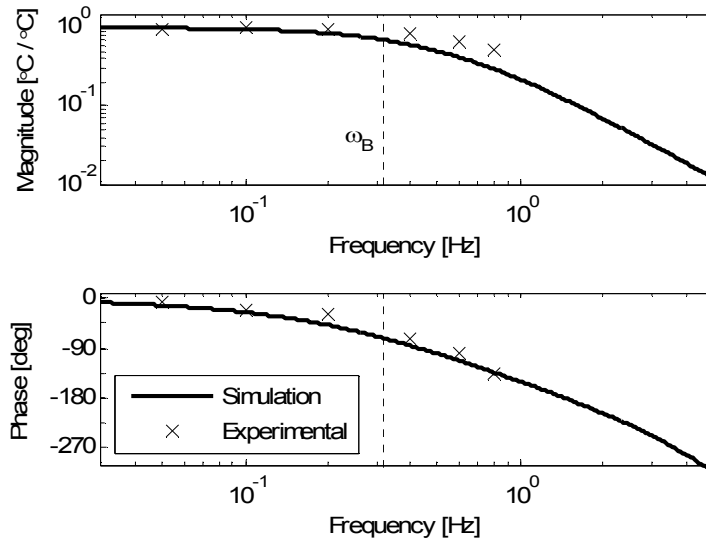


Figure 47: Simulated and experimental frequency response plot of closed-loop compensated system.

6.1.2. Performance of Temperature Controller Under Varying Thermal Boundary Conditions

Welding over the different backing plates affects welding forces, spindle torque and measured temperatures. Traverse and axial forces as well as spindle torque increase in magnitude over the copper plate compared to a titanium backing plate. The measured interface temperature decreases by 50 °C when passing the backing plate interface (Figure 48). These observations can be explained by the fact that, due to the higher thermal diffusivity of copper, more heat is conducted out of the weld zone over the copper backing plate as compared to the titanium backing plate. This leads to a colder weld over copper than over titanium for the same weld parameters, causing higher welding forces and a larger spindle torque [12].

The closed-loop controller described above was also applied to the experimental setup that uses two different backing plates to affect heat dissipation from the weld (Figure 21). The desired interface temperature was set to 560 °C throughout the entire weld traverse. The drop in weld temperature when translating from a titanium to a copper backing plate (*i.e.*, increased heat dissipation) is sensed by the thermocouple and compensated for by the controller through an increase of the spindle speed (800 rpm to 1300 rpm). Figure 48 shows that the measured temperature drops briefly by less than 20 °C (compared to about 50 °C when no temperature control is applied), but after that it is held relatively constant (± 5 °C) with the use of the controller. The required spindle power and the axial force are both much higher in magnitude over the copper

backing plate than over the titanium backing plate, due to the lower weld temperatures over the copper backing plate resulting from higher heat dissipation. The spindle power over the copper backing plate is approximately 500 W lower in the controlled case than in the uncontrolled case, due to the reduced torque resulting from a higher temperature. The use of the controller has only a minor effect on the axial force.

This setup simulates the condition of a weld getting too cold due to changing boundary conditions. Another possible scenario is welding into an area with reduced thermal diffusivity such as an area of reduced cross-section or welding towards the edge or corner of a workpiece. To simulate this situation, the backing plates are reversed, so that the weld starts on a copper plate and finishes on a titanium plate (Figure 49). Again, the temperature set point is 560 °C. The controller commands a higher initial spindle speed to accommodate for the heat losses over the copper backing plate and then reduces the spindle speed to limit heat input over the titanium backing plate. The result is an interface temperature that stays within ± 5 °C of the desired temperature. The exception is a brief increase of less than 10 °C shortly after crossing the backing plate interface. The spindle power over the titanium backing plate is higher in the controlled than in the uncontrolled scenario, due to higher welding torque resulting from a lower weld temperature.

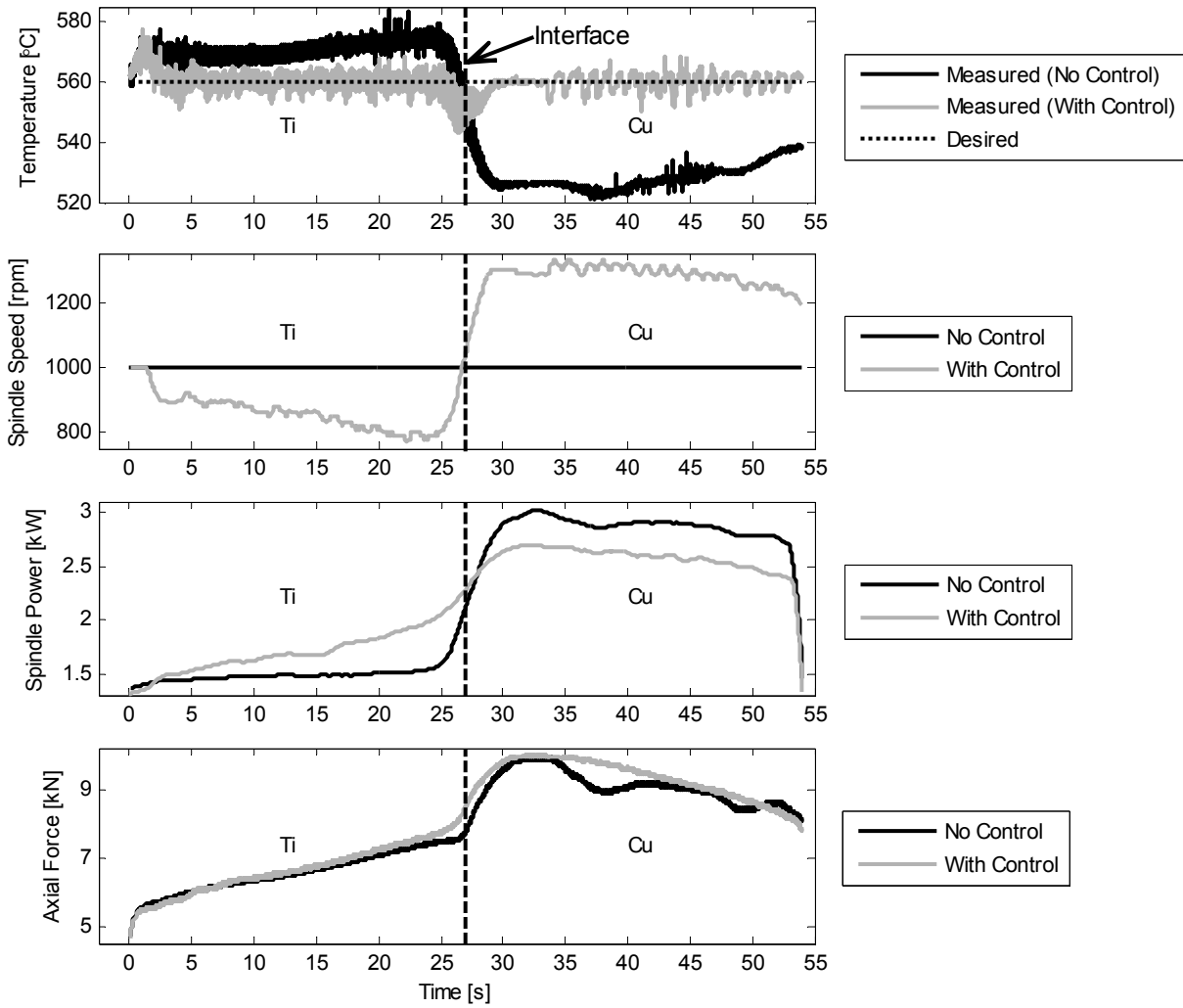


Figure 48: Measured interface temperature, commanded spindle speed, measured spindle power and measured axial force when welding across a titanium and copper backing plate.

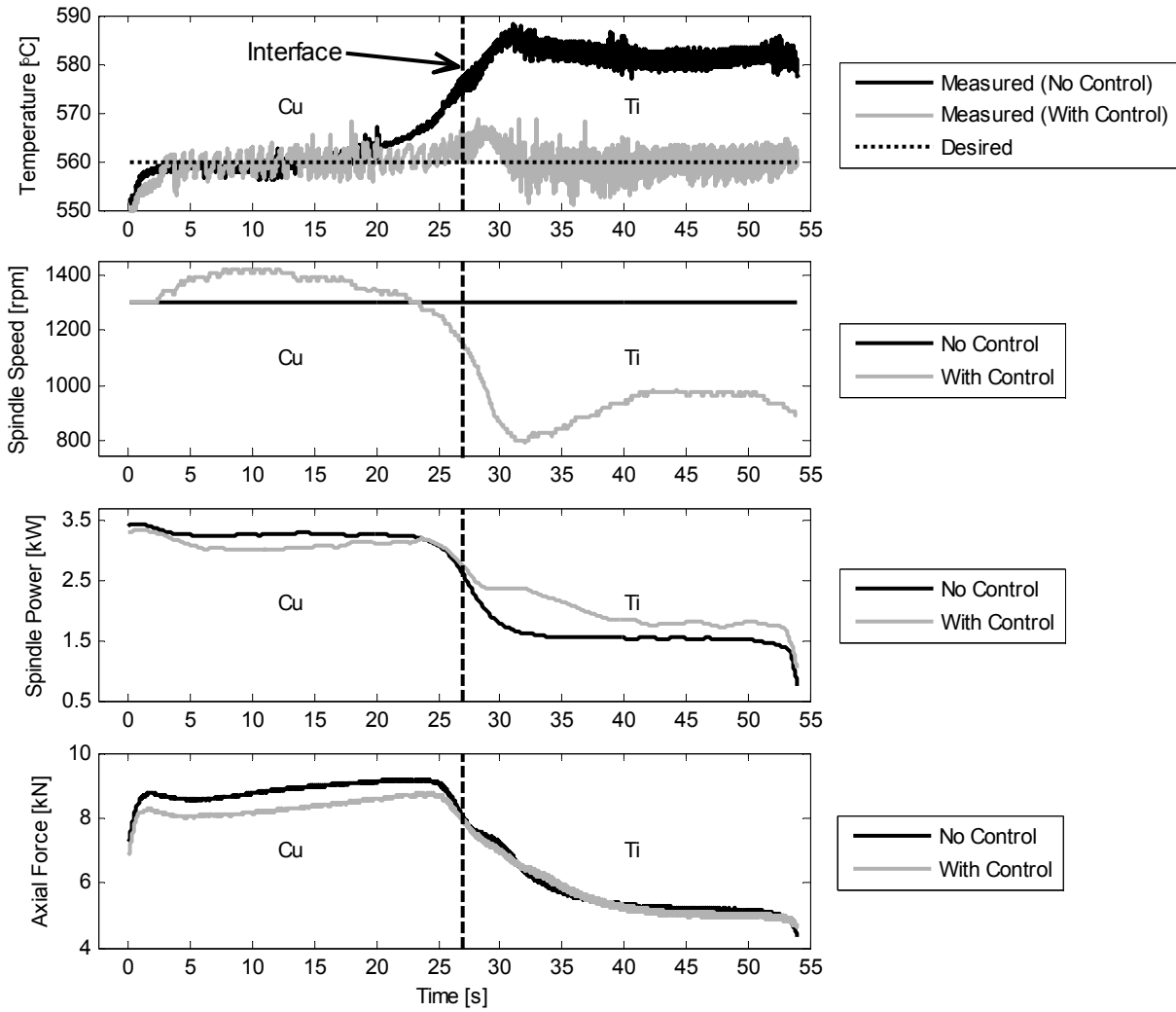


Figure 49: Measured interface temperature, commanded spindle speed, measured spindle power and measured axial force when welding across a copper and titanium backing plate.

6.2. *Transfer of Temperature Control System to Different Aluminum Alloys*

6.2.1. *Introduction*

For the same welding parameters, welding different aluminum alloys results in different interface temperatures, plunge forces and spindle powers. Experiments have been carried out to compare the measured interface temperatures, plunge forces and spindle powers for varying aluminum alloys. Figure 50 and Figure 51 report the average temperature, plunge force and spindle power during the middle 75 % of each weld traverse for each alloy. For all welds the same tool was used, the spindle speed was 900 rev/min, the travel speed 150 mm/min and the plunge depth 3.2 mm. Under these conditions, acceptable welds without any apparent defects were produced for all alloys. Average plunge forces range from 7.5 kN to almost 10 kN and average spindle powers range from over 1.4 kW to almost 1.7 kW. For 6061-T6 and 5083-H116, the measured temperatures are below the solidus temperatures [55] of the base material, which suggests that no local melting of aluminum occurred. In the case of 2024-T3, the measured temperature is at the solidus temperature and for 7075-T6 the average measured temperature is 43 K above the solidus temperature. In the latter case, this is an indication for localized melting of aluminum at the tool-workpiece interface under these process conditions. This does not indicate that melting occurred in the whole weld zone.

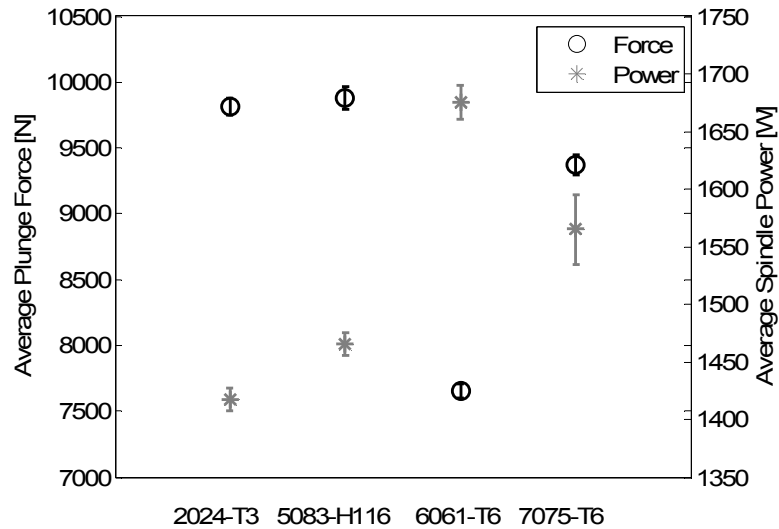


Figure 50: Average plunge force and average spindle power for different aluminum alloys, all welded at the same welding parameters (900 rev/min spindle speed, 150 mm/min travel speed and 3.2 mm plunge depth).

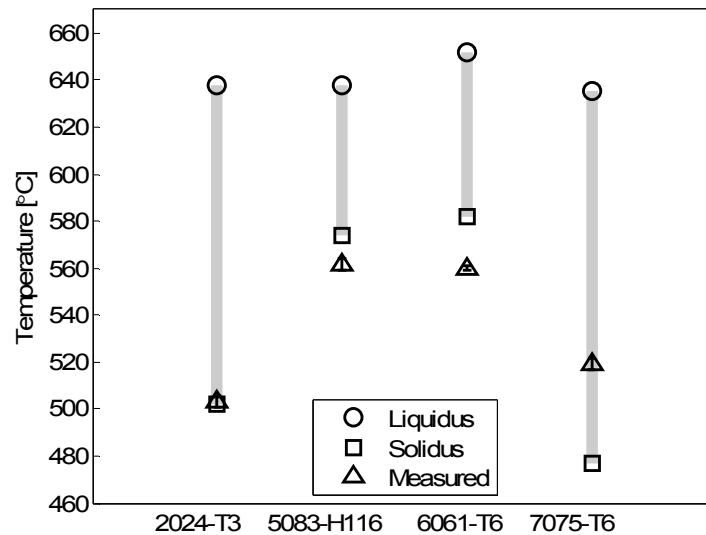


Figure 51: Average interface temperature compared to liquidus and solidus temperatures for different aluminum alloys, all welded at the same welding parameters (900 rev/min spindle speed, 150 mm/min travel speed and 3.2 mm plunge depth). The grey bars indicate the temperature range between solidus and liquidus temperature.

6.2.2. Parametric Effects on Interface Temperature

A method was sought which would describe the influence of spindle speed and travel speed on the interface temperature. In initial tests, the travel speed and spindle

speed were both identical for all alloys tested (1000 rev/min and 200 mm/min). Table 5 shows the amplitude of the measured interface temperature observed with the initial parameters. For each data column, one parameter was held constant, while the other changed sinusoidally at a frequency of 0.6 Hz. While the travel speed was held constant, the spindle speed was set at 1000 rev/min with 50 rev/min amplitude. With constant spindle speed, the travel speed was set at 200 mm/min with 150 mm/min amplitude. The corresponding tool feeds (weld distance per revolution) for each condition ranged from 0.19 to 0.21 mm/rev and from 0.05 to 0.35 mm/rev, respectively. While running deviation tests, the average amplitude of the temperature wave form (ΔT) in weld interface temperature is determined using a FFT. The measured temperature amplitude ΔT is compared among the alloys tested; indicating for each alloy which parameter has a greater influence on temperature change.

The initial results indicated that when welding with different aluminum alloys, the parametric influence on temperature varies for each alloy. As shown, the interface temperature was more sensitive to changes in travel speed in alloys 2024-T3 and 7075-T6, but more sensitive to changes in spindle speed in 6061-T6. However, for 5083-H116, both spindle speed and travel speed had only minor influence on the tool-workpiece interface temperature.

Table 5: Measured interface temperature amplitudes for initial tests with identical welding parameters for each alloy. Spindle and travel speed changed at 0.6 Hz.

Alloy	Amplitude in Temperature ΔT [°C]	
	Variable Spindle Speed	Variable Travel Speed
	Constant Travel Speed: 200 mm/min	Constant Spindle Speed: 1000 rev/min
2024-T3	0.98	2.45
5083-H116	0.48	0.89
6061-T6	3.14	0.67
7075-T6	0.98	4.77

While several alloys are tested in this study, there was no assumption that a single set of parameters would produce a high quality joint for each alloy. Thus, suitable baseline parameters were sought that would “maximize” the weld strength for each alloy. The nominal welding parameters for each alloy that result in the optimal weld strength under the conditions tested are summarized in Table 6 along with the maximum ultimate tensile strength (UTS) achieved relative to the parent material tested (joint efficiency). These joint efficiencies do not represent the highest strength that is achievable in each alloy but rather reflect what was feasible with the current setup. For this reason, optimum strength was not desired, only an acceptable strength was needed for the testing method. Limitations are given by the tool that was available for this study. The tool was not optimized for each alloy but rather the same tool was used for all alloys. Furthermore, the particular tool used has a relatively short pin compared to the shoulder diameter.

Table 6: Nominal alloy welding parameters and measured joint efficiency.

Alloy	Spindle Speed (rev/min)	Travel Speed (mm/min)	Joint Efficiency (%)
2024-T3	1100	75	89.3
5083-H116	1300	150	85.8
6061-T6	1300	200	53.3
7075-T6	1300	75	71.1

Once the nominal baseline parameters were chosen, each alloy is welded with a single parameter (spindle speed or travel speed) held constant while the other is varied in a sinusoidal fashion with a specific frequency and amplitude. The deviations are chosen to be $\pm 20\%$ of the nominal spindle speed and travel speed, which results in a comparable change in feed (weld distance per revolution). Care is taken to make sure the deviations are within the physical limits of the welding machine (*i.e.*, weld parameters are well within the achievable limits of the system). This deviation percentage also provides consistent changes and comparison among alloys with regards to testing parameters. Table 7 shows parameters for the 20% amplitude tests with changes at a frequency of 0.1 Hz.

