NUMERICAL SIMULATION OF ARC WELDING PROCESS
AND ITS APPLICATION

DISSERTATION

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*****

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ABSTRACT

The numerical simulation of arc welding process provides insight and information not available from experiments for process development, but has not been used in practical welding applications. In order to demonstrate its usefulness in welding applications, a three-dimensional numerical simulation of the pulsed gas metal arc welding (P-GMAW) process using Volume of Fluid technique was developed based on mathematical models. It was validated by the comparison of weld deposit geometry, transient radius, and temperature history. The physical mechanism of weld bead hump formation, which has not been clearly understood, and a suppression technique were explored based on heat and fluid flow profiles and solid/liquid interface contours obtained from the numerical simulation of P-GMAW and hybrid (P-GMAW + laser) processes.

The mechanism of hump formation was investigated by using the numerical simulation. According to simulation results of P-GMAW, traveling at a high travel speed, the events leading to the formation of a humped bead were identified. In the initial stage of hump formation, a thin, elongated, molten bead was formed and then pinched due to capillary instability, resulting in a dramatically reduced cross section of the molten weld
bead. Solidification then divided the weld pool into front and back sections, guaranteeing hump formation.

The numerical simulation was also used to demonstrate the suppression of hump formation by hybrid process. Simulation results of hybrid process showed that a defocused laser beam located in front of the P-GMA weld pool could suppress hump formation. A shallow “skin” melt produced by the defocused laser beam, with sufficient beam intensity and beam radius, promoted a wider weld bead with a smaller internal contact angle, which was less susceptible to capillary instability of weld metal deposit.
Dedicated to my parents and my wife
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CHAPTER 1

INTRODUCTION

The arc welding process is a common joining technology used in the manufacturing industry. Despite this fact and the general trend toward simulation based design of manufacturing processes and products, the arc welding process has not been simulated accurately enough for many practical process design needs. This is partly due to the fact that the physical processes associated with the electric arc and its interaction with material are very complex phenomenon to understand from basic principles. Four states (solid, liquid, gas, and plasma) of several materials exist simultaneously in a small weld volume and material interactions associated with electric, magnetic, kinetic, thermal, chemical, atomic and fluidic processes take place. Aspects of the physical behavior of the cathode spots formed at the negative electrode of the welding process (for example, their retrograde motion in a transverse magnetic field and their high mobility) are still not even understood fundamentally at this time. The work described in this dissertation is aimed at establishing the usefulness of arc welding models based on accurate simulation of heat and fluid flow phenomena that incorporate empirically-calibrated material, momentum, pressure and energy inputs associated with the arc.
There is considerable published literature dealing with mathematical modeling of welding arc effects on the welded material. In published literature, the spatial distributions of the direct arc heat input, the electrical current and normal pressure due to stagnation of arc plasma flow on the weld pool surface have been characterized as Gaussian density functions. Gaussian density functions are described by a single shape parameter and a total magnitude (i.e. total heat input and current) that can be obtained from experimental measurement. The plasma gas flow also exerts a shear stress on the liquid metal surface that tends to push the molten metal outward, reducing the weld penetration. The other important force due to the electric arc is Lorentz force that is induced by interaction between the current flow in the weld pool and its magnetic field. At the high welding current, the Lorentz force is dominant and largely determines the weld pool fluid flow.

Other forces that induce flow in the weld pool are caused by the variation in material properties due to temperature gradients. The surface tension gradient induced flow on the weld pool surface due to the temperature gradient has major effects on weld pool shape. The final weld shape can be significantly changed by the contents of the surface active elements (e.g. sulfur) that alter the direction of surface tension gradient induced flow (Marangoni flow). The high sulfur steel tends to generate the inward fluid flow pattern that tends to produce the deeper penetration, and the outward fluid flow pattern that generates the shallow penetration is expected for the low sulfur steel. Change of density with temperature also induces buoyancy forces that stir the weld pool. Even these relatively well-understood flow-inducing forces are related to temperature gradients.
caused by welding heat, mass and momentum inputs. Thus, prediction of even these
relies on accurate modeling the arc inputs.