Table 7: Welding Parameters for each alloy tested with 20% amplitude deviation from nominal spindle or travel speed at 0.1 Hz.

Alloy	Nominal		20 %Amplitude of Nominal		Feed (mm/rev)		
	Spindle Speed (rev/min)	Travel Speed (mm/min)	Spindle Speed (rev/min)	Travel Speed (mm/min)	Varying Spindle Speed	Varying Travel Speed	
2024-T3	1100	75	220	15	0.085	0.082	Max
					0.057	0.055	Min
5083-H116	1300	150	260	30	0.144	0.138	Max
					0.096	0.092	Min
6061-T6	1300	200	260	40	0.192	0.185	Max
					0.128	0.123	Min
7075-T6	1300	75	260	15	0.072	0.069	Max
					0.048	0.046	Min

Because the amplitudes in travel speed are far from the machine's physical limits, the results of the previous study are combined with an additional study where travel speed was changed at an amplitude of 80% of the nominal value. The amplitude increase was not applied to the spindle speed because the parameters would have been outside of the welding system limits. Figure 52 summarizes the results from these studies.

There are several significant observations from these results. For most alloys, spindle speed is the dominant parameter affecting temperature change. The outlying alloy is 5083-H116, where travel and spindle speed amplitudes were similar. Once the travel speed amplitude was increased to 80%, spindle speed was still the dominant process parameter affecting measured interface temperatures. Still for 5083-H116, the influences were relatively close, but at 80%, travel speed influence surpassed that of spindle speed.

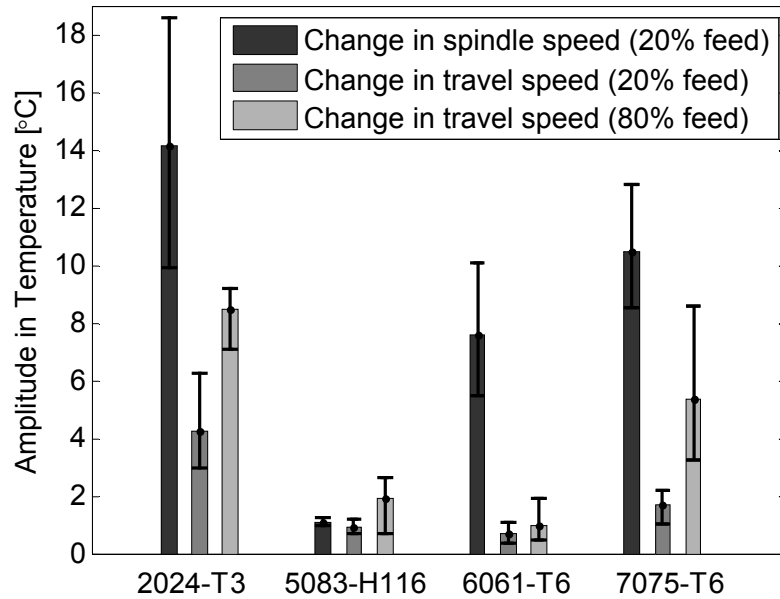


Figure 52: Average temperature amplitudes during the weld traverse due to spindle speed or travel speed oscillations for each alloy tested.

There is a different response in temperature for the two process inputs. When increasing the spindle speed, the higher heat generation causes the interface temperature to increase whereas a higher travel speed causes more heat removed from the weld zone through advection (in an Eulerian reference frame), hence causes a lower temperature.

There is a measurable phase lag between the commanded spindle speed or travel speed (process input) and the measured response in temperature (process output). This phase lag increases in magnitude for higher frequencies. At a relatively low frequency of 0.1 Hz, the phase lag is less than 10 degrees when changing spindle speed, but the phase lag is significantly higher (about 200 degrees phase lag) when changing travel speed. This means that when choosing travel speed to affect the

process, there is a significantly higher delay until the interface temperature changes, which is undesirable for the purpose of control.

6.3. Sensitivity of Temperature Control System

For the design of model-based closed-loop temperature control algorithms it is critical to establish a useful dynamic process model. There is always some uncertainty in such models and the question needs to be answered how accurately those process models need to be known in order for the control system to function properly in terms of performance and stability.

In a series of experiments (as part of system identification tests), five different workpiece geometries and joint configurations were welded as shown in Table 8. The spindle speed was changed in steps between 1000 rpm and 1400 rpm with a constant travel speed of 200 mm/min and a constant plunge depth of 4.9 mm.

Table 8: Workpiece geometries (workpiece length for all cases 203 mm).

Thickness [mm]	Width [mm]	Cross-sectional Area [mm ²]	Joint Configuration	Weld Penetration	Notes
3.18	76.2	242	Bead-on-plate	Full	Tool with reduced pin length
4.76	102	484	Bead-on-plate	Full	
6.35	102	645	Bead-on-plate	Partial	
4.76	2 x 102	968	Butt weld	Full	Nominal configuration
6.35	2 x 102	1290	Butt weld	Partial	

Prior system identification work of the welding process indicated that a first order model with pure delay could be used to represent the dynamic relation between commanded spindle speed and measured shoulder interface temperature [52]. In that

work, the process model was identified using frequency domain techniques. While providing good process model and parameter identification, frequency domain identification can be time consuming. In this work, we can rely on the earlier process model identification and focus on parameter identification. As such, time domain identification, via examination of time history response to step inputs, can be used. Based on the measured interface temperatures due to step inputs in spindle speed (1000 rpm to 1400 rpm at a constant travel speed of 200 mm/min), dynamic process models were estimated for welding 6061-T6 (using the System Identification Toolbox in MATLAB™). It was found that simple first order models with a time delay as denoted in (13) in the time domain and (14) in transfer function notation in the Laplace domain represent the system behavior well relating the measured interface temperature $t_{\text{int}}(t)$ (or $T_{\text{int}}(s)$) to the commanded spindle speed $\omega_{\text{tool}}^*(t)$ (or $\Omega_{\text{tool}}^*(s)$).

$$\tau_p \frac{d}{dt} t_{\text{int}}(t) + t_{\text{int}}(t) = K_p \omega_{\text{tool}}^*(t - \Delta T_p) \quad (13)$$

$$G_p(s) = \frac{T_{\text{int}}(s)}{\Omega_{\text{tool}}^*(s)} = \frac{K_p}{\tau_p \cdot s + 1} \cdot e^{-\Delta T_p \cdot s} \quad (14)$$

From a step response in the time domain as seen in Figure 53, the time delay ΔT_p [s], gain K_p [°C/rpm] and time constant τ_p [s] were determined for workpieces with varying cross-sectional areas. For workpieces with larger cross-sectional area the average interface temperature at both the shoulder and pin interfaces decrease as seen in Figure 54 due to larger thermal masses of the workpieces. Average shoulder temperatures are higher than average pin temperatures for all cases in this section. For

the condition with the lowest workpiece cross-sectional area, another FSW tool was used that only has a single thermocouple at the shoulder (*i.e.*, no information available about temperatures at the pin interface). None of the welds showed any apparent weld defects.

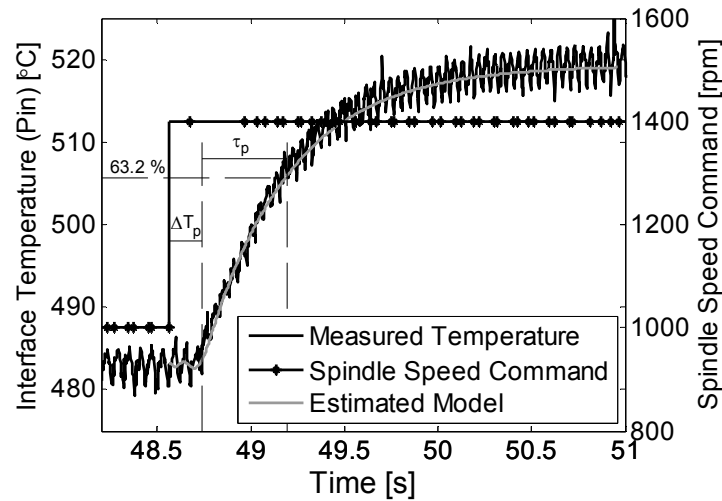


Figure 53: Typical step response in interface temperature due to step input in spindle speed.

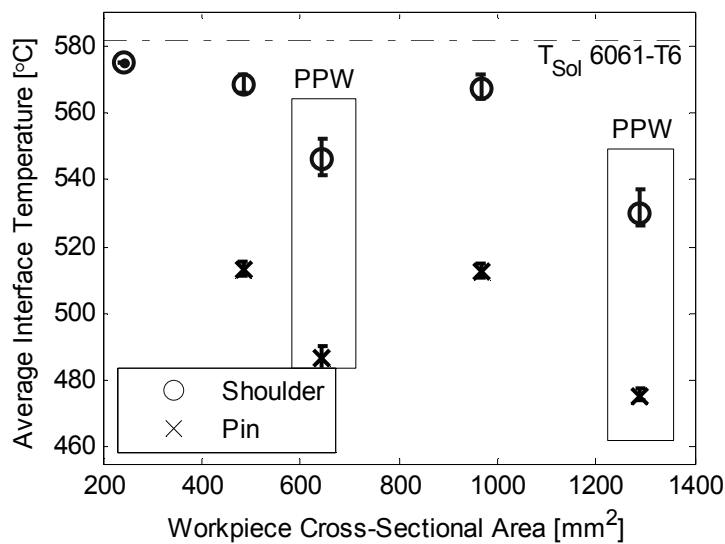


Figure 54: Average interface temperature vs. workpiece cross-sectional area. Conditions: 1000 rpm, 200 mm/min, 6061-T6 (PPW = Partial penetration weld).

Figure 55 shows a block diagram of the overall process with the parameters estimated in this study (found in Table 9) for the nominal workpiece geometry (4.76 mm thick butt welds). The model can be broken down into individual dynamic elements as illustrated in Figure 55 to indicate the different components of the overall process. By doing this, it is realized that the overall process model should be of higher order, however, this is just a simplified, qualitative analysis. In order to determine a higher order model, system identification data at higher frequencies would be needed. This data cannot be obtained due to hardware limitations (*e.g.*, limited spindle acceleration). Based on the known dynamics of the actuator (spindle motor), thermocouple sensors and wireless transmission (determined experimentally), the dynamics of the actual process (relating actual spindle speed to the temperature at the tool-workpiece interface) can be back-calculated.

Of particular interest for related work were the dynamics of the thermocouple sensors, which allows the back-calculation of the true temperatures experienced at the tool-workpiece interface, *i.e.*, without any attenuation and phase lag [41]. It can be seen that the thermocouple sensor response times are minor compared with the spindle motor response. The time delay ΔT_p is largely dependent on communication delays, *i.e.*, commanded spindle speed signal through custom interface to CNC mill ($\Delta T_{p,a}$) and wireless transmission of measured temperatures ($\Delta T_{p,t}$). It is therefore considered constant (approximately 180 ms) within the scope of these experiments of varying workpiece geometries.

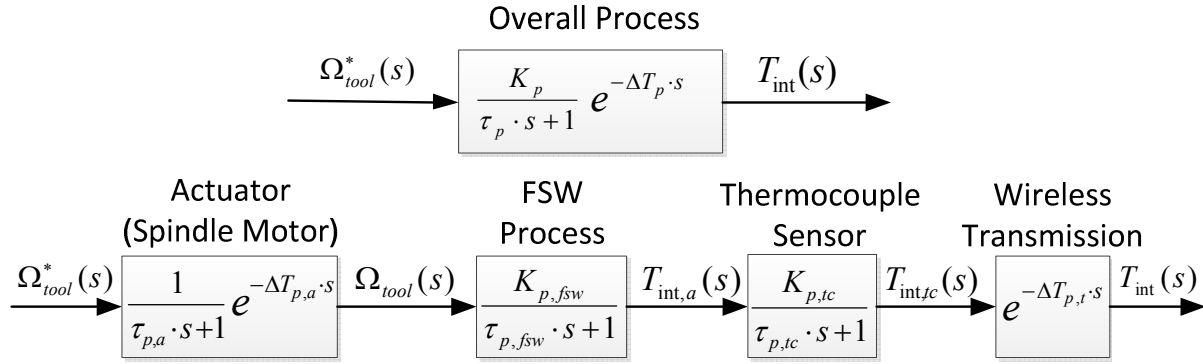


Figure 55: Block diagram of overall process (top) and process broken into separate dynamic elements (bottom).

Table 9: Estimated overall and individual parameter values of process model.

Parameter	Value	Unit
K_p	0.04 (shoulder), 0.07 (pin)	$^{\circ}\text{C}/\text{rpm}$
τ_p	170 (shoulder), 320 (pin)	ms
ΔT_p	180	ms
$\tau_{p,a}$	150	ms
$\Delta T_{p,a}$	110	ms
$K_{p,tc}$	1	$^{\circ}\text{C}/^{\circ}\text{C}$
$\tau_{p,tc}$	11 (shoulder), 37 (pin)	ms
$\Delta T_{p,t}$	70	ms

The estimated time constant τ_p [s] of the dynamic process model increases with larger workpiece cross-sectional area due to the larger thermal mass of the workpieces (Figure 56). Figure 57 shows the estimated gain K_p [$^{\circ}\text{C}/\text{rpm}$] of the dynamic process model, which increases for higher workpiece cross-sectional areas. The gain value of the condition with the lowest workpiece cross-sectional area is larger than expected,

which might be due to the fact that a different system identification method was used for this case (one frequency at a time vs. step response). A possible explanation for this gain increase is that for larger cross-sectional workpieces, the average interface temperature decreases (Figure 54), and for lower average temperatures (*i.e.*, spindle speeds), a certain change in spindle speed results in a larger change in temperature than at higher average temperatures, where the temperature approaches the solidus temperature of the workpiece. This means that the process gain is temperature dependent and is illustrated in Figure 32 and Figure 58. Figure 32 shows the average temperatures experienced at the tool-shoulder interface when varying the spindle speed and travel speed for 6061-T6 with a workpiece cross-sectional area of 968 mm². Figure 58 shows the extracted process gains [°C/rpm] from the data in Figure 32 and demonstrates the temperature dependency of the process gain, *i.e.*, lower gains for higher temperatures. This also explains that the process gains are higher for the pin interface temperature than for the shoulder interface temperature, since average shoulder interface temperatures are higher than average pin interface temperatures (Figure 54).

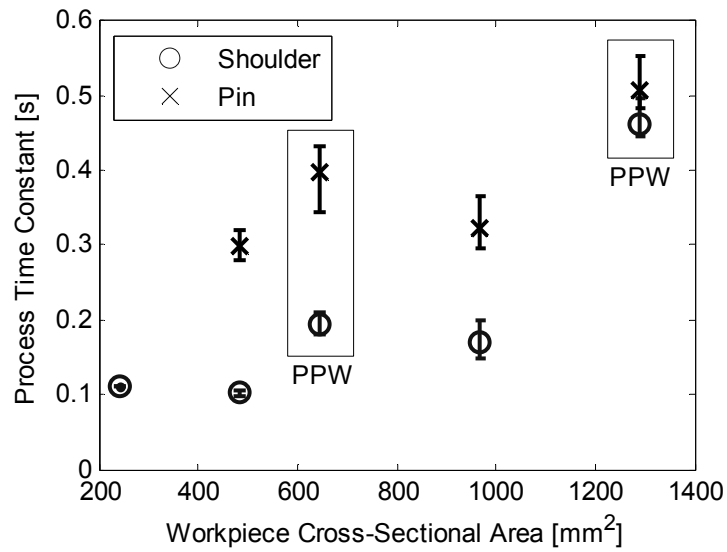


Figure 56: Time constant from system identification vs. workpiece cross-sectional area (6061-T6) (PPW = Partial penetration weld).

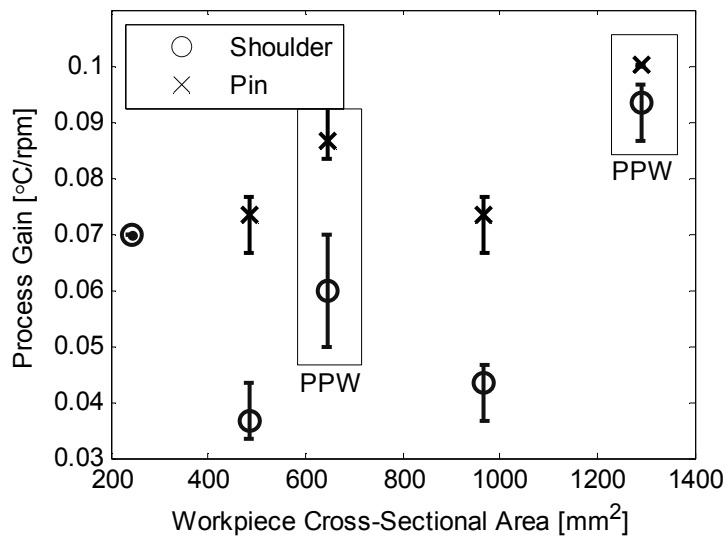


Figure 57: Gain from system identification vs. workpiece cross-sectional area (6061-T6) (PPW = Partial penetration weld).

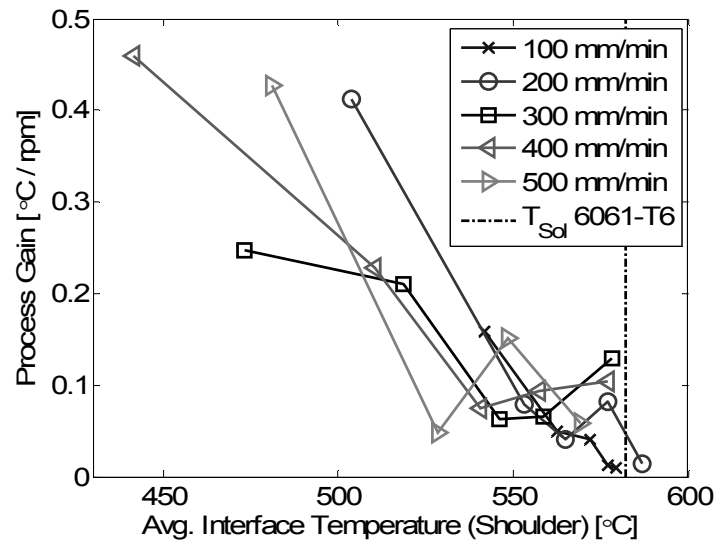


Figure 58: Process gain relating shoulder interface temperature to spindle speed for various spindle speeds and travel speeds for 6061-T6 with a cross-sectional area of 968 mm².

This data shows that among the full penetration welds, the effect of workpiece cross-sectional area on the process models is only minor (in the range investigated). The process model is influenced more strongly by the weld penetration depth (full vs. partial). Thicker workpieces welded with a tool of the same pin length resulting in a partial penetration weld cause a significant decrease in average interface temperature and a significant increase in both process gain and time constant. This can be attributed to the extra workpiece material beneath the tool pin acting as an additional thermal mass.

When process model parameters change, the performance of the closed-loop control system can become limited or result in unwanted dynamics. Both an increase in process time constant and an increase in process gain causes the closed-loop system to be less stable. Based on this work, it can be estimated how the process model changes

and the control gains can be appropriately reduced to avoid resulting in an unstable system.

The closed-loop temperature control system as described in [52] was originally developed for the workpiece geometry with the lowest cross-sectional area (3.18 mm thick workpieces and a tool with a pin length of 3.0 mm, called nominal workpiece geometry in this section). In order to test how sensitive this controller is when welding the other workpiece geometries investigated in this study, it was applied to the workpieces with the two highest cross-sectional areas. Figure 59 shows the command tracking for step commands during butt welding of 4.76 mm thick workpieces (full penetration welds). It can be seen that the control system is still able to adjust the heat generation by changing the spindle speed to achieve the desired shoulder interface temperature. The system responds in a timely manner, there is minimal steady-state error and no overshoot. The time constant of the closed-loop step response is approximately 0.6 s (compared to 0.8 s with nominal workpiece geometry [52]). The response is slightly faster due to the slower process but utilizing the same controller gain originally determined for the geometry with a lower cross-sectional area, and the closed-loop bandwidth being slower than the process bandwidth.

Figure 60 shows the command tracking for sinusoidal commands at 0.2 Hz with a 10 °C amplitude for butt welding of 6.35 mm thick workpieces (partial penetration welds). In this case, the controller performs still well by tracking the desired temperature, but there is some overshoot. The attenuation and phase lag are approximately 1.3 and 35

degrees, respectively (compared to 0.9 and 28 degrees with nominal workpiece geometry at the same excitation frequency and amplitude [52]). The system performs slightly worse (overshoot and larger phase lag) compared to the nominal geometry, which is expected when examining the dynamic process gains and time constants as shown in Figure 56 and Figure 57, respectively. The gain and time constant for this geometry (partial penetration weld with largest workpiece cross-sectional area) are significantly higher than for the other (full penetration) welds) causing a reduction in damping of the dynamic system.

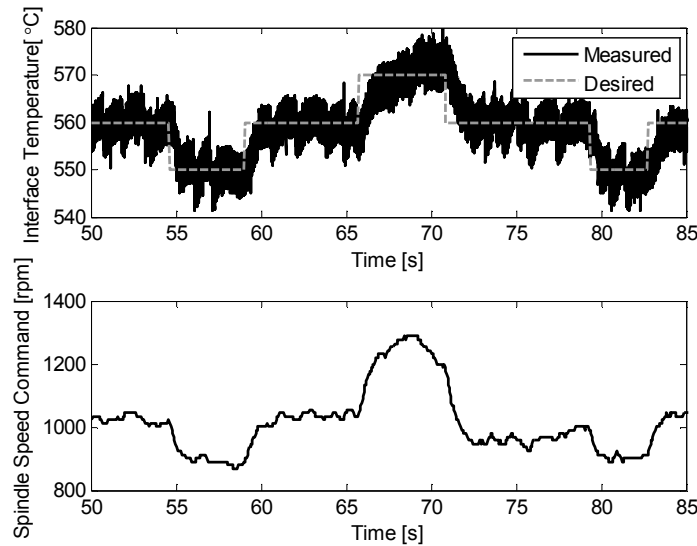


Figure 59: Closed-loop control of shoulder interface temperature during full penetration butt welding of two 4.76 mm thick workpieces (6061-T6). Command tracking in steps with 10°C step size.

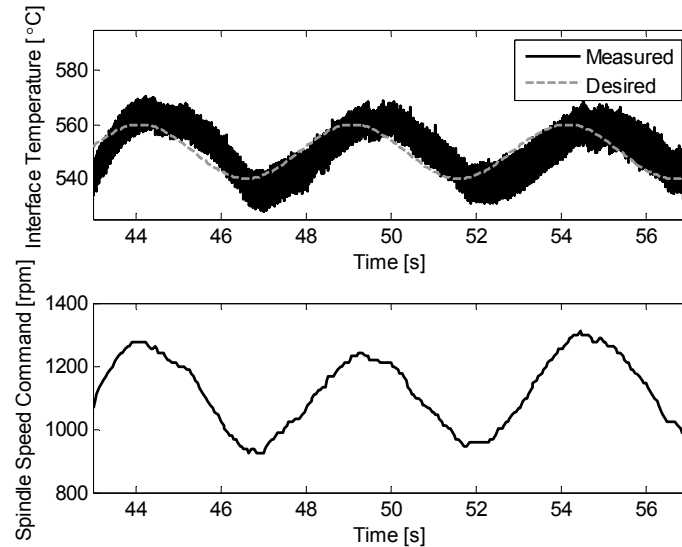


Figure 60: Closed-loop control of shoulder interface temperature during partial penetration butt welding of two 6.35 mm thick workpieces (6061-T6). Sinusoidal command tracking at 0.2 Hz, 10°C amplitude.

6.4. Transfer to Robotic Friction Stir Welding System

6.4.1. Process Dynamics

Figure 61 shows the measured interface temperatures at the shoulder and pin location and the measured forces during a weld under position control with a constant spindle speed of 2000 rpm, a constant travel speed of 300 mm/min and a step change in commanded vertical tool position from 10.5 mm to 11.0 mm in the first half of the weld traverse. The temperature measurement approach chosen in this work captures the dynamics of the process very well, because the thermocouple sheaths are in direct contact with the aluminum workpiece at the tool-workpiece interface. The measured temperatures are not constant, but rather oscillating as the tool traverses under constant operating conditions as seen in Figure 61. The frequency of these oscillations is found

to match the frequency of the spindle rotation, *i.e.*, the thermocouple is capturing temperature variations through 360 degrees of the tool rotation. More information about the temperature dynamics captured with this measurement strategy can be found in [67], a calibration of the measurements is presented in [41]. The amplitudes of the oscillations observed in the three welding forces (also at the same frequency as the spindle rotation) are larger than welding the same workpieces using the same tool on a CNC mill, which is one indication of a higher flexibility of the robotic system compared to the stiffer CNC mill.

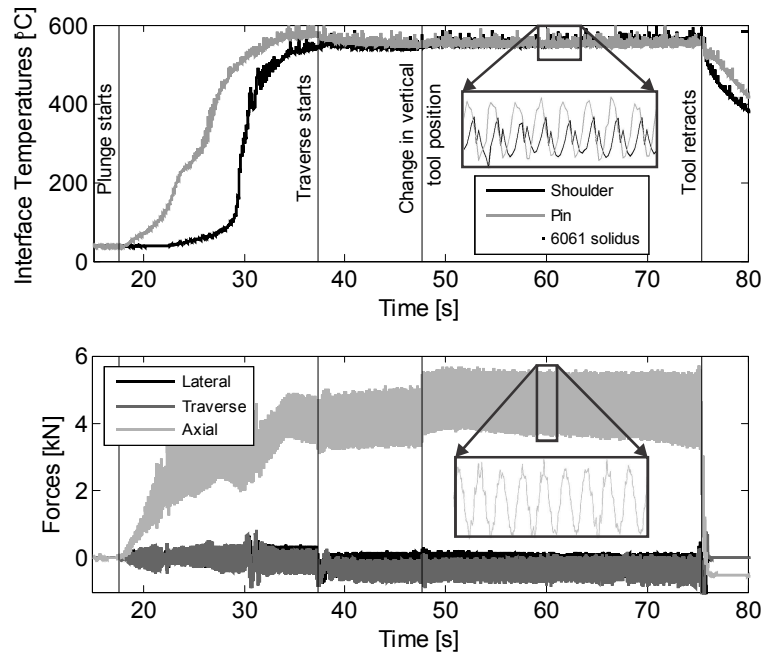


Figure 61: Measured interface temperatures and forces when welding 6.35 mm thick 6061-T6 with a constant spindle speed of 2000 rpm, constant travel speed of 300 mm/min and a step in commanded vertical tool position from 10.5 mm to 11.0 mm (at 48 s).

The closed-loop control system is only enabled during the weld traverse, hence all figures from now on solely show the weld traverse for simplicity.

6.4.2. Closed-Loop Control of Interface Temperature

In order to verify the control system's performance, command tracking experiments were performed (Figure 62), in which the desired pin temperature was varied sinusoidally with a 10 °C amplitude and a frequency of 0.2 Hz. It can be seen that the spindle speed is adjusted by the controller and the measured pin interface temperature also varies sinusoidally at the same frequency. Because the system operates right at the closed-loop bandwidth, there is a significant phase lag of approximately 90 degrees. This matches the result from the simulation of 83 degrees (Figure 24). Due to the higher communication delays present on the robotic system (the delay on the CNC mill was found to be only 85 ms), the phase lag of the control system implemented on the CNC mill is only approximately 40 degrees (same workpiece and command trajectory). The closed-loop performance could be increased using more sophisticated controller structures if needed, but the performance of the given system is likely to be acceptable for many applications. The attenuation of the measured temperature compared to the commanded temperature is approximately 0.8 on the robotic system (matching the result from the simulation of 0.74, Figure 24) and approximately 1.3 on the CNC mill. Disturbance rejection properties of the temperature control system have been demonstrated previously on the CNC mill and can be found in [52].

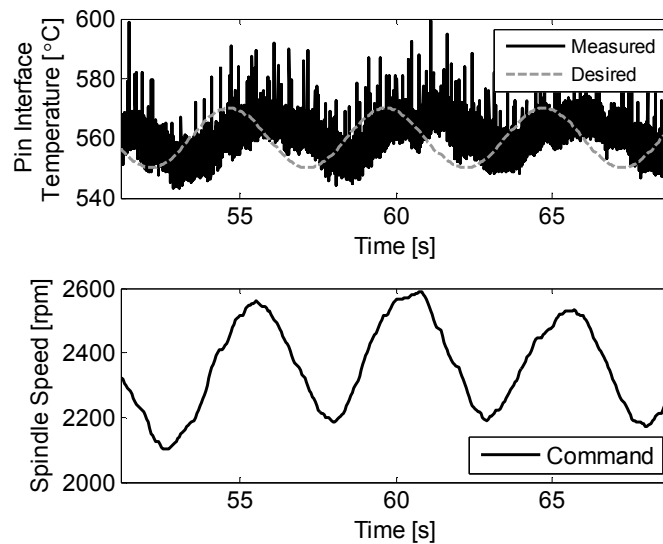


Figure 62: Command tracking of closed-loop interface temperature control system using a sinusoidal command with 10 °C amplitude and frequency of 0.2 Hz. Constant commanded vertical tool position 10.5 mm. Constant travel speed of 360 mm/min. Feedback of pin interface temperature.

6.4.3. Closed-Loop Control of Axial Force

The performance of the axial force control system was evaluated and is shown in Figure 63 for command tracking of steps in desired force of 500 N magnitude. It can be seen that the vertical tool position is adjusted appropriately and that the system follows the commands well with no steady-state error, no overshoot and a closed-loop time constant of approximately 0.9 s.

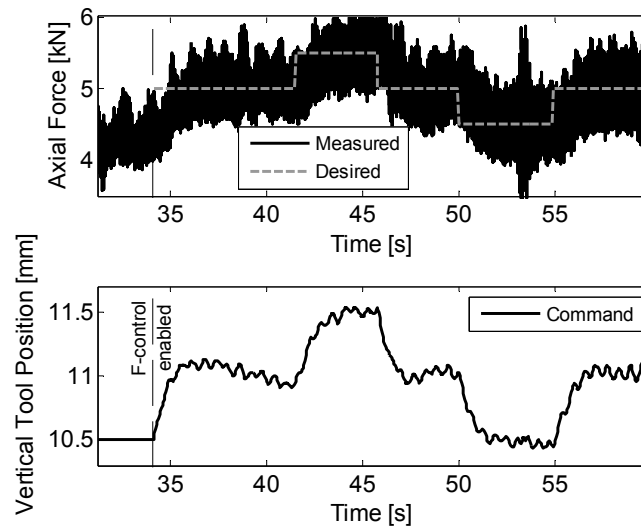
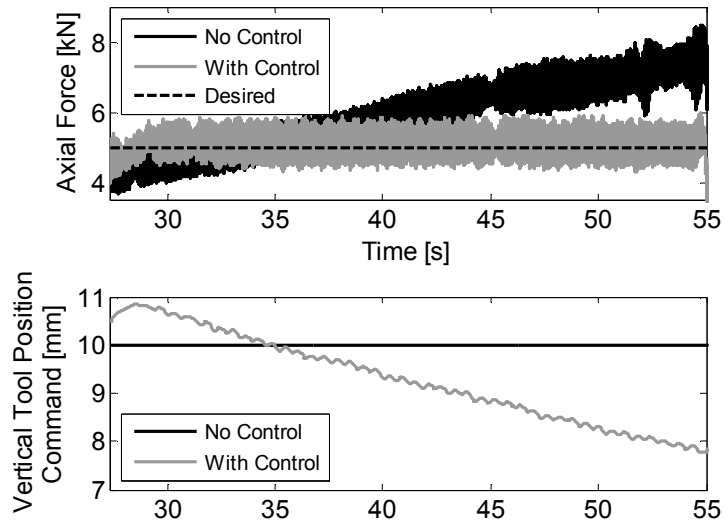
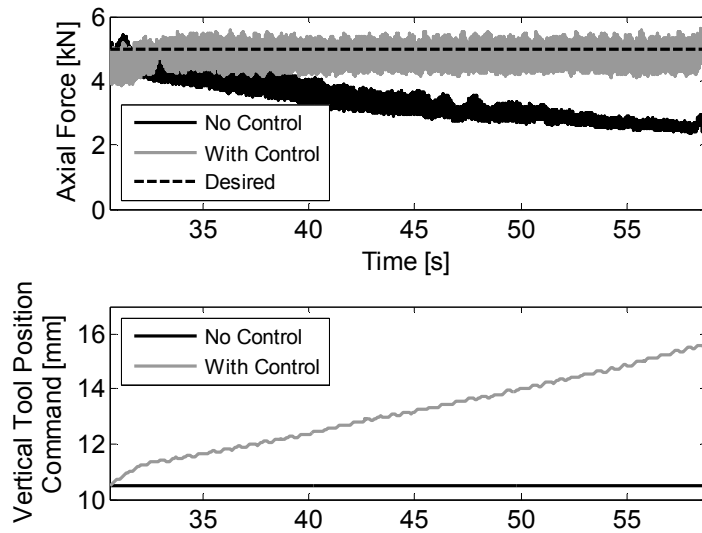


Figure 63: Command tracking of closed-loop axial force control system using step commands of 500 N magnitude. Constant spindle speed 2300 rpm. Constant travel speed of 360 mm/min.

In order to evaluate the disturbance rejection properties of the axial force control system, ramp disturbances were introduced into the system by adding a vertical offset to the programmed end point of the weld. In Figure 64 it can be seen that the axial forces increase or decrease significantly (positive and negative offset, respectively) when no axial force control is utilized. This can lead to a significant degradation in weld quality as seen in the photographs of the weld surfaces (Figure 65). When employing axial force control, the vertical tool position is adjusted by the controller, which leads to proper tool shoulder-workpiece contact, a constant axial force and a substantial improvement in weld quality while encountering the ramp disturbance.



(a)



(b)

Figure 64: Disturbance rejection of closed-loop axial force control system using an (a) 3 mm positive and (b) 5 mm negative ramp disturbance. Constant spindle speed 2300 rpm. Constant travel speed of 360 mm/min.

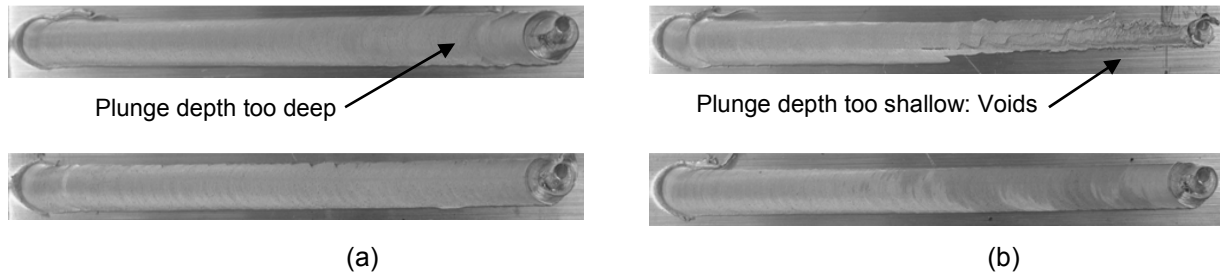


Figure 65: Photographs of weld surfaces related to Figure 64. Top: No control, bottom: With axial force control. (a) 3 mm positive ramp disturbance, (b) 5 mm negative ramp disturbance.

6.4.4. Combined Closed-Loop Control of Interface Temperature and Axial Force

The combined temperature and force control system is first evaluated in terms of command tracking (Figure 66). After commanding a constant axial force of 4.5 kN, it is decreased and then increased in steps while the desired interface temperature is held constant. The vertical tool position is automatically decreased, then increased to achieve the desired axial force. Changing the vertical tool position also affects the temperatures, so the controller increases, then decreases the spindle speed in order to compensate for the changed heat generation through different vertical tool positions. At the end of the weld, the desired interface temperature is increased in steps while the axial force command was held constant, which caused the controller to increase the spindle speed and hold the vertical tool position approximately constant (negligible effect of spindle speed on axial force).