The other work described in this dissertation based on the numerical simulation of
arc welding process is that ability to predict the deposition of filler metal droplets, the
shape of the free surface of the weld pool and solidified weld metal will be useful to
welding engineers. In this regard, it is noted that there are several critical weld defects
that are related to weld shape. The formation of weld bead humps is a defect mode that
sets an upper limit on the arc welding travel speed. The shape of the wetting contact line
between weld metal and adjacent un-melted base material is fundamental to prediction of
lack of fusion defects and usually determines the fatigue strength of welds. The arc
welding simulation is useful to use the numerical simulation as a key tool for the
investigation of the welding defect such as hump formation at the high travel speed weld
and the process development to eliminate the welding defect based on the understanding
of its mechanism. Another capability of using the numerical simulation is its flexibility of
studying the phenomenon under varying conditions (e.g. welding parameters, thermal
mechanical properties etc), which are very difficult to acquire experimentally. In this
work, the formation of humped welds at the high speed weld was numerically simulated
to fully understand its mechanism because the mechanism of hump formation has not
been clearly understood. Based on the understanding of hump formation, hybrid (GMAW
+ laser) process was developed to suppress bead humps. Also, the hybrid process was
numerically simulated to study its capability to prevent hump formation and investigate
the suppression mechanism based on the simulation prediction.
In this dissertation, three-dimensional numerical simulation of pulsed gas metal arc welding process, the simulation and study of the hump formation in the high travel speed weld, and simulation of humping suppression by a hybrid laser + arc welding process are described in detail in the following chapters. Chapter 2 provides the basis for the bead hump defect studies described in later chapters. The stationary pulsed gas metal arc welding (GMAW-P) process was numerically simulated using a code based on Volume of Fluid (VOF) technique implemented the mathematical models with parameters obtained from analysis of high-speed video images and data acquisition (DAQ) system. The simulation of P-GMAW welding was validated by comparison of measured and predicted weld deposit geometry, transient radius, and temperature history. Based on the weld simulation parameters, a parametric study of weld simulation was performed to demonstrate and understand the effectiveness of individual simulation parameters on heat and fluid flow in the molten weld pool and the final configuration of stationary welds. Finally, the simulation of a two heat input case was introduced to illustrate the usefulness of the simulation for process development. In chapter 3, three-dimensional numerical simulations of the high speed pulsed gas metal arc welding process (P-GMAW) were conducted to demonstrate the formation of humped beads on the thick plate. Based on heat and fluid flows profiles and the transient molten deposit images acquired from simulation and experimental results, the physical mechanisms associated with humping phenomenon were investigated and two conditions responsible for hump formation were identified. Especially, the simulation results clarified the fluid flow associated with two different hump shapes. Chapter 4 discusses the formation of
humped beads on the thin plate that are compared with humped beads made on the thick plate demonstrated in previous chapter and the capability of hybrid process (P-GMAW + laser) to suppress hump formation on the thin plate. According to the simulation results of hybrid process, the mechanism of hump suppression is demonstrated based on the heat and fluid flow profiles. Finally, based on the parametric study of laser beam, the investigation of the laser beam intensity and size for the suppression of hump formation are discussed and demonstrated using heat and fluid flow obtained from the hybrid weld simulation results. In Chapter 5, the key results of each chapter in this dissertation are summarized, and the unresolved problems concerning numerical simulation of the arc welding process that remain for future work are mentioned.
CHAPTER 2

NUMERICAL SIMULATION OF ARC WELDING PROCESS

2.1 INTRODUCTION

During arc welding processes such as gas metal arc welding (GMAW) and gas tungsten arc welding (GTAW), fluid flow and heat flow are key factors that determine the final weld shape. Many previous efforts have been attempted in predicting these aspects of arc welding by numerical simulation. While currently-available welding heat flow and distortion simulations are quite comprehensive and accurate enough for many practical purposes, phase change and fluid flow phenomena occurring in arc welding are complex and have still not been realistically simulated. In particular, numerical model-based prediction of the dynamic changes in shape of the liquid weld pool surface would be useful in many applications if they were possible [1].

In GMAW, heat input to the weld pool is composed of a direct arc heat input and the enthalpy of molten droplets transferring from the welding wire. In numerical weld pool simulations, the current density is also needed to predict the distribution of Lorentz
force in the weld pool fluid. These parameters are difficult to measure for GMAW because of difficulties posed by filler metal transfer, but measurements have been made for GTAW. To quantify direct heat and also the electrical current distributions on the weld pool surface, Lu and Kou [2] measured power and current density distributions using a split copper block. Based on the analysis by the Abel inversion method, the shape of power and current distribution were found out to be Gaussian density functions, so the arc shape could be described by the total magnitudes (i.e. total heat input and current) and Gaussian distribution parameters.

The shape of the weld pool and bead shape are also strongly affected by flow of plasma in the welding arc. The forces exerted by the arc plasma jet on the weld pool are the arc stagnation pressure and drag force. Arc pressure acts on the weld pool surface in the normal direction, depressing the molten deposit. Arc pressure density distribution on the weld pool surface has also been investigated for GTAW [3], and was characterized as a Gaussian density distribution function. Adonyi et al. [4] studied its effect on the weld pool dynamics and found that the arc pressure mainly caused the depression of the weld pool surface. Drag force is a shear stress on the liquid metal surface produced by plasma gas flow. Tanaka et al. [5] investigated the driving forces for weld pool convection during gas tungsten arc welding, and the drag force and the Marangoni force (discussed below) were found out to be dominant.

Convection caused by surface tension gradients has major effects on weld pool shape. The mechanism was studied by Heiple and Roper [6, 7]. They proposed that the final weld shape can be significantly altered by variations of the surface active elements
(e.g. sulfur) that changes the direction of surface tension gradient induced flow
(Marangoni flow) in GTA welding conditions.

Based on understanding the well-known forces and heat input in the weld pool, many researchers simulated arc-welding processes and studied weld pool convection, the formation of the weld pool and molten droplets, droplet transfer and solidified weld bead shape. The idea of solving for the shape of the free surface of a fluid volume as a static energy minimization problem [8] has been applied to calculate weld bead shape by a number of authors [9-13]. Zhang et al. [14-16] studied the three dimensional numerical simulation of the complex geometry such as fillet welds based on the surface energy minimization for the surface deformation tracking. Also, Kumar and DebRoy [17] developed the optimization algorithm to minimize the error between the experimental results and the simulation results by the determination of unknown variables from a limited volume of experimental data. Since the molten weld metal is not stationary and is also cooling and solidifying as it accumulates and spreads to form a weld bead, a more accurate analysis of bead shape takes simultaneous fluid and heat flow into account in additional to the forces included in the static balance. Trapaga and Szekely [18] used the VOF numerical technique to simulate the isothermal spreading of impacting droplets on surfaces. Wang and Tsai [19] investigated the dynamics of periodic filler droplets impinging onto weld pool and phase change, using VOF technique that can handle a transient deformed weld pool surface and the continuum model [20], respectively. Zheng [21] modeled the spreading of an impacting droplet using the level set method, another interface tracking scheme with similarities to VOF. More recently, the VOF technique
has been used to simulate melting and detachment of metal droplets from welding wire in GMAW [22-27], and Fan and Kovacevic [28] developed the unified 2D axisymmetric model to study droplet formation and detachment, droplet transfer in arc plasma, impingement of droplets on the weld pool, and solidification in gas metal arc welding.

In this dissertation, the Flow3D CFD code using Volume of Fluid (VOF) numerical technique was selected to simulate non-isothermal weld pool fluid flow, integrating mathematical models for phenomena as developed by previous researchers. In order to arrive at an accurate P-GMAW simulation that can be executed relatively short time and is also reasonable enough for engineering use, the arc effects were represented as boundary and body inputs. Thus, simulation parameters to characterize the molten filler metal droplets and arc dimensions were measured from experiments. Also the real time weld pool radius measurements, thermal history measurements at selected positions, and cross sections of final weld profiles and fusion boundaries were used for simulation validation. The effects of selected individual simulation parameters on GMAW weld pool flow and final shape were also investigated. Finally, predictions of the transient and final weld shape for a dual heat source process influence are shown to illustrate the usefulness of the simulation for welding process development.
2.2 EXPERIMENTAL PROCEDURES

Stationary welds were made for 1.8 sec using the pulsed gas metal arc welding process (using a Thermal Arc 500P power supply). Materials selected for the experiment were 6.35 mm-thick ASTM A-36 steel, containing 50ppm sulfur, with sand-blasted surface preparation for the workpiece and 1.143 mm diameter of ER70S-6 welding wire. Contact-tip-to-work distance (CTWD) was 19.1mm. Thermophysical material properties of A-36 steel are shown in Table 2.1.