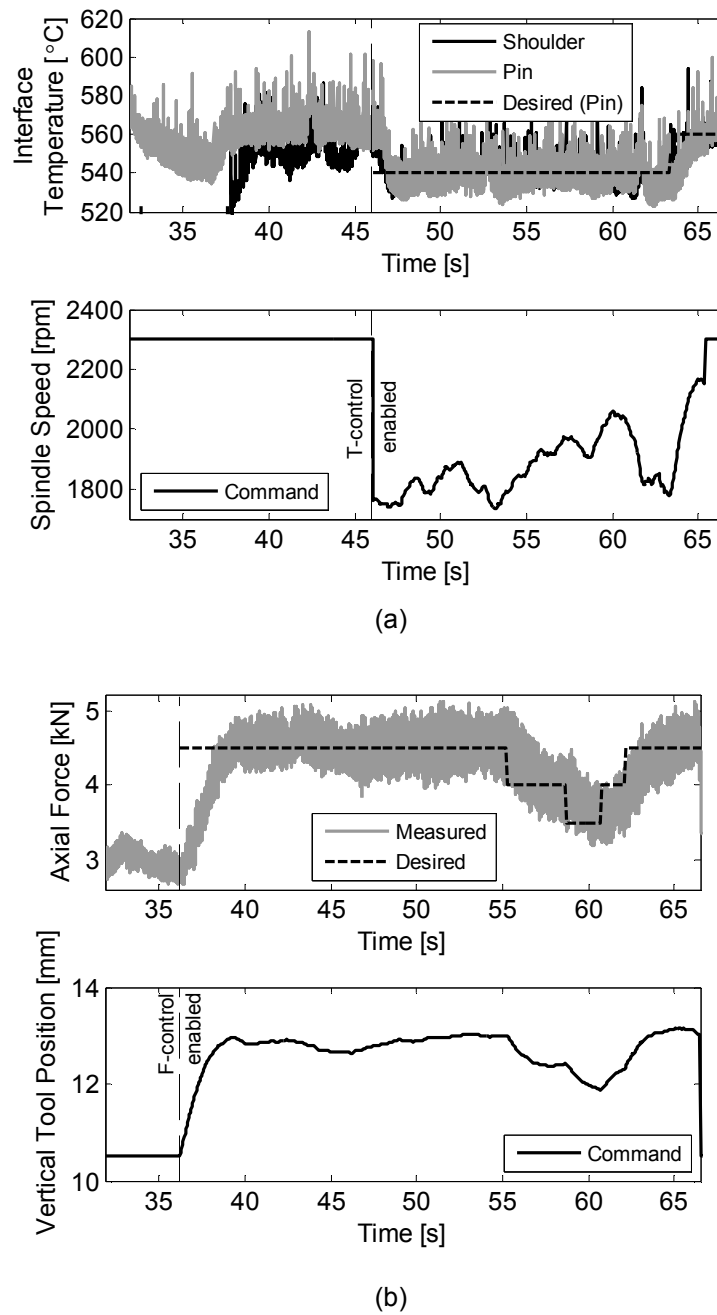
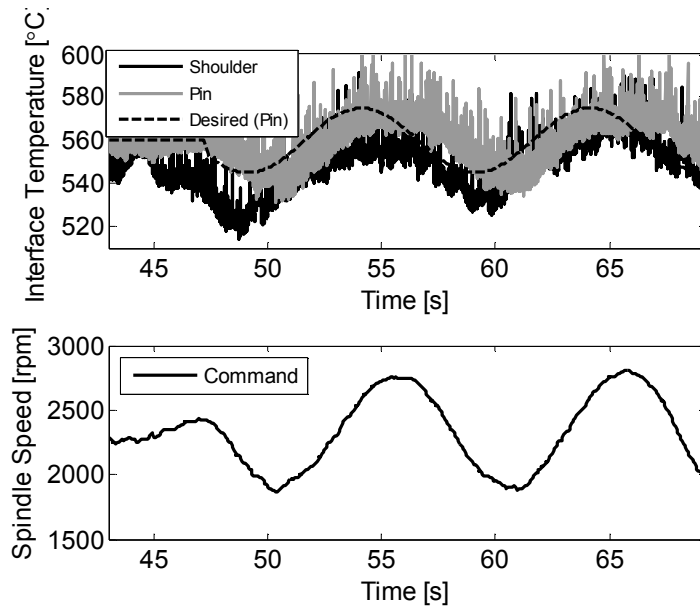


Figure 66: Command tracking of combined closed-loop interface temperature and axial force control system. (a) Temperature part, (b) force part (same weld). Constant travel speed of 300 mm/min. Feedback of pin interface temperature.

In order to further test the control system for simultaneous command tracking, both sinusoidal temperature and force commands were used (Figure 67). The desired

pin interface temperature varied at 0.1 Hz with an amplitude of 15 °C and the desired axial force varied at 0.1 Hz with an amplitude of 500 N and a phase shift of 90 degrees. It can be seen that the combined control system performs well by simultaneously adjusting spindle speed and vertical tool position to track both the temperature and force commands. The phase lag for the temperature is approximately 60 degrees (44 degrees from the simulation, Figure 24) and about 50 degrees for the axial force (53 degrees from the simulation, Figure 27).



(a)

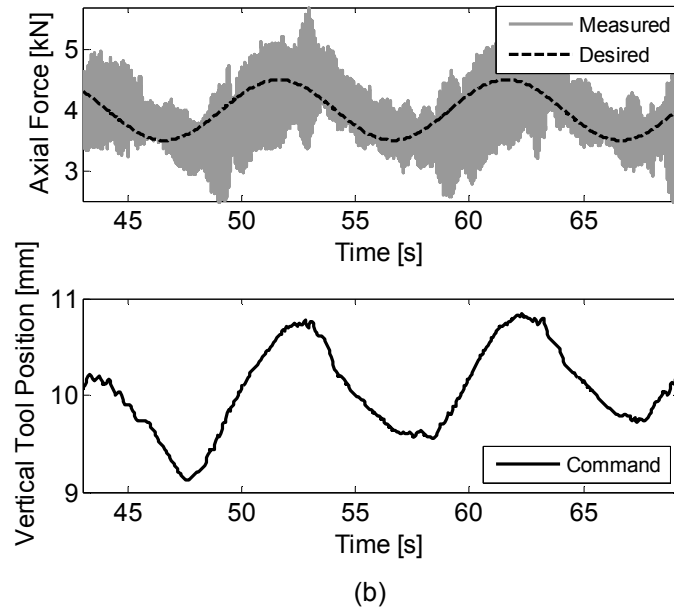
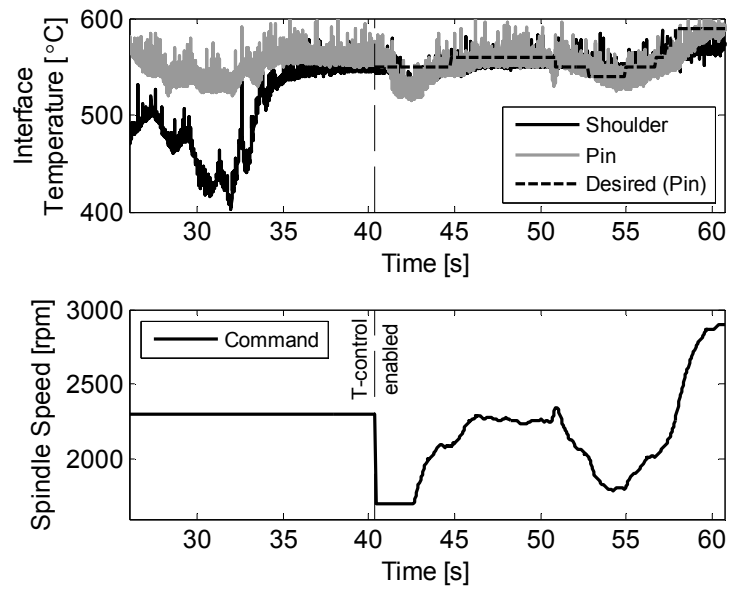
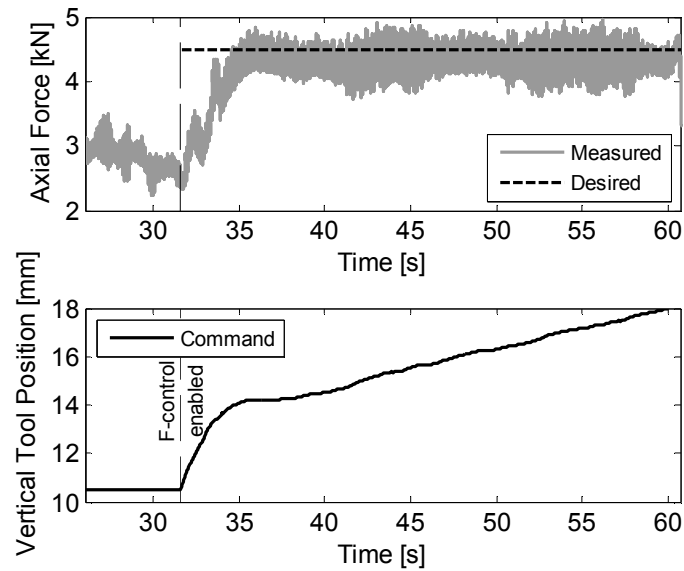


Figure 67: Simultaneous command tracking of combined closed-loop interface temperature (a) and axial force (b) control system (same weld). 0.1 Hz sinusoidal commands for both interface temperature (15 °C amplitude) and axial force (500 N amplitude). Constant travel speed of 300 mm/min. Feedback of pin interface temperature.

The combined temperature and force control system is also applied in a scenario with a ramp disturbance (5 mm negative ramp) as described above in section 6.4.3 while simultaneously commanding a desired interface temperature (Figure 68). The vertical position of the tool is automatically adjusted throughout the weld in order to maintain a proper tool shoulder-workpiece contact, resulting in a constant axial force. Meanwhile, the desired interface temperature is changed in 10 °C steps, resulting in the controller adjusting the spindle speed to alter heat generation.



(a)

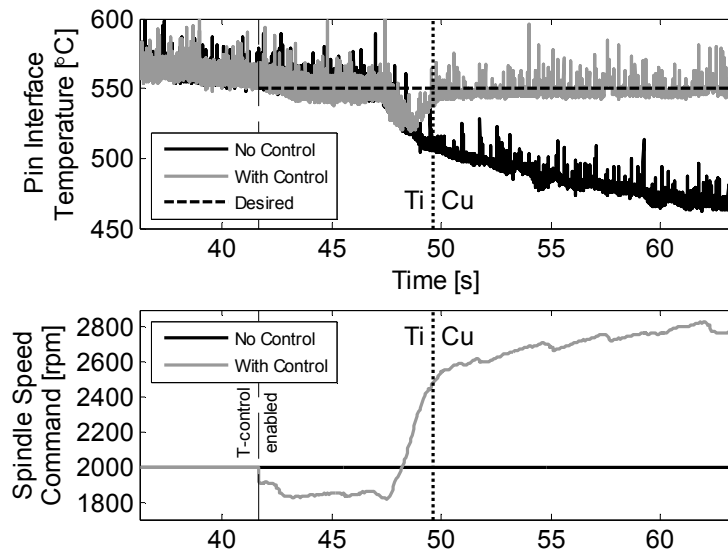


(b)

Figure 68: Command tracking of temperature (a) and disturbance rejection of force (b) of combined closed-loop interface temperature and axial force control system (same weld). Step commands in interface temperature of 10 °C magnitude. 5 mm negative ramp disturbance for axial force. Constant travel speed of 300 mm/min. Feedback of pin interface temperature.

A final scenario to evaluate the combined control system is the application of a step disturbance to the temperature by welding over two backing plates of very different thermal diffusivities as illustrated in Figure 21 and simultaneously applying a ramp disturbance to the force (3 mm negative ramp disturbance). The desired pin interface temperature was held constant at 550 °C and the desired axial force was held constant at 4 kN. The results are shown in Figure 69 and it can be seen that when no temperature or force control is used, the interface temperature decreases by over 80 degrees over the copper backing plate as compared to the titanium backing plate due to the higher thermal diffusivity and hence heat loss out of the weld zone. The temperature starts to decrease approximately 2 s (*i.e.*, 10 mm) before the backing plate interface is crossed, which indicates that the heat flow out of the weld zone is already affected by the copper backing plate at that location. The axial force decreases throughout the whole weld due to the weld end point intentionally programmed 3 mm above the workpiece. When employing the combined control system, the controller increases the spindle speed in a step and increases the vertical tool position throughout the weld. This successfully maintains the desired temperature and force as compared to the scenario when no control is used and weld quality is substantially improved as seen in the weld surfaces in Figure 70. There is a brief drop in pin interface temperature just before the backing plate interface, which is due to the limited response characteristics of the control system (see section 6.4.5). The pin temperature is dropping by 27 °C, even when using the control system, however, the pin temperature is only outside a ± 10 °C window for 1.7 s. This brief drop is not believed to have any significant effect on the

weld quality since the temperature stayed at all times within a temperature window previously determined to result in acceptable weld quality [68]. The chosen setup with the different backing plates also represents a rather extreme change of thermal boundary conditions, which is rarely found in practice.



(a)

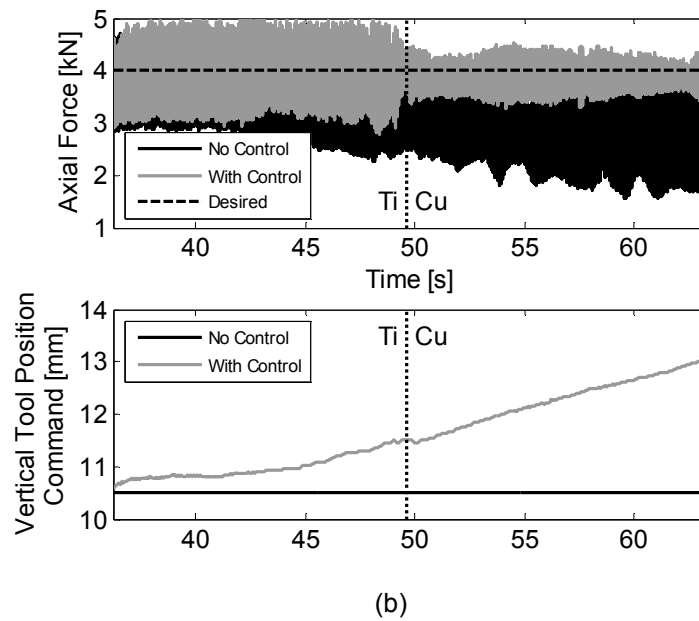
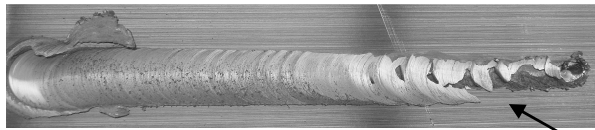
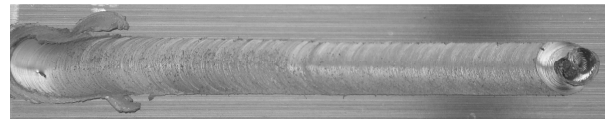


Figure 69: Simultaneous disturbance rejection of combined closed-loop interface temperature (a) and axial force (b) control system (same weld). Welding over different backing plates (step disturbance for temperature) and 3 mm negative ramp disturbance for axial force. Constant travel speed of 300 mm/min. Feedback of pin interface temperature.



Plunge depth too shallow and temperature too low: Voids

(a)



(b)

Figure 70: Weld surfaces when welding over different backing plates and applying a 3 mm negative ramp disturbance for axial force. (a) No control, (b) with combined temperature and force control.

6.4.5. Limitations

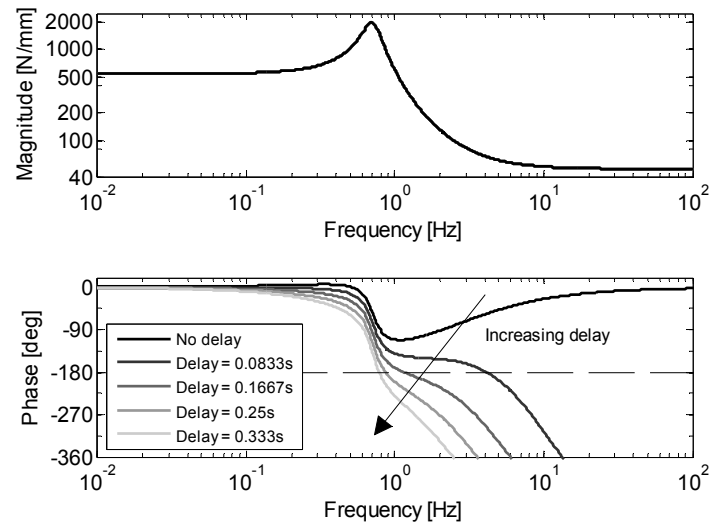
Since the resulting closed-loop bandwidth of each control system is lower than the open-loop process bandwidths, a faster response of the closed-loop system could be expected. A high closed-loop bandwidth is always desired to achieve improved control system performance. Indications of limited control system performance have been

pointed out above, e.g. the deviation in interface temperature from the desired value in Figure 69. The major limitations on the given system are time delays and the low stiffness of the robotic manipulator.

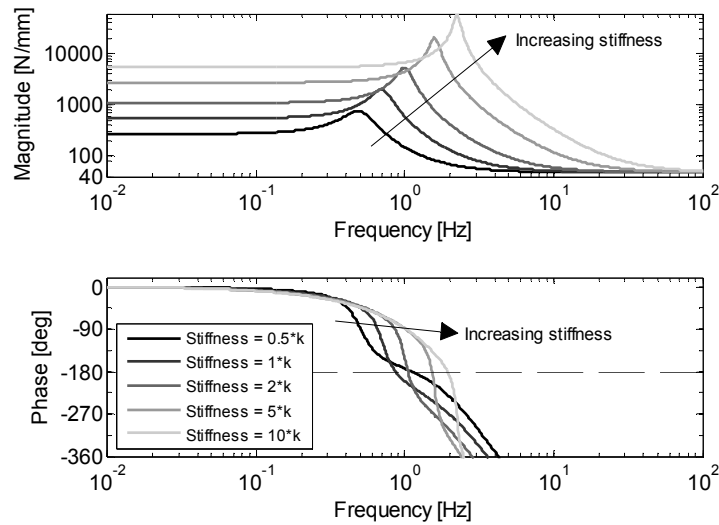
Time delays are caused by communication delays between the laptop that runs the control laws and the robot controller, internal delays in the robotic FSW system (e.g., communication to spindle motor drive) and transmission delays of the measured temperatures. These delays are inherent to the given system and could be slightly reduced if needed by employing more advanced hardware and software. In order to better cope with these delays, an advanced controller structure could be implemented, e.g., a Smith predictor. The effect of the time delay on the open-loop process model relating commanded vertical tool position to axial force is illustrated in the frequency response plot in Figure 71 (a). It can be seen that the magnitude is not affected by the delay, but that a frequency dependent phase lag is added. A time delay always reduces the system stability and limits the achievable bandwidth of a system.

The industrial robot used in this work is designed for a variety of manufacturing tasks, most of them require higher tool center point (TCP) velocities and lower loads as compared to most FSW applications. The inherent robot flexibility causes deflections in the serial robot linkages during FSW, resulting in position errors. *E.g.*, in order to achieve a plunge depth of approximately 5 mm, the tool position must be commanded to be approximately 11 mm below the workpiece surface. The robot's compliance was estimated to be in the order of 1 mm/kN, as compared to the CNC mill which has a

compliance of 0.05 mm/kN [23]. Figure 71 (b) shows the effect of varying stiffness on the open-loop process model relating commanded vertical tool position to axial force. It can be seen that for a higher stiffness, the natural frequency increases, as well as the phase - both resulting in a higher achievable closed-loop bandwidth.



(a)



(b)

Figure 71: Frequency response plot of open-loop process model of axial force relating to commanded vertical tool position when (a) varying the time delay and (b) varying the stiffness.

In order to further examine the effect of time delays and compliance, both delay and stiffness were varied and the maximum achievable crossover frequency was computed so that the system is still stable. Figure 72 plots the maximum achievable crossover frequency as a function of time delay and natural frequency. As shown above, the natural frequency is directly related to stiffness. It can be seen that the time delay in combination with the natural frequency (stiffness) affects the maximum crossover frequency and hence closed-loop bandwidth and system performance. For low natural frequencies (as experienced on the robotic system), the low stiffness is the dominating limitation and the time delay only plays a minor role. On setups with a high stiffness (such as a CNC mill), the time delay is the mayor limitation. For systems with low delays the stiffness has an important effect on the performance whereas for higher delays the delay becomes dominant. On the current setup (shown in Figure 72 in the lower right hand corner), both the delay and the compliance should be reduced in order to improve the performance of the control system.

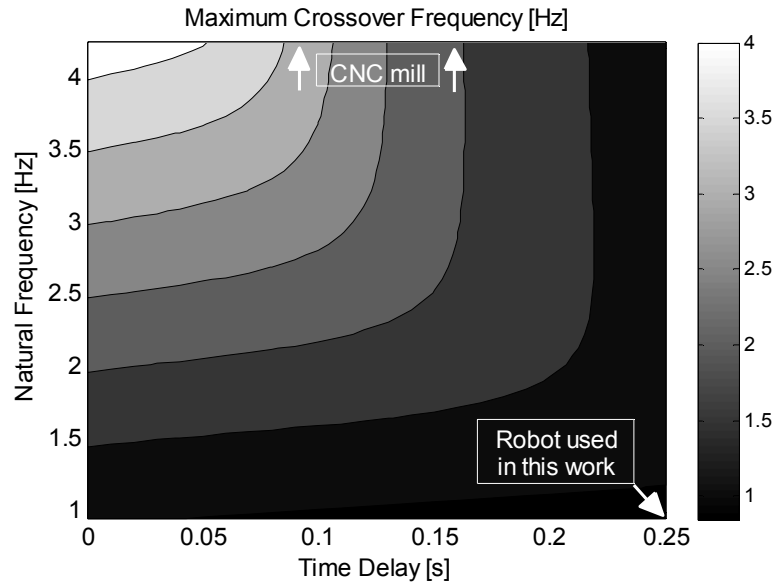


Figure 72: Maximum achievable crossover frequency (Hz) when varying the stiffness (*i.e.*, natural frequency) and time delay.

Loop update rates are also a limitation, *e.g.*, internal interrupts in the robot controller and bus communication to the spindle motor servo drive are executed at approximately 10 Hz and are not able to run faster because communication to external devices has lower priorities than motion and safety tasks in the given robot control architecture. In addition, the response of the actuators (spindle motor and robotic arm drive system) is limited, as well as the rate of change of the actuators due to restricted power being available to these devices. The outputs of the actuators are also limited to a certain range based on their design specifications.

6.5. Comparison Temperature Response Shoulder vs. Pin

This section investigates the differences in temperature response of the two measurement locations (shoulder and pin interfaces). This is of interest for the application of providing a feedback signal for temperature control systems.

From offline (*i.e.*, no FSW) experiments [41] it was found that the two embedded thermocouples have insignificant time delays (compared to their diffusive time constants) and that the thermocouple at the shoulder responds faster (diffusive time constant 11 ms) than the thermocouple at the pin (diffusive time constant 37 ms). From FSW experiments shown above (process response tests using spindle speed steps in Figure 57) it was found that the shoulder thermocouple is associated with a shorter process time constant (100 ms to 460 ms) than the pin thermocouple (300 ms to 510 ms). The process delay is dependent on communication between the control computer and the CNC mill and the wireless transmission between the rotating tool and the control computer and hence considered constant. This data shows how the embedded thermocouples themselves are responding relatively fast compared to the rather slow process.

When welding over two different backing plates of distinctly different thermal diffusivities but similar stiffness as illustrated in Figure 21, the measured interface temperatures decrease over the copper backing plate as seen in Figure 73 due to higher conduction heat transfer out of the weld zone. The pin temperature responds earlier than the shoulder temperature (approximately 130 ms or about two tool rotations earlier). In addition, the rate at which the temperature measured at the pin interface decreases is higher than at the shoulder interface. This can be attributed to the pin thermocouple being located physically closer to the backing plates than the shoulder thermocouple and therefore experiencing changes in conduction heat transfer earlier due to different thermal boundary conditions. Figure 73 also shows that the measured

interface temperatures capture thermal variations within one tool rotation. These temperature dynamics can be seen because of the direct contact of the thermocouple sheaths and the workpiece.

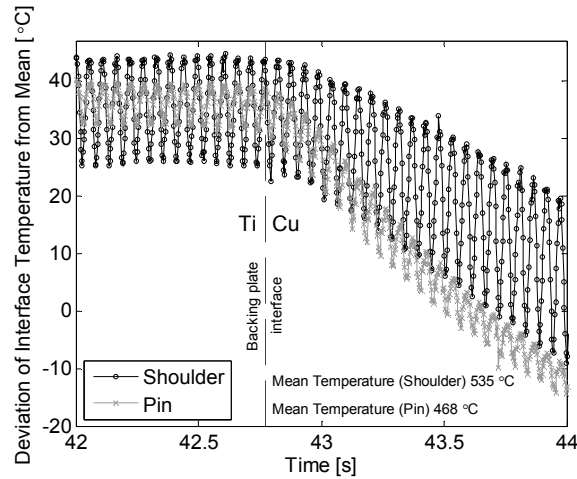


Figure 73: Deviation of interface temperature from the mean (of the traverse) when crossing a backing plate interface from titanium to copper. Conditions: 1100 rpm (constant), 200 mm/min (constant), 6.35 mm thick 6061-T6 (partial penetration weld).

Both shoulder and pin interface temperatures correlate similarly to weld quality when varying the spindle speed or travel speed (see section 7.2). When varying the plunge depth, the shoulder interface temperature seems a better indicator of weld quality because insufficient contact between the tool and the workpiece can be detected (see section 7.1). All performance metrics besides the backing plate transition speak for the thermocouple at the shoulder location. It can therefore be concluded that the shoulder interface temperature is better suited as a feedback signal for temperature control systems than the pin interface temperature. These results are summarized in Table 10.

Table 10: Comparison of shoulder and pin interface temperature responses. Sensor: Offline calibration [41], Process: During welding 6061-T6 of various workpiece cross-sectional areas as shown above.

		Shoulder	Pin
Sensor	Delay	Insignificant	Insignificant
	Time constant	11 ms	37 ms
Process	Delay	180 ms	180 ms
	Time constant	100 ms to 460 ms	300 ms to 510 ms
	Backing plate Transition	Slower response	Faster response
	Correlation to weld quality	Better	Worse

Chapter 7 Weld Quality

So far, it has not been investigated to which value the control system should regulate the interface temperature to in order to obtain a high quality weld. It is the goal of this chapter to investigate the relation of measured interface temperatures to weld quality by varying the main process parameters plunge depth, spindle speed, travel speed and thermal boundary conditions.

7.1. Effects of Plunge Depth on Weld Quality

When varying the plunge depth between 4.6 mm and 5.0 mm in 0.1 mm increments for welding 4.76 mm thick 6061-T6 (constant spindle speed of 1200 rpm and constant travel speed of 200 mm/min), it was found that 4.9 mm yields the highest ultimate tensile strength (UTS) (225 MPa or 70 % of the parent material, the parent material was tested to have a nominal UTS of 321 MPa). The weld surface of such a weld is shown in Figure 76 (a). Figure 74 shows that higher plunge depths result in higher shoulder and pin interface temperatures, due to more frictional and shear layer heat generation. For most welds, the average shoulder interface temperature is at least 20 °C higher than the average pin interface temperature with the exception of the lowest plunge depth (4.6 mm), for which the average pin interface temperature is over 50 °C higher. This weld has an unacceptable UTS (179 MPa or 56 % of the parent material) and a weld surface as shown in Figure 76 (b), which is a result of insufficient contact of

the tool shoulder with the workpiece. This lack of contact limits the heat generation at the shoulder and therefore causes lower shoulder interface temperatures. The interface temperature at the pin is higher than at the shoulder interface because the pin is still fully submerged into the workpiece.

Another indication of such a weld with insufficient shoulder-workpiece contact is found in the frequency domain as seen in the FFT plot of the shoulder interface temperature of the weld traverse in Figure 75. The magnitude at the 20 Hz frequency represents the temperature oscillations about the mean temperature at the spindle rotation rate (1200 rpm). The weld with 4.6 mm plunge depth shows higher magnitudes at all frequencies, especially at lower frequencies (below the frequency of spindle rotation) due to erratic tool-workpiece contact and workpiece consolidation. This indicates the potential for temperature measurements from this study to provide real-time monitoring of weld quality conditions.

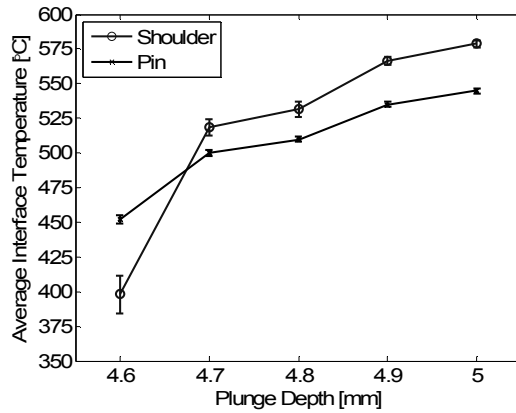


Figure 74: Average measured interface temperatures vs. plunge depth. Conditions: 1200 rpm (constant), 200 mm/min (constant), 6061-T6.

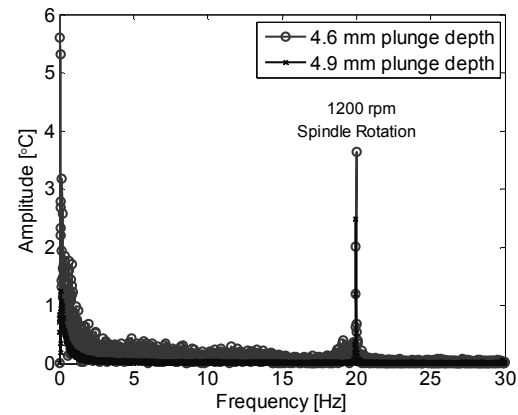
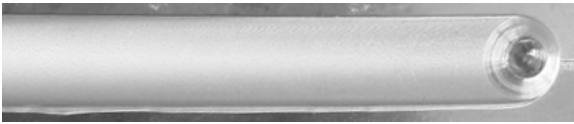
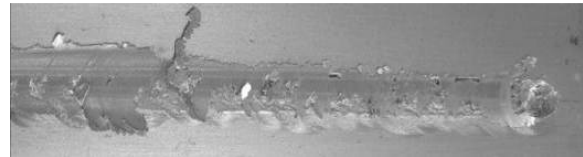


Figure 75: FFT plot of shoulder interface temperature for welds in 6061-T6 at two different plunge depths (1200 rpm, 200 mm/min).



(a)



(b)

Figure 76: (a) Nominal weld surface (plunge depth 4.9 mm). (b) Weld with insufficient plunge depth (4.6 mm). Workpiece material 6061-T6.

7.2. Effects of Spindle Speed and Travel Speed on Weld Quality

This section investigates the implications of spindle speed and the travel speed on the resulting weld quality for welding of 4.76 mm thick 6061-T6. Figure 32 shows the average temperatures experienced at the tool-shoulder and tool-pin interface respectively when varying the spindle speed and travel speed. It can be seen that the interface temperatures increase for higher spindle speeds due to more heat generation

by increased friction and plastic deformation. The temperatures also increase for lower travel speeds, due to more heat being deposited per unit weld length.

By varying the spindle speed and travel speed in the given tests, the shoulder temperature varies from 395 °C to 591 °C and the pin temperature from 389 °C to 580 °C. For all cases tested, the shoulder temperature is higher than or equal to the pin temperature. This is in agreement with the theory that in general, most heat is generated at the tool-shoulder interface. The measured temperatures approach the solidus temperature of 6061-T6 (582 °C) for higher heat inputs and in a few cases the shoulder temperature is above the solidus temperature, which suggests local melting at the tool-workpiece interface. The approach of the average shoulder interface temperature toward the solidus temperature as seen in Figure 32 supports the self-limiting theory during FSW [58], [59], *i.e.*, for higher temperatures, the metal flow stress decreases, resulting in lower spindle torque, reduced friction at the tool-workpiece interface and reduced heat generation, leading to lower temperature increases. The data also shows that for the parameter window chosen, varying the spindle speed results in a larger variation in interface temperature than varying the travel speed, which is an important result for developing closed-loop temperature control for FSW. For the temperature control system used in this work, the travel speed is held constant while the spindle speed is modified by the controller to adjust the heat generation.

Ultimate tensile strengths of the welds are plotted in Figure 77 and Figure 78 over the average measured shoulder and pin interface temperatures, respectively. The

change in temperatures results from a change in spindle speed from 700 rpm to 1700 rpm in 200 rpm increments. The plunge depth was constant at 4.9 mm for these tests. It was found that among the parameters tested, a spindle speed of 1100 rpm and a travel speed of 400 mm/min produces the strongest weld with a UTS of 245 MPa, or 76 % of the parent material. Most welds failed in the heat affected zone (HAZ) on the retreating side, but some welds failed in the weld zone (indicated with circles in the figures). It can be seen that all of the welds that failed in the weld zone are associated with average interface temperatures below a critical value (515 °C at the shoulder interface and 460 °C at the pin interface). It also shows that as the average interface temperatures (*i.e.*, spindle speeds) decrease, the UTS decreases, down to unacceptable values (below 25 % of the parent material). The only welds for which subsurface voids were observed in the macrographs (as seen in the cross-sectional image in Figure 79 and in the fracture surface in Figure 81 c) were the two welds with the lowest average shoulder interface temperatures (weld parameters of 700 rpm, 400 mm/min and 900 rpm, 500 mm/min). The voids reduce the load bearing area of the weld and limit the strength significantly. Figure 81 also shows the appearance of the fracture surface of the welds that failed in the HAZ (a) and welds that failed in the weld zone without the occurrence of voids (b). No surface voids occurred in any of the welds discussed in this section. These results demonstrate that the measured interface temperature and weld quality are closely correlated, but it also shows that it is not the only factor. Although the UTS decreases slightly for shoulder interface temperatures around the solidus temperature (possibly due to local melting), a certain 'optimum' interface temperature

cannot be formulated from the given data. The UTS of the welds that failed in the HAZ seems to be a function of the travel speed as shown in Figure 80. For the welds that failed in the HAZ, the UTS appears to reach a maximum for a travel speed of 400 mm/min.

This method is shown to help predict the weld quality based on the measured interface temperatures, hence providing valuable information for process monitoring systems. In addition, the temperature measurements can be used as feedback signals for closed-loop temperature control systems. Based on these results, a certain travel speed (400 mm/min in this case) can be chosen which produces the highest UTS and is held constant throughout welding. Desired shoulder and pin interface temperatures (533 °C and 482 °C, respectively in this case) can be formulated and can be maintained in the presence of disturbances by automatically regulating the spindle speed, which adjusts the heat input. The processing parameters might differ for other tools, workpiece alloys or properties.

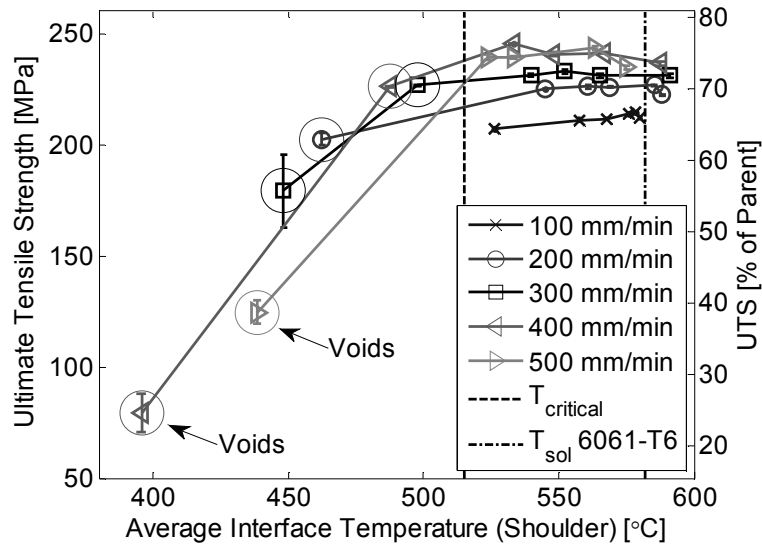


Figure 77: Ultimate tensile strength plotted over average shoulder interface temperature. The welds indicated with a circle failed in the weld zone instead of the heat affected zone. Workpiece material 6061-T6.

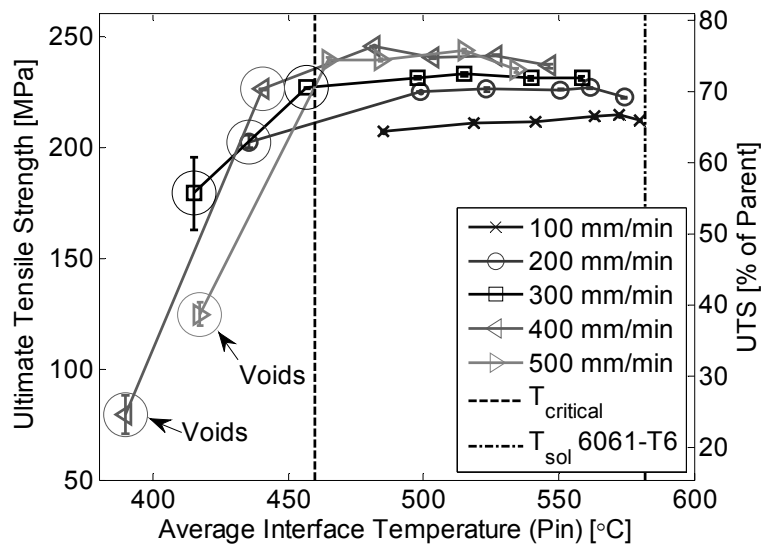


Figure 78: Ultimate tensile strength plotted over average pin interface temperature. The welds indicated with a circle failed in the weld zone instead of the heat affected zone. Workpiece material 6061-T6.

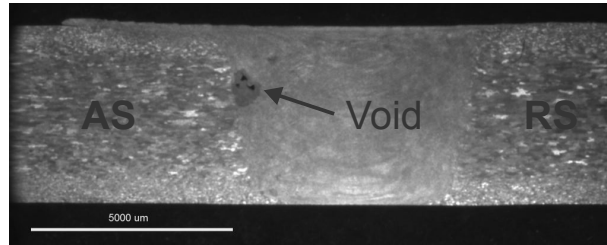


Figure 79: Cross-section of weld with insufficient heat input (900 rpm, 500 mm/min, 6061-T6, average shoulder interface temperature 439 °C).

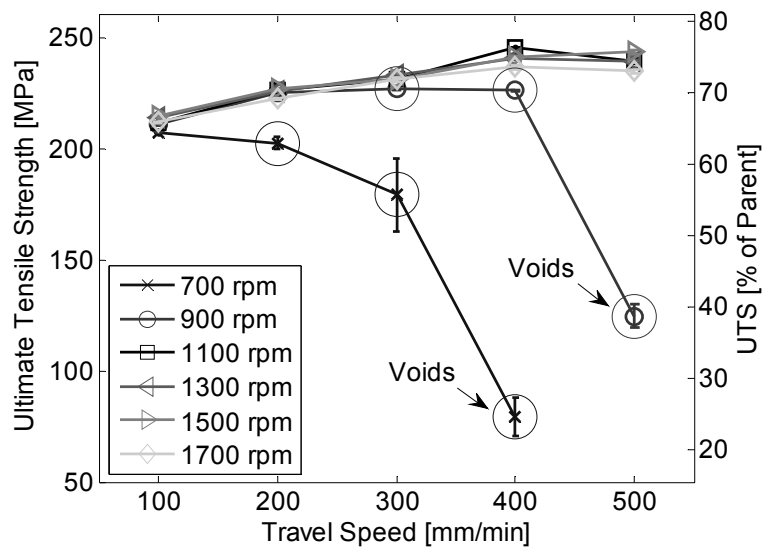


Figure 80: Ultimate tensile strength of welds produced with various spindle speeds and travel speeds plotted over travel speed. The welds indicated with a circle failed in the weld zone instead of the heat affected zone. Workpiece material 6061-T6.