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<th>Nomenclature</th>
<th>Value</th>
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<td>Density $\rho$ (kg/m$^3$)</td>
<td>7800</td>
<td>CTE (m/m K)</td>
<td>14.4x10$^{-6}$</td>
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<td>Viscosity $\mu$ (kg/m s)</td>
<td>6x10$^{-3}$</td>
<td>Liquidus Temp. $T_L$ (K)</td>
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</tr>
<tr>
<td>Kinematic viscosity $v$ (m$^2$/s)</td>
<td>7.7x10$^{-7}$</td>
<td>Solidus Temp. (K)</td>
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<tr>
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<td>Temp. dependent</td>
<td>Vaporization Temp. $T_v$ (K)</td>
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<td>Thermal conductivity $k$ (l) (W/m K)</td>
<td>26</td>
<td>Heat transfer coeff. $h$ (W/m$^2$K)</td>
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<tr>
<td>Specific heat of solid (J/kg K)</td>
<td>686</td>
<td>Emissivity</td>
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<tr>
<td>Specific heat of liquid $c_l$ (J/kg K)</td>
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<td>Material permeability $\mu_m$ (H/m)</td>
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<tr>
<td>Latent heat of fusion (J/kg)</td>
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<td>Drag coefficient constant</td>
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</tr>
<tr>
<td>Latent heat of vaporization (J/kg)</td>
<td>7.34x10$^6$</td>
<td>Thickness (m)</td>
<td>2.6x10$^{-3}$</td>
</tr>
</tbody>
</table>

Table 2.1 Thermophysical material properties of A36 using in the simulation

<table>
<thead>
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<th>Nomenclature</th>
<th>Value</th>
<th>Nomenclature</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Welding wire type</td>
<td>ER70S-6</td>
<td>Average power (W)</td>
<td>8842</td>
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<tr>
<td>Wire diameter (mm)</td>
<td>1.125</td>
<td>Peak current (A)</td>
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<td>Wire feed speed (mm/s)</td>
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<td>_background current (A)</td>
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<td>CTWD (mm)</td>
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<td>Peak voltage (V)</td>
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<td>Pulse time (ms)</td>
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<td>Background voltage (V)</td>
<td>24.4</td>
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<tr>
<td>Pulse frequency (Hz)</td>
<td>250</td>
<td>Shielding gas/flow rate</td>
<td>Ar,10CO2 /40 CFH</td>
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</tbody>
</table>

Table 2.2 P-GMAW current waveform and welding parameters
Figure 2.1 High speed video sequences showing different metal transfer of molten droplets. 125Hz (a) and 250Hz (b) drop frequencies are measured.
During welding, measurements were made with a high speed CCD camera and a data acquisition system (DAQ). Images of the arc and molten metal pool were captured by the high speed CCD camera with a 950nm +/- 10 nm band pass filter mounted between high speed camera lenses used to filter out unwanted arc light in order to capture the clear images of metal transfer and weld deposit growth. According to video images of metal transfer (Figure 2.1), one drop per pulse metal transfer was observed after 1.2sec of weld time. Before that time, the molten metal transfer was somewhat random but transfer rate was approximately one drop every two pulses. It is supposed that this difference in transfer corresponds to differences in the temperature distribution in the welding wire extension. The arc length measured from the arc images was approximately 4 mm. As shown in Figure 2.2, the electrode tip was relatively blunt in the beginning of welding, but became sharper when the metal transfer stabilized. The tip angle was measured to be approximately 90 degrees before 1.2 sec and 60 degrees after 1.2 sec. The transient weld deposit profile and radius were measured from images taken at 500 frames per second.