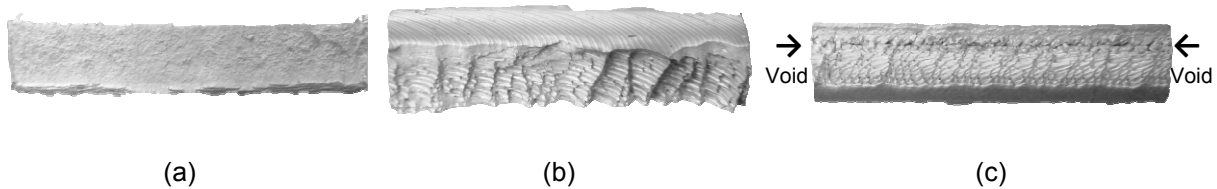


Figure 81: Fracture surfaces from tensile test specimen (6061-T6). (a) Measured interface temperature above $T_{critical}$ (failure in HAZ). (b) Measured interface temperature below $T_{critical}$ (failure in weld zone). (c) Weld with lowest measured interface temperature (failure in weld zone).

7.3. Effects of Thermal Boundary Conditions on Weld Quality

Welding over different backing plates with very distinct thermal diffusivities affects welding forces, spindle torque and measured temperatures. For constant weld parameters, traverse and axial forces as well as spindle torque increase in magnitude over a copper plate compared to a titanium backing plate [52]. Figure 82 and Figure 83 show the UTS of welds in 5 mm thick 5083-H111 performed at various spindle speeds, travel speeds and backing plates, plotted over the average shoulder and pin interface temperatures, respectively. The datapoints are labeled with the weld parameters (spindle speed followed after 'S' in rpm, travel speed followed after 'F' in mm/min and backing plate 'C' for copper, 'T' for titanium and 'S' for steel). It can be seen that the two welds with the lowest UTS are over a copper backing plate; these welds also have the lowest interface temperatures (shoulder and pin), have surface voids and fail in the weld zone, whereas all other welds fail in the parent material. Based on these observations, a critical temperature is proposed that separates these two welds with relatively low ultimate tensile strengths from the other welds with acceptable UTS. This temperature is 518 °C at the shoulder interface and 479 °C at the pin interface. These temperatures are comparable to the critical temperatures found above in section 7.2 for another alloy, 6061-T6 and different workpiece thickness, 4.76 mm (515 °C at the shoulder interface and 460 °C at the pin interface). This data shows that the backing plates create a thermal boundary condition, which significantly affects the weld quality. However, other process parameters can be used to counteract the effect of heat dissipation through the backing plates: e.g., the weld with the highest and lowest UTS are both welds performed

over a copper backing plate. These other process parameters (*e.g.*, the spindle speed) can be automatically adjusted by a temperature control system as described in [52] in order to maintain weld quality.

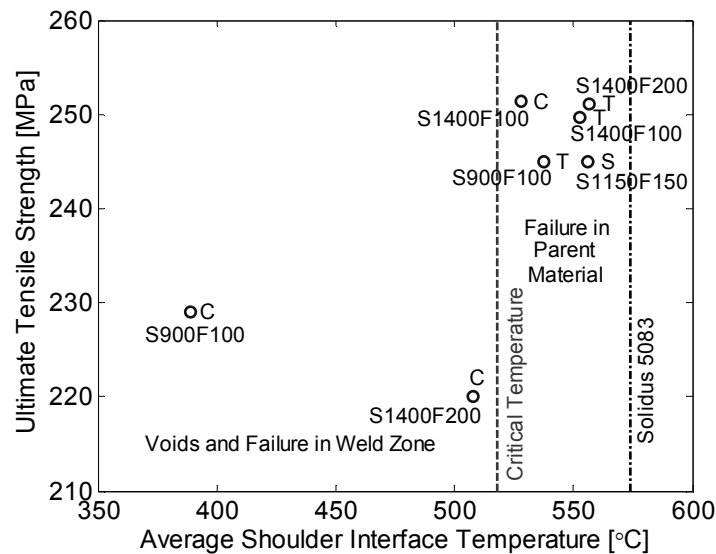


Figure 82: Ultimate tensile strength vs. average shoulder interface temperature for welds with varying spindle speed, travel speed and backing plate (5083-H111).

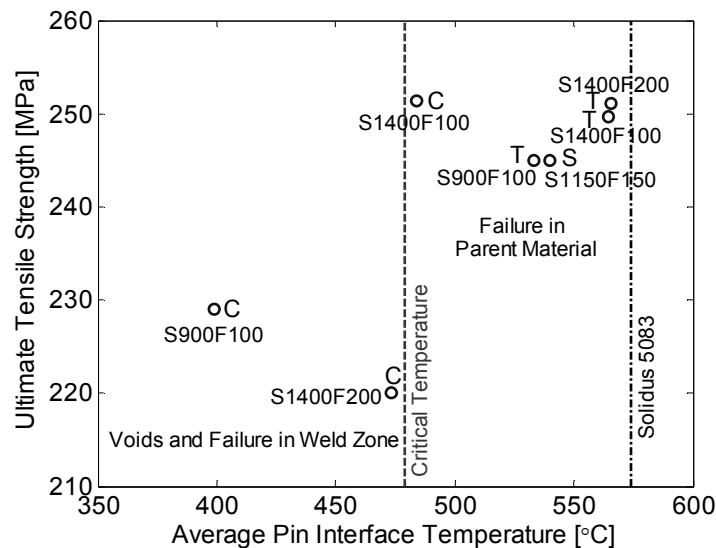


Figure 83: Ultimate tensile strength vs. average pin interface temperature for welds with varying spindle speed, travel speed and backing plate (5083-H111).

Grain sizes have been measured for the welds presented in this section. One micrograph example of the weld nugget region (5083-H111) is presented in Figure 84. Figure 85 shows the average grain size of the welds for various spindle speeds, travel speeds and backing plates over the average shoulder interface temperature. Because of the dynamic recrystallization due to the plastic deformation during the FSW process, the resulting grains are significantly reduced in size compared to the parent material, which has an average grain size of 700 μm . Higher weld zone temperatures cause some grain growth as seen in Figure 85. This data shows that lower grain sizes do not always cause welds with higher strength. The two welds with the two lowest grain size actually result in the two lowest weld strengths. A certain minimum temperature is required during FSW for flow stresses to reach low enough values, allowing proper material mixing and material flow.

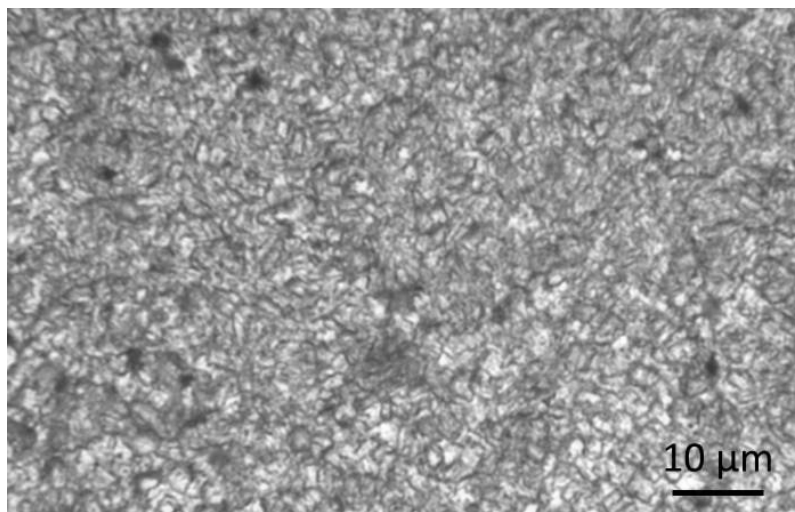


Figure 84: Micrograph of 5083-H111 in weld nugget region used to determine the grain size.

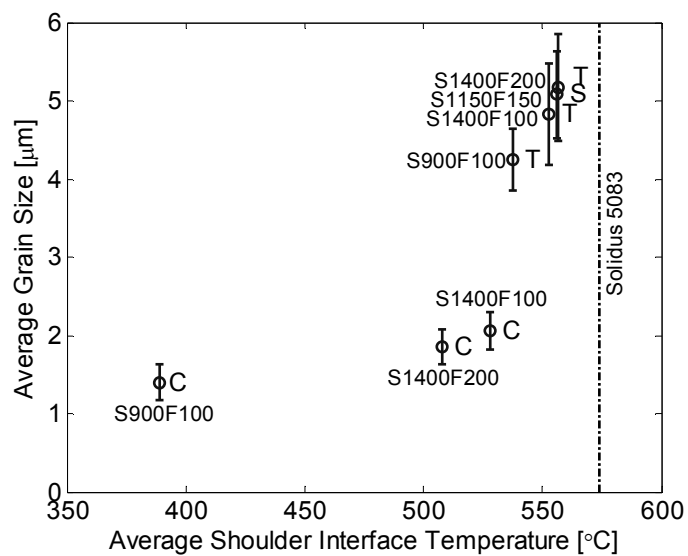


Figure 85: Average grain size plotted over average shoulder interface temperature for welds with varying spindle speed, travel speed and backing plate (5083-H111).

Chapter 8 Conclusions

8.1. Temperature Measurement Method

Through-holes for placing two thermocouples at the tool-workpiece interface were used for the first time on a seam weld. Using through-holes enables the thermocouple sheaths to be in direct contact with the workpiece material. The through-hole thermocouple placement was combined for the first time with wireless data acquisition to capture weld temperature variations (*i.e.*, process dynamics) at the tool-workpiece interface in real-time at a sample rate of 333 Hz. Variation of the interface temperature within one rotation of the tool was experimentally observed for the first time. The temperature at the shoulder-workpiece interface is oscillating at the same frequency as the spindle is rotating. With the current sample rate of 333 Hz and a spindle speed of 1000 rpm, the system is capturing 20 temperature measurements per rotation of the tool.

The temperature measurement technique was shown to work reliably over a wide range of operating conditions (various spindle speeds, travel speeds and plunge depths) for several aluminum alloys and revealed further insight into the process dynamics. The system can be used for process monitoring and detect the occurrence of local melting (exceeding the solidus temperature) at the tool-workpiece interface and hence can limit the degradation of weld quality. Results from this work support the self-limiting theory

during FSW, *i.e.*, for higher temperatures, the metal flow stress decreases, resulting in lower spindle torque, reduced friction at the tool-workpiece interface and reduced heat generation. However, temperatures at the shoulder-workpiece interface exceed the solidus temperature for some conditions.

The temperature distribution underneath the shoulder and around the pin was experimentally measured in great spatial and temporal detail for the first time. It was found that the highest temperatures are at the shoulder interface between the advancing side and the trailing edge of the tool, closer to the advancing side. The temperature distribution was mostly affected by travel speed, also affected by plunge depth but spindle speed showed an insignificant influence (among the tested parameter ranges). The temperature difference within one tool rotation was found to be between 10 °C and 50 °C, depending on the process parameters.

It was shown how different aluminum alloys require different welding parameters to maintain desirable weld temperatures. The temperature measurement strategy can assist in process monitoring and developing process parameters for various welding tasks (*e.g.*, different workpiece alloys, geometries, *etc.*).

The experimental results from this work have shown to be useful in improving the process understanding and the measurement method can also be a valuable tool to verify thermal models and to establish weld parameters when welding different or dissimilar workpiece materials or geometries by monitoring the interface temperatures.

To date, the instrumented tool holder with tool A has been successfully operated with a single thermocouple for a total weld length of over 35 m (tool B over 22 m and tool C over 7 m). This emphasizes the durability of the through-hole strategy for locating the thermocouple at the tool-workpiece interface. The thermocouple can even be slightly recessed into the through-hole without having a significant effect on the measured response time: the process pressures will fill the recess with high thermal diffusivity aluminum alloy. *I.e.*, temperature changes will travel through that small amount of aluminum very quickly, as compared to a similar amount of tool steel. Recessing the thermocouple will also enable measurement of more abrasive alloys without significant danger posed to the thermocouple since it will not experience continuous abrasion, after the recess is filled with workpiece material.

8.2. *Process Modeling*

A first order plant model with a transport delay was developed for 3.18 mm thick 6061-T6 relating spindle speed and interface temperature over the applied frequencies of 0.1 Hz to 3 Hz. Travel speed was not found to significantly affect interface temperature for 6061-T6 using the tool and operation conditions in this study. Thus travel speed was not suitable as an input to the process model for these conditions. Applying the developed process model, an integral controller was designed for FSW and shown to automatically adjust the spindle speed in order to achieve the desired interface temperature.

A first order linear multi-input-multi-output process model has been shown to model the dynamic relations between spindle speed and vertical tool position to interface temperature and axial force. It was found that the interface temperature is affected by both inputs but that the effect of spindle speed on the axial force is negligible.

8.3. Closed-Loop Control System

The ability of this controller to track a desired temperature was demonstrated by commanding step changes in interface temperature, commanding a sinusoidal change in temperature, and commanding a constant temperature when welding over two very different backing plates. The controller performed very well at maintaining the commanded temperature (within ± 5 °C after crossing the backing plate interface compared to an interface temperature change of 50 °C without the controller. It was demonstrated that closed-loop control of the tool-workpiece interface temperature is feasible.

The results of the study have shown the effects of FSW process parameters (spindle and travel speed) on the measured tool-workpiece interface temperature for aluminum alloys 2024-T3, 5083-H116, 6061-T6 and 7075-T6. Spindle and travel speed amplitudes have been chosen in order to maintain the same range of feeds; however, their impact on measured temperature change differs, hence each alloy will require a different process model and controller. For alloys 2024-T3, 6061-T6 and 7075-T6, spindle speed has been found to have the most significant effect on temperature change and controlled response. On the other hand, 5083-H116 (a non-heat treatable alloy) not

only has the lowest temperature amplitude response to spindle speed change among the alloys, but also shows similar effects when changing travel speed. Temperature control using spindle speed changes can also be implemented for aluminum alloys 2024-T3 and 7075-T6, but due to the low response in temperature it will be very challenging to develop temperature control for 5083-H116 with the parameters investigated in this study.

Step response tests were performed to rapidly determine dynamic process models for workpieces with varying cross-sectional area. Simple first order models with a time delay were established that are able to capture the interface temperature dynamics as a result of changing spindle speeds. The method using step inputs greatly reduces the time and effort to create dynamic process models for new welding setups (different workpieces, fixturing, FSW tool, *etc.*) compared to the previous method (one frequency at a time). It was shown that the closed-loop temperature control system developed previously can also be successfully applied to workpieces with larger cross-sectional area. This implies that an approximate knowledge of the dynamic process model is sufficient to control the interface temperature.

It was found that the thermocouple at the shoulder interface has a shorter time constant both offline (embedded sensor only) and during the FSW process than the thermocouple at the pin interface. In addition, the shoulder interface temperature correlates better to weld quality, which makes this temperature more favorable as a feedback signal for the use in temperature control systems than the pin interface temperature in order to achieve the best performance.

The experiments from this study showed that the weld quality is strongly dependent on the interface temperatures (range in ultimate tensile strength from 25 % to 76 % of parent material), which are mostly affected by spindle speed and thermal boundary conditions. It was found that for the welds that failed in the heat affected zone, the travel speed has also an effect on the ultimate tensile strength (range in ultimate tensile strength from 66 % to 76 % of parent material). An important result is that based on the measured interface temperature it can be predicted if the weld will fail in the heat affected zone or in the weld zone. Based on the results, a desired interface temperature can be formulated and can be maintained in the presence of disturbances by automatically regulating the spindle speed, which adjusts the heat input. It was also demonstrated that the temperature measurement strategy could be used to indicate welds with insufficient shoulder-workpiece contact, thus potentially identifying and preventing welds with detrimental weld quality due to lack of penetration.

It was shown that backing plates of different thermal diffusivity changes the heat flow out of the weld zone and causes significantly different interface temperatures, which affects the weld quality. By changing other process parameters, e.g., through a temperature control system, weld quality can be maintained in the presence of such changing thermal boundary conditions.

8.4. Transfer to Robotic FSW System

A wireless data acquisition system was built to collect temperature measurements off a rotating tool (up to 2900 rpm) on a robotic FSW system. Two through-holes for

placing the thermocouples at the tool-workpiece interface were used for the first time on a robotic FSW system, which enables the thermocouple sheaths to be in direct contact with the workpiece material. The system captures weld temperature variations (*i.e.*, process dynamics) at the tool-workpiece interface in real-time.

Closed-loop temperature control can be implemented on a robotic FSW system with the main differences and limitations being the low stiffness of the robotic manipulator and increased communication delays on the robotic system causing lower achievable closed-loop bandwidths than on a CNC mill. Although limitations being present on the given hard- and software, the closed-loop system responds reasonably fast and should suffice for most practical applications.

A closed-loop controller was implemented that combines temperature and force control on the robotic FSW system. The controller decouples the cross-coupled process inputs to simultaneously maintain a desired axial force and a desired interface temperature. The performance of the combined control system was demonstrated with successful command tracking and disturbance rejection. It can be concluded that a combination of force and temperature control can be realized on a robotic FSW system. Within a certain range, desired axial forces and interface temperatures are achieved by automatically adjusting the spindle speed and the vertical tool position at the same time. The axial force and interface temperature can be maintained during both thermal and geometric disturbances and thus weld quality can be maintained for a variety of conditions in which each control strategy applied independently could fail. The

developed system is believed to have many potential applications for production in industrial settings.

Chapter 9 Future Work

9.1. *Future Research Ideas*

9.1.1. *Application of Temperature Measurement System for Process Monitoring*

Using the temperature measurement strategy developed in this work, process dynamics can be captured in great detail and closed-loop control of temperature has been successfully demonstrated. The temperature measurements can also be utilized as an input for a process monitoring system, which would provide the operator with valuable feedback about any irregularities detected during welding (Figure 86). With such information, the operator can decide whether to subject the part to more quality assessment, *e.g.*, non-destructive testing (which might be too time consuming or costly to be performed on every part), repair the part or reject the part. Due to its high spatial and temporal resolution, the temperature measurement strategy presented in this work might be an ideal, inexpensive instrument for such process monitoring tasks. Correlations between the measured shoulder and pin temperatures and common weld defects (such as surface voids, wormholes, lack of penetration, *etc.*) need to be found (time domain and/or frequency domain).

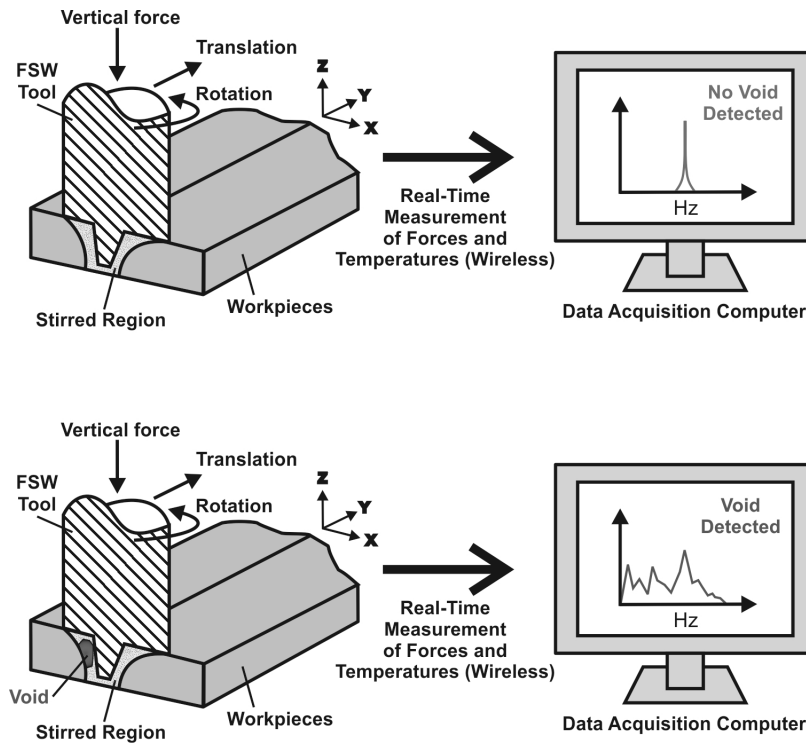


Figure 86: Schematic of real-time process monitoring system to detect weld flaws.

Preliminary work has shown that the temperature measurement method is able to detect a gap between the surfaces to be joined in a butt weld (Figure 87). The gap width increased from 0 mm to 2.5 mm and leads to significant surface voids as seen in the weld surfaces in Figure 88.

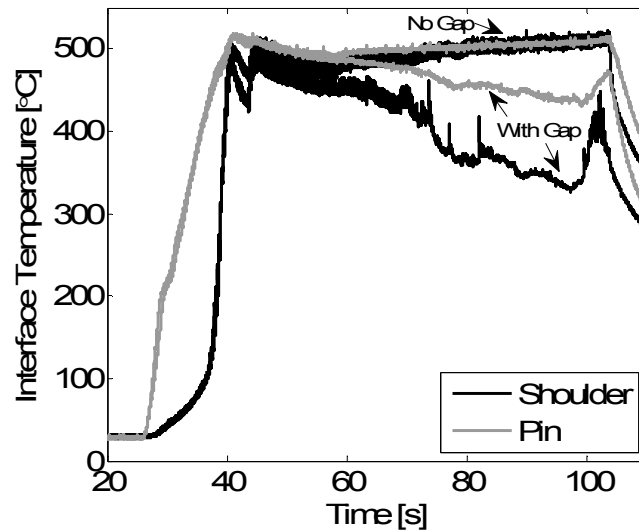


Figure 87: Measured interface temperatures at shoulder and pin location when welding over an increasing gap (0 mm to 2.5 mm) vs. no gap (5083-H111).

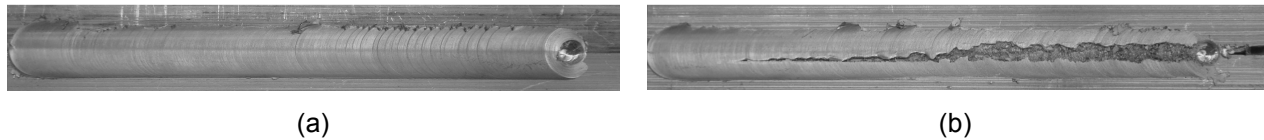


Figure 88: Weld surface for weld with (a) no gap and (b) weld with increasing gap.

9.1.2. Improved Calibration of Temperature Measurement Method

The measurement method using tool-embedded thermocouples has been calibrated using a modified laser-flash method [41] to determine diffusive time constants which are used to back-calculate the true amplitude and phase relations of the interface temperatures. To create similar conditions as during the FSW process, the calibration has been performed with the thermocouples embedded in the tool, however, it was done at room temperature. Since many thermal properties are temperature dependent, a calibration at elevated temperatures (in the order of the temperatures experienced during the FSW process) should be performed to obtain a more accurate calibration.

9.1.3. Implementation of More Advanced Control Algorithms

The automatic control of temperature and force was shown to maintain weld quality in certain cases such as varying thermal and geometrical boundary conditions. However, there are many more situations in real-world manufacturing environments that can adversely affect weld quality. One example is the occurrence of gaps between the abutting surfaces. Previous work has shown that weld quality can be increased by changing the travel angle while encountering the gaps [23]. A shared human-computer approach has also been developed that adjusts the travel angle based on the knowledge of a gap [69]. The force and temperature control system could be combined with such a gap compensation control system to further improve weld quality under varying conditions in unstructured environments.

The temperature control system could also be applied to maintain the surface quality of welds. *E.g.*, for some consumer products, the visual appearance of the finished weld might be of greater interest than to generate a weld with superior strength without any internal voids. The temperature could be regulated to a certain value that results in a good surface finish. A qualitative relation between temperature and surface quality has been reported by Gratecap *et al.* [14].

Apart from joining identical alloys, which is done most commonly, there is a growing interest in joining dissimilar alloys for tailored product properties, which can result in advantages such as weight reductions or cost savings. Due to different melting temperatures and shear strengths of the dissimilar alloys, finding proper welding parameters can be challenging. The temperature measurement method developed in

this work could be used to determine the temperatures in each alloy when butt-welding dissimilar aluminum alloys and a lateral tool offset can be applied to alter the heat input symmetry along the weld path in real-time (Figure 89).

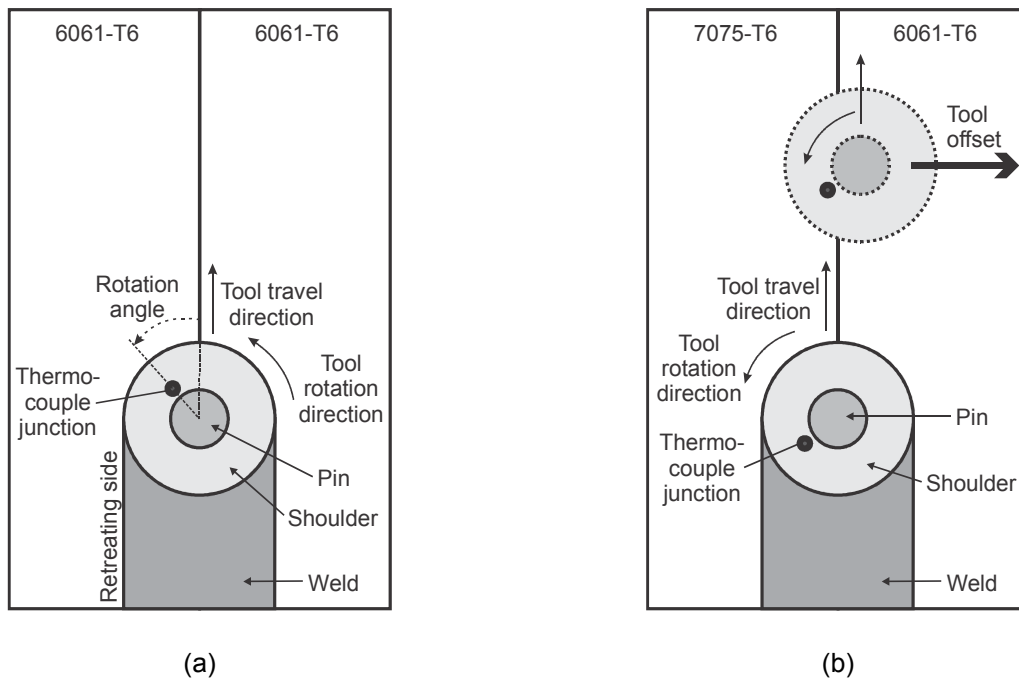


Figure 89: Schematic of welding (a) same alloy with tool tracking along seam and (b) dissimilar alloys and tool traveling with lateral offset.

Another possible improvement of the control system is the addition of a state observer to the current setup (Figure 90). Since a model of the physical process is available, a real-time simulation of the process can be implemented in the control software that allows the estimation of states that are not accessible on the real system, *e.g.*, the interface temperature without the transport delay or the disturbance (*e.g.*, in Watts) due to varying thermal boundary conditions. Estimated states can be used as feedback signals for the closed-loop controller instead of using the measured state, which can lead to performance improvements. It could be investigated how the

estimation of these states can benefit in increasing the performance of the control system. Preliminary work shows how the shoulder interface temperature can be estimated in real-time (Figure 91). It should be noted that the tracking is fairly slow in this experiment because the observer gain was not optimized.

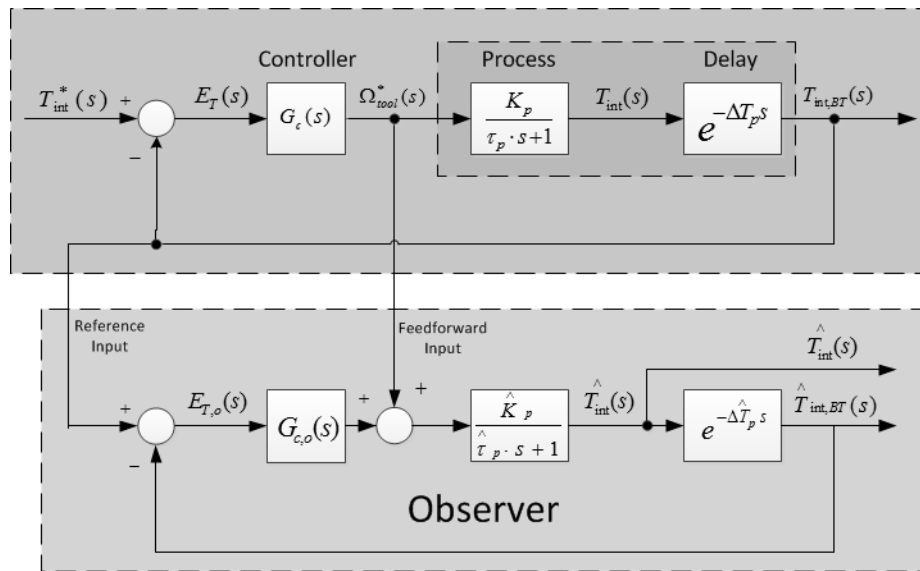


Figure 90: Simple observer as an addition to the current closed-loop control system.

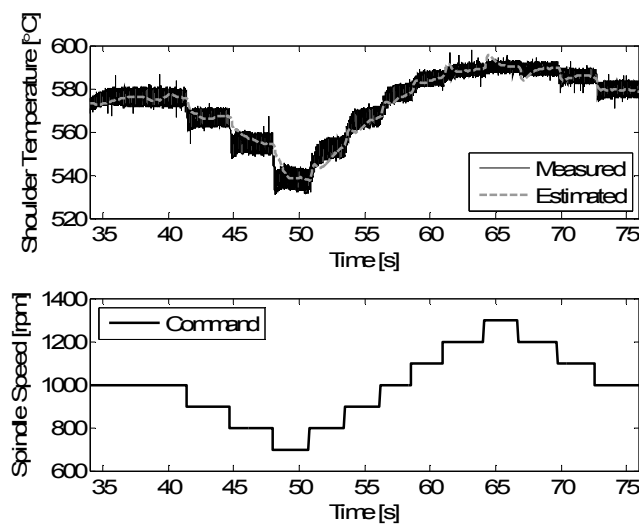


Figure 91: Measured and estimated shoulder temperature from observer when commanding different spindle speeds.

9.2. Further Technologie Improvements

9.2.1. Simplified Tool and Thermocouple Connectivity

Currently, the 0.25 mm diameter thermocouple sheaths exit the through-holes on the side of the FSW tool, approximately 5 mm from the edge of the shoulder (Figure 92 a). This design decreases the aspect ratio of the holes, allowing easier manufacturing. The exposed sheaths have been protected by a layer of thermocouple cement (Figure 5b and Figure 6b) and high-temperature tape, but there is still risk of damage to the sheaths when welding at high plunge depths or when excessive flash occurs. When welding on the robotic FSW system, more vibrations are another hazard that limit the robustness of the current method. Another limitation is the fact that the FSW tools cannot be easily changed because the tool with the attached thermocouple wires needs to be threaded into the tool holder.

A future design could overcome these limitations to increase the durability, decrease maintenance efforts and enable easier tool changes. Instead of exiting the tool on the shank, the thermocouple wires could remain inside the tool in a longer through-hole to exit on its back (Figure 92 b). At the rear end of the tool, the thermocouple signals could travel through a connector interface into the tool holder, where they are routed to the custom circuit board for processing. This would allow protection of the thermocouple sheaths and easier tool changes. Challenges are the manufacturing of through-holes with higher aspect ratios and the limited space available for the electrical connections between the FSW tool and tool holder. The contact

interface for the sensor signals would have to be designed to allow easy and secure mating between a connector on the back of the FSW tool and a counterpart terminal located on the tool holder. The signal wires and contacts need to be made of the same materials as the wires forming the thermocouple junction to allow accurate measurements without any intermediate junctions.

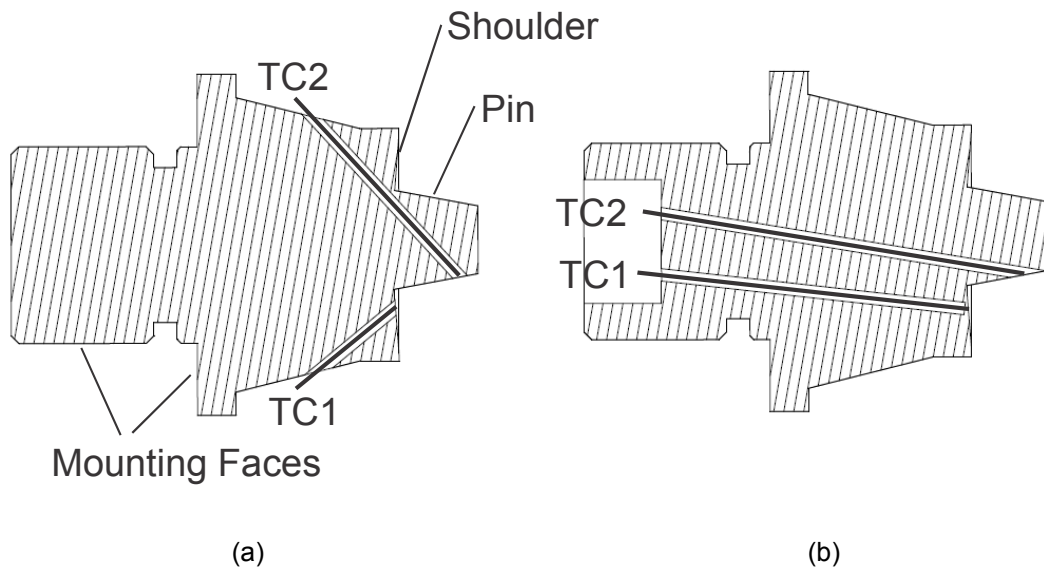


Figure 92: (a) Current instrumented FSW tool and (b) possible future design. The tool is connected mechanically to the tool holder through the mounting faces. TC = Thermocouple.

9.2.2. Other Improvements of Instrumented Tool Holder

As of now, the system is able to capture temperature measurements with an angular resolution of 13 degrees to 31 degrees (700 rpm to 1700 rpm spindle speed, respectively). For some applications, an improved angular resolution might be desirable for better insight into the process dynamics, especially at higher spindle speeds. To enable this, higher sampling and wireless transmission rates need to be implemented, by employing other hardware.

The current size of the electronic components and battery that perform the signal conditioning and transmission of the temperature data requires a fairly large tool holder to be used for mounting of the parts. It would be desirable to reduce the size of the circuit boards (e.g., by using surface mount technology) and find components with a smaller power consumption to reduce the size of the required battery.

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APPENDICES

APPENDIX A – Publication List

Journal Articles

- A. Fehrenbacher, C. Smith, N. Duffie, N. Ferrier, F. Pfefferkorn, and M. Zinn, “Combined Temperature and Force Control for Robotic Friction Stir Welding,” *Paper in preparation*, 2012.
- A. Fehrenbacher, N. Duffie, N. Ferrier, F. Pfefferkorn, and M. Zinn, “Closed-Loop Control of Temperature in Friction Stir Welding and its Effect on Weld Quality,” *Paper in preparation*, 2012.
- A. Fehrenbacher, J. Schmale, M. Zinn, and F. Pfefferkorn, “Measurement of Tool-Workpiece Interface Temperature Distribution in Friction Stir Welding,” *Paper in preparation*, 2012.
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- A. Fehrenbacher, N. A. Duffie, N. J. Ferrier, F. E. Pfefferkorn, and M. R. Zinn, “Toward Automation of Friction Stir Welding Through Temperature Measurement and Closed-Loop Control,” *J. Manuf. Sci. Eng.*, vol. 133, no. 5, pp. 051008–12, Oct. 2011.
- F. E. Pfefferkorn et al., “Characterization of Exposures to Airborne Nanoscale Particles During Friction Stir Welding of Aluminum,” *Annals of Occupational Hygiene*, vol. 54, no. 5, pp. 486-503, Jul. 2010.