Figure 2.2 The snap image of electrode tip to measure the different tip angles
The velocity of molten droplets was too high to analyze at 500 frames per second rate, so the recording rate was increased to 4500 frames per second to analyze their size and speed.

Figure 2.3 P-GMAW current waveform (a) and voltage waveform (b).

Welding current and voltage waveforms were acquired at a sampling rate of 10 kHz: examples of the waveforms are shown in Figure 2.3. Peak values and background values of current and voltage, pulse frequency, and pulse duration were obtained from the waveforms and used to calculate the instantaneous average power, peak power and background power for the heat input in the weld system during the process. The waveform parameters and welding parameters are displayed in Table 2.2.
2.3 MATHEMATICAL MODELING AND NUMERICAL SIMULATION

The P-GMAW weld pool and bead deposit were mathematically modeled using 3D Cartesian coordinate system, and the governing equations were solved numerically to simulate the arc welding process using Flow3D commercial code. The liquid metal was considered to be an incompressible Newtonian fluid, and flow was laminar. The density change of molten metal was only considered for the buoyancy term in the momentum equation using a Boussinesq approximation. The flow at the solid/liquid phase interface was modeled using a porous media drag concept [40]. Arc heat input (defined as the direct heat input to the workpiece) and arc pressure on the molten pool surface were modeled as Gaussian density distributions. The total heat input applied to the workpiece, calculated by multiplying the instantaneous average arc power by a process efficiency, was the sum of direct heat input and the latent heat of droplets.

2.3.1 GOVERNING EQUATIONS

The weld pool simulation was based on the numerical solution of mass, momentum and energy conservation relationships

\[
\nabla \cdot \mathbf{v} = \frac{\dot{m}_s}{\rho} \tag{2.1}
\]

\[
\frac{\partial \mathbf{v}}{\partial t} + \mathbf{v} \cdot \nabla \mathbf{v} = -\frac{1}{\rho} \nabla P + \nu \nabla^2 \mathbf{v} + \mathbf{f} + \frac{\dot{m}_s}{\rho} \mathbf{v} - \mathbf{K} \mathbf{v} \tag{2.2}
\]
\[
\rho \left( \frac{\partial U}{\partial t} + \mathbf{v} \cdot \nabla U \right) = \nabla \cdot (k \nabla T) + \dot{U}_s
\]  

(2.3)

where \( \mathbf{v} \) is molten metal velocity, \( \dot{m}_s \) is a mass source term, \( P \) is hydrodynamic pressure, \( \nu \) is kinematic viscosity, \( \mathbf{f} \) is body accelerations due to body force (e.g. gravity acceleration), \( \rho \) is a fluid density, \( K \) is the drag coefficient for a porous media model, \( U \) is internal energy per unit mass, \( k \) is thermal conductivity (temperature dependant values), \( T \) is a local temperature and \( \dot{U}_s \) is an energy source term due to a mass source term.

To model solid-liquid phase changes, the mathematical model of enthalpy-temperature relationship is

\[
h = \begin{cases} 
\rho_s C_s T & (T \leq T_s) \\
h(T_s) + h_{sl} \frac{T - T_s}{T_l - T_s} & (T_s < T \leq T_l) \\
h(T_l) + \rho_l C_l (T - T_l) & (T_l < T)
\end{cases}
\]

(2.4)

where \( h \) is enthalpy, \( \rho_s \) and \( \rho_l \) are solid and liquid density, respectively, \( C_s \) and \( C_l \) are specific heat at constant volume of the solid and liquid phases, \( T_s \) and \( T_l \) are solidus and liquidus temperatures of the metal alloy and \( h_{sl} \) is the latent heat of fusion for phase change between liquid and solid.