Conference Proceedings

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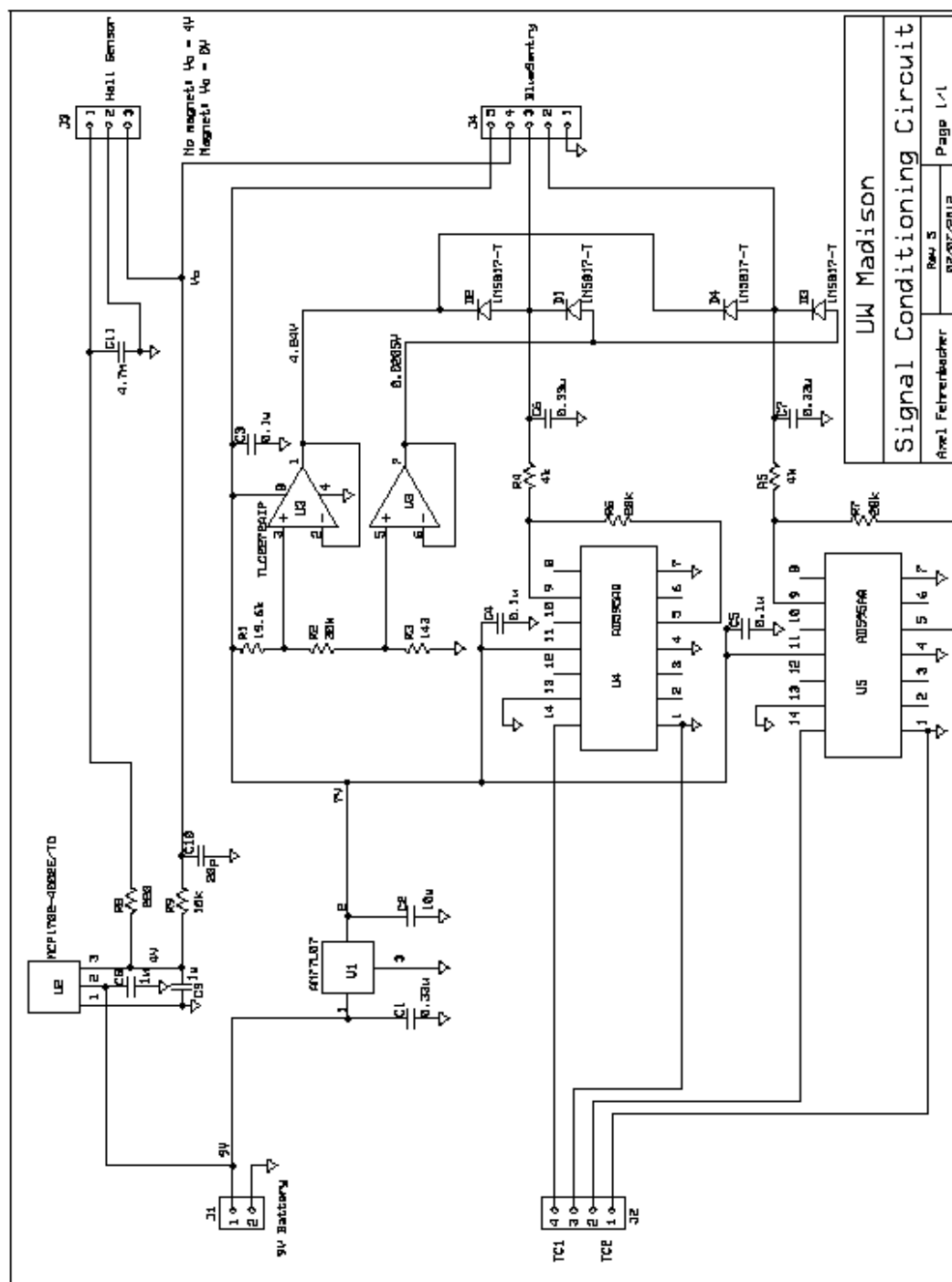
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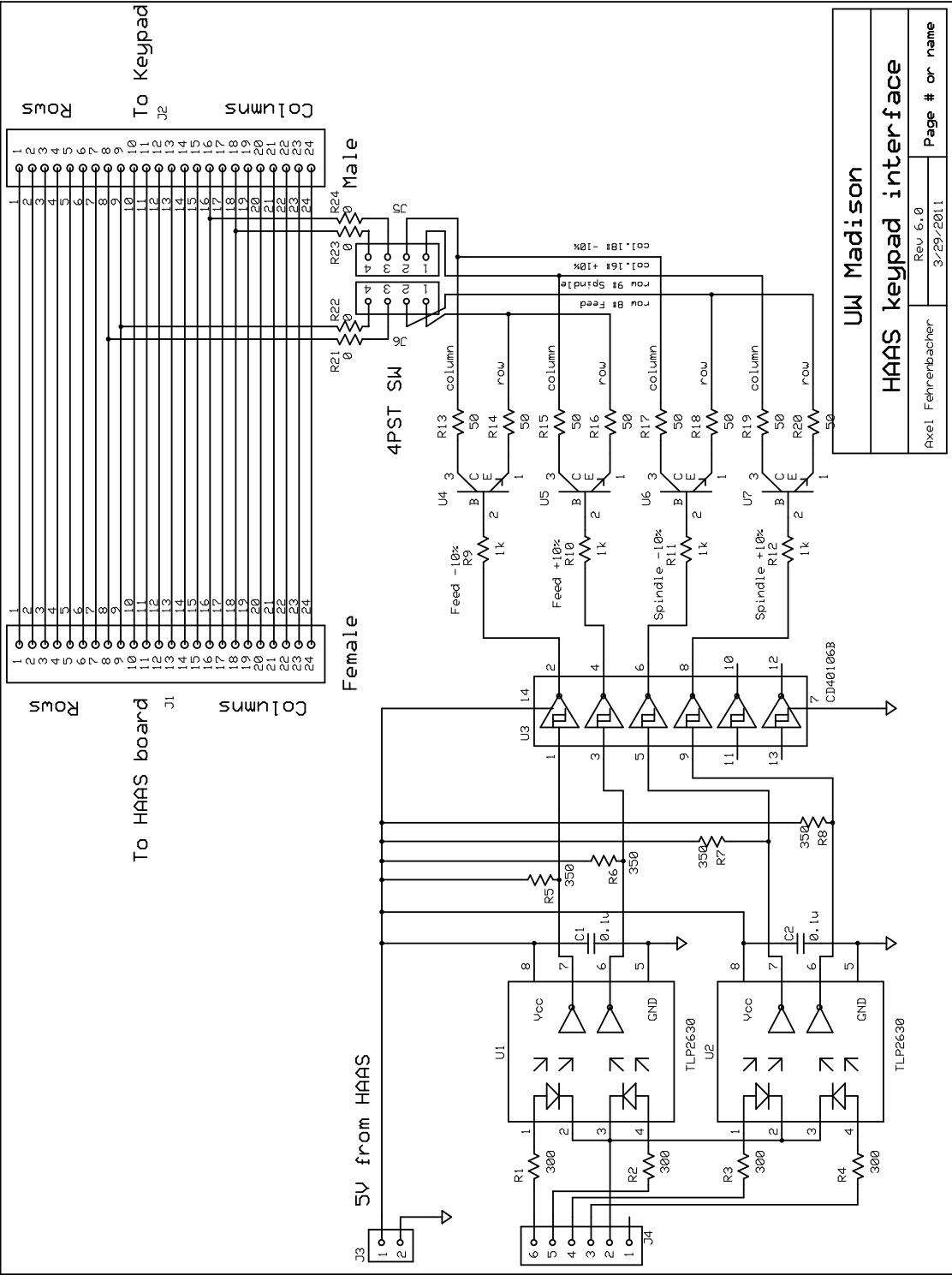
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APPENDIX B – Custom Circuit Board for Instrumented Tool Holder



APPENDIX C – Custom CNC Mill Keypad Interface



APPENDIX D – Measurement Uncertainty

Table 11: Summary of measurement uncertainties on CNC mill testbed.

Variable	Source	Estimate	Method of Determination	Total Uncertainty
Force	3-Axis Dynamometer	F_x and F_y : ± 7.65 N F_z : ± 8.08 N	DAQ uncertainty was calculated. Others were given by manufacturer's calibration.	F_x : ± 8.4 N F_y : ± 9.4 N F_z : ± 36 N
	Charge Amplifier	± 0.5 %		
	A/D (DAQ)	± 0.000076 V		
Spindle Moment	Current Probe	± 0.5 %	DAQ uncertainty was calculated. Others were given by manufacturer's calibration.	± 1.2 Nm
	2-Axis Dynamometer	± 1.04 %		
	Charge Amplifier	± 0.5 %		
	A/D (DAQ)	± 0.000076 V		
Spindle Power	Moment	± 0.8 Nm	Moment uncertainty was used from above. Spindle speed uncertainty was found from CNC mill.	± 91 W
	Spindle Speed	± 5 rpm		
Temperature	Type K Thermocouple	± 2.2 °C or 0.75 %	Given by manufacturer's calibration.	± 4.5 °C
	Thermocouple Amplifier	± 1 °C		
	Resistor	± 0.1 %		
	A/D	± 0.00052 V		
Tool Position	CNC Mill	± 0.0005 mm	Half of the smallest increment the mill is able to move.	± 0.0005 mm
Ultimate Tensile Strength	Tensile Tester	± 0.25 %	Given by manufacturer's calibration.	± 0.89 MPa
	Caliper	± 0.0127 mm		

APPENDIX E – Calibration of Spindle Torque Measurement

For the spindle torque measurements, a current probe is used, which needs to be calibrated. The current probe measures the current in one of the leads connecting the spindle motor controller to the spindle motor and outputs a voltage proportional to the measured current. The current probe has two scaling settings, of 1 mV/mA and 10 mV/A, where the latter is used to generate a voltage signal in a range that can be measured with the DAQ system.

The torque calibration is done with a 2-component torque dynamometer (type Kistler 9271A), which measures downward forces and moments around the vertical axis. An aluminum block is bolted on top of the torque dynamometer and is grinded by using a sanding disk (# 50), which is clamped into the mill spindle and moved downwards while rotating to increase the loads. Tests are performed for a set of 7 different rotation speeds: 500 rpm, 1000 rpm, 1500 rpm, 2000 rpm, 2250 rpm, 2500 rpm and 3000 rpm. For every speed, the sanding disk is manually moved downward by 0.1 mm increments, increasing the downward force and the spindle moment. After performing these tests, the moments M_Z measured with the torque dynamometer and the voltage output of the current probe v_{cp} are correlated for different spindle rotation speeds N . With the help of MATLAB™, a function of the form $M_Z = f(N, v_{cp})$ is extrapolated. This surface function is plotted in Figure 93. During FSW, the LabVIEW™ DAQ program computes the spindle

moment as a function of spindle rotation speed and current probe voltage. The function was determined to be:

$$M_z = (0.00000887 \cdot N^2 - 0.0683 \cdot N + 153) \cdot v_{cp} - 0.00000118 \cdot N^2 + 0.00683 \cdot N - 10.2 \quad (15)$$

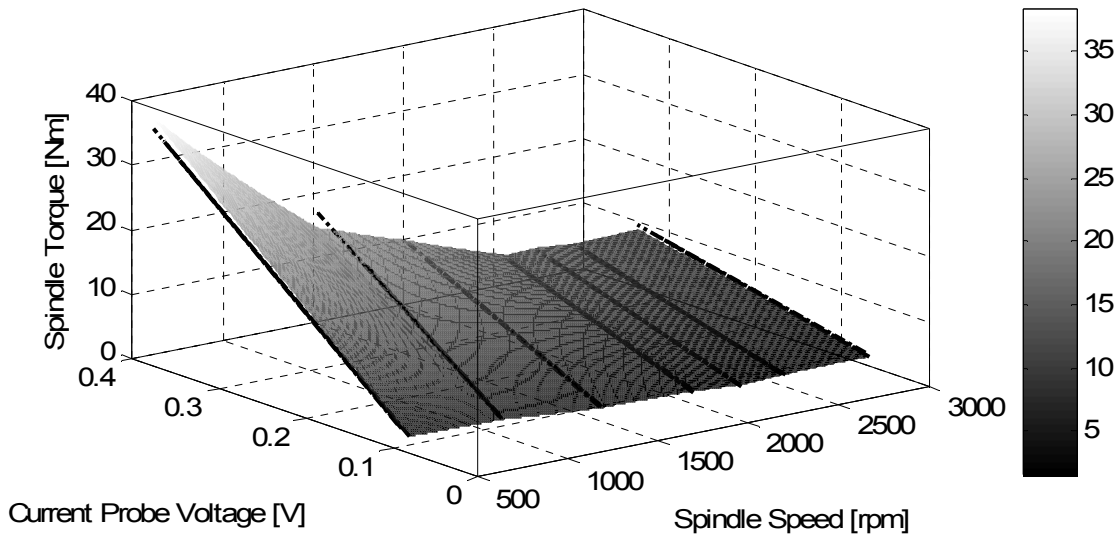


Figure 93: Spindle torque vs. current probe voltage and spindle rotation speed.

APPENDIX F – Custom LabVIEW™ GUI

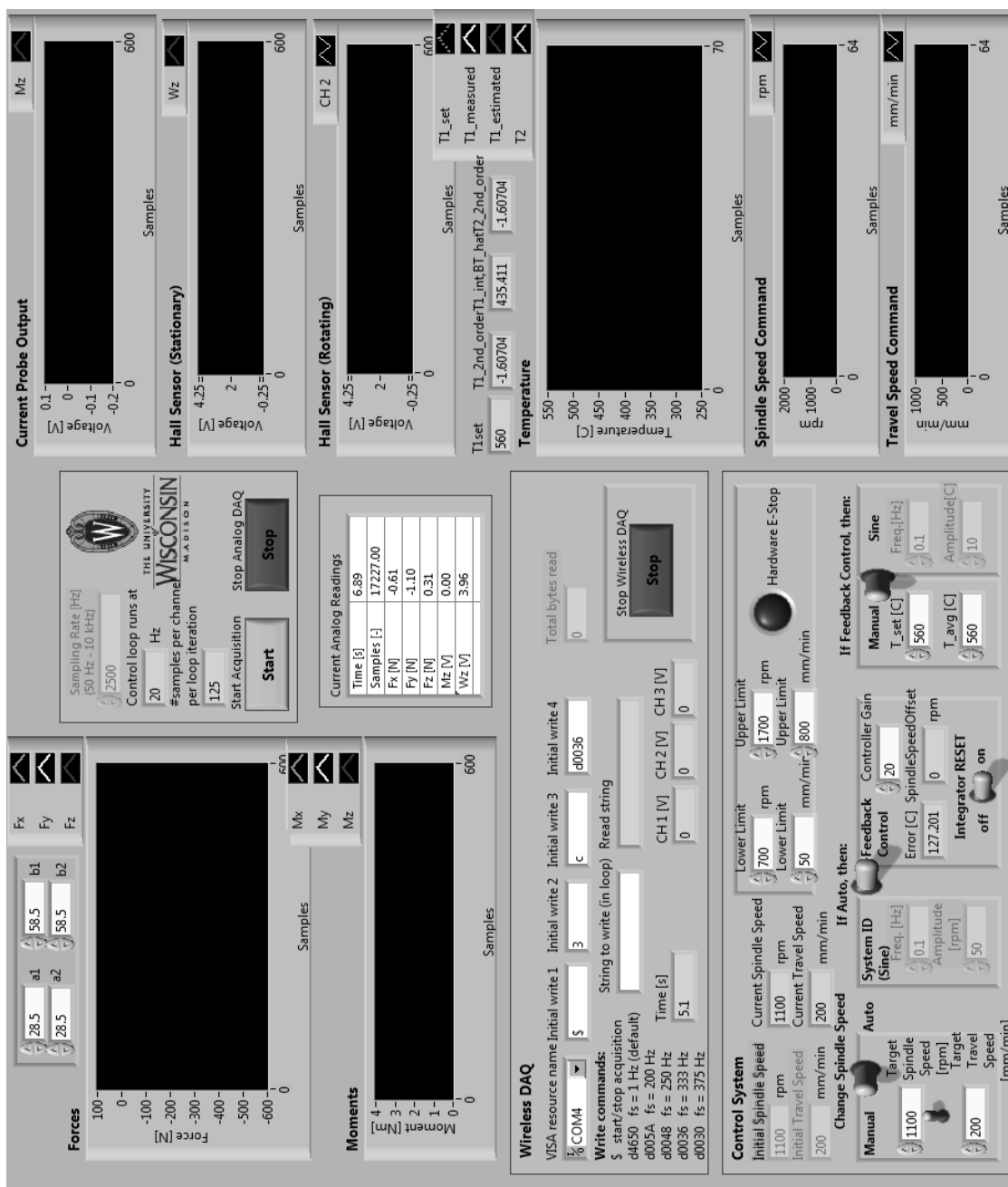


Figure 94: Front panel GUI of custom LabVIEW™ software (CNC mill testbed).

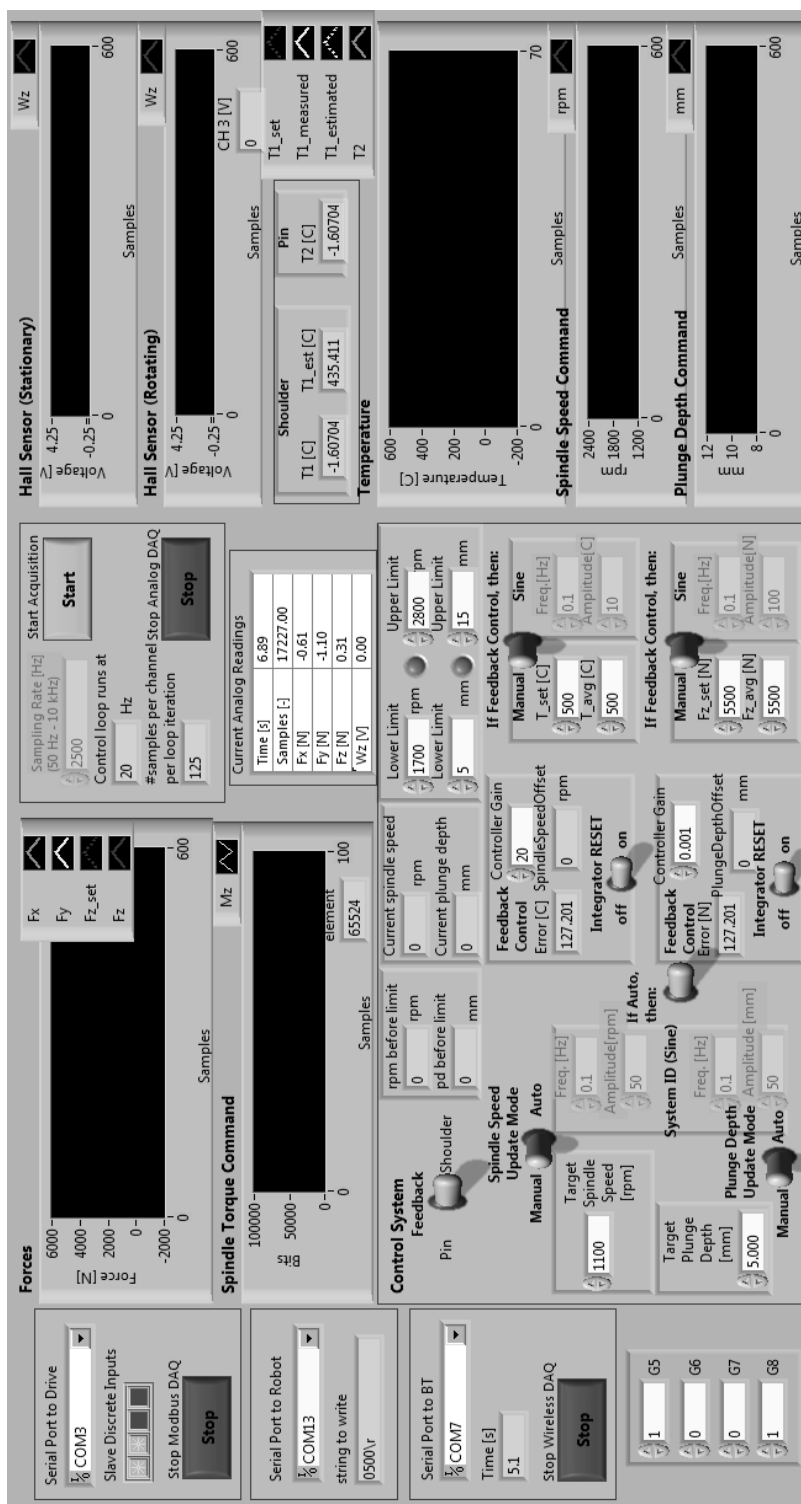


Figure 95: Front panel GUI of custom LabVIEW™ software (robotic FSW system).