The simulation technique used in this work is based on an additional advection relationship that expresses the conservation of volume fraction in the fluid flow at the
free surface and fluid interfaces (for a two-fluid model). It is derived from the conservative form of the mass conservation law using of density and fluid volume fraction relationships

\[
\frac{\partial \rho}{\partial t} + \nabla \cdot (\mathbf{v} \rho) = \dot{m}_s
\]  

(2.5)

\[\rho = \rho_o F\]  

(2.6)

\[\dot{m}_s = \rho_o \dot{F}_s\]  

(2.7)

where \(\rho\) is the zone density at the current cell, \(\rho_o\) is the density of material, \(F\) is a volume fraction of a fluid and \(\dot{F}_s\) represents the change of the volume fraction of fluid associated with the mass source \(\dot{m}_s\) in the continuity equation. Substituting (2.6) and (2.7) into (2.5) results in the volume of fluid (VOF) equation (2.8), which can be used to effectively track the location of free fluid surfaces in the simulation domain.

\[
\frac{\partial F}{\partial t} + \nabla \cdot (\mathbf{v} F) = \dot{F}_s
\]  

(2.8)

Based on Equation (2.8), free surface cells are defined as those which have void (zero volume fraction) neighbor cells. A numerical technique for tracking the shape and volume of the free surface of fluid and a volume advection algorithm presented by Hirt and Nichols [29] is not reiterated here.
2.3.2 SOLIDIFICATION MODEL

Since a single fluid is used in the model, the solid and liquid phases are distinguished based on the enthalpy-temperature relationship (2.4). The fluid temperature of each cell is determined from its enthalpy, which is computed based on conduction and convection of material. If the temperature is between liquidus and solidus temperatures, the cell becomes a part of a mushy zone. The amount of solid phase is calculated in terms of the temperature ratio and is used for the determination of the effective viscosity and the drag coefficient in the mushy zone.

To model flow in the mushy zone, it is divided into three sub-regions distinguished by the critical solid fraction and the coherent solid fraction. Fluid in each sub-region is assigned a different drag coefficient and a local viscosity. The first region consists of cells with solid fraction below the coherent solid fraction. The local viscosity is varied due to the amount of solid fraction according to

\[
\mu = \mu_0 \left(1 - \frac{F_s}{F_{cr}}\right)^{-1.55}
\]

(2.9)

where \( F_s \) is the local solid fraction in the given cell and \( F_{cr} \) is the critical solid fraction.

The second region of the mushy zone consists of cells where the solid fraction is above the coherent solid fraction but less than the critical solid fraction. In this region, the microstructure is acting as a porous media so the Carman-Kozeny equation [30] that is derived from Darcy model [31, 32] is used to compute the drag coefficient.
\[ K = C_o \cdot \frac{F_s^2}{(1 - F_s)^3 + \varepsilon} \]  \hspace{1cm} (2.10)

where \( K \) is the drag coefficient, \( C_o \) is the drag coefficient constant (equal to 1 for steel) and \( \varepsilon \) is the positive zero (for computation purposes). In this region, (2.9) is still needed to calculate the local viscosity of fluid to compute the precise fluid resistance in the computational cell.

In the region above the critical solid fraction, the microstructure is assumed to be fully developed into a complete rigid structure and there is assumed to be infinite resistance to fluid flow. Thus, fluid flow is stopped due to an effectively infinite drag coefficient computed by equation (2.10).

2.3.3 BOUNDARY CONDITIONS

The axisymmetric free surface heat input from the arc was modeled as a fixed Gaussian density function [33]

\[ q(r) = \frac{Q}{2\pi \sigma_a^2} \exp\left(\frac{-r^2}{2\sigma_a^2}\right) \]  \hspace{1cm} (2.11)
where \( Q \) is the actual heat input directly from the arc to the substrate and \( \sigma_a \) is the Gaussian heat distribution parameter. The Gaussian heat distribution parameter is the main factor to adjust the heat input distribution on the free surface of weld pool. Additionally, convection and radiation are applied on the free surface. Therefore, the heat input on the free surface is expressed as

\[
q(r) = \frac{Q}{2\pi \sigma_a^2} \exp\left(-\frac{r^2}{2\sigma_a^2}\right) - h_c (T - T_o) - \sigma \varepsilon (T^4 - T_o^4) \tag{2.12}
\]

where \( h_c \) is heat transfer coefficient, \( T \) is temperature, \( T_0 \) is ambient temperature, \( \sigma \) is Stefan-Boltzmann constant (5.67 x 10^{-8} \text{ W/m}^2\cdot\text{K}^4), \) and \( \varepsilon \) is the emissivity. The wall boundary condition was applied to solid free surface cells

\[
-k \frac{\partial T}{\partial n} = -h_c (T - T_o) \tag{2.13}
\]

To model Marangoni flow, the shear stress balance as boundary condition on the free surface is described as

\[
\mu \frac{\partial n}{\partial n} = -\frac{\partial y}{\partial T} \frac{\partial T}{\partial r} \tag{2.14}
\]
where $\mu$ is the dynamic viscosity, $\mathbf{v}_r$ is the tangential velocity vector, $n$ is the normal to the free surface, $\partial \gamma / \partial T$ is the surface tension gradient, and $r$ is the tangential direction on the free surface. An additional plasma drag shear stress is described below. Also, the normal pressure balance as boundary condition on the free surface is expressed as

$$-p + 2\mu \frac{\partial \mathbf{v}_n}{\partial n} = -p_{arc} + \gamma \left( \frac{1}{R_1} + \frac{1}{R_2} \right)$$

(2.15)

where $p$ is the liquid pressure at the free surface in normal direction, $\mathbf{v}_n$ is the normal velocity vector, $p_{arc}$ is the arc pressure (described below), $\gamma$ is the surface tension, and $R_1$ and $R_2$ are the principal radii of surface curvature. For this study, surface tension is obtained from the formula developed by Sahoo et al. [34]. The equation of surface tension as a function of temperature with sulfur active element in a binary Fe-S system is expressed by

$$\gamma(T) = \gamma_m^o - A \cdot (T - T_m) - R \cdot T \cdot \Gamma_s \cdot \ln(1 + k_i \cdot a_i \cdot e^{-\Delta H^o / RT})$$

(2.16)

where $\gamma_m^o$ is the surface tension of pure metal at the melting point, 1.943, $A$ is the negative of surface tension gradient for pure metal, 4.3E-4, $T_m$ is the melting point of the material, 1798 K, $R$ is gas constant, $\Gamma_s$ is the surface excess at saturation, 1.3E-8, $k_i$ is the entropy factor, 0.00318, $a_i$ is weight percent of sulfur, 0.005% for this work, and $\Delta H^o$ is
the heat of absorption, \(-1.66E+8\). According to equation (2.16), the negative surface tension gradient at temperatures above 2000 K is large so a strong outward Marangoni force spreads the molten metal.

Pressure gradients generated by Lorentz force in the arc plasma causes downward (along the negative z-coordinate) flow of the ionized gas. A stagnation pressure that is consistent with the redirection of this downward flow is approximated as Gaussian density distribution whose magnitude and radius are based on analysis of experimental results [3, 35]

\[
p_{arc}(r) = \frac{P}{2\pi \sigma_p^2} \exp\left(-\frac{r^2}{2\sigma_p^2}\right)
\] (2.17)

where \(\sigma_p\) is Gaussian pressure distribution parameter and \(P\) is total force (N).

When the plasma jet flow impinges on the weld pool surface, the plasma drag force is induced on the weld pool surface. This plasma drag force creates outward fluid flow of liquid metal at the surface and also changes with weld pool configuration. In this work, an analytical solution [36] of the war shear stress produced by the normal impingement of a plasma jet on a flat surface was used to determine and apply the drag force as the boundary condition on the free surface cell. Two-dimensional axisymmetric coordinate system was used to derive the theoretical equation in terms of Reynolds number and a ratio of jet height and nozzle diameter, which is expressed as
\[
\frac{\tau}{\rho_p u_o^2} \text{Re}_o^{1/2} \left( \frac{H}{D} \right)^2 = g_2 \left( \frac{r}{H} \right) \tag{2.18}
\]

where \( \tau \) is shear stress (N/m\(^2\)), \( \rho_p \) is plasma density (Kg/m\(^3\)), \( u_o \) is the initial plasma velocity (m/s), \( \text{Re}_o \) is Reynolds number, \( H \) is a nozzle height (m), \( D \) is the nozzle diameter (m), \( r \) is the radius (m) from the center, and \( g_2 \) is the universal function plotted in reference paper [36]. The initial plasma jet velocity is calculated based on the maximum plasma stagnation pressure at the weld pool center using Bernoulli’s equation in order to obtain Reynolds number. The jet height and the jet nozzle diameter are assumed to be the arc length and the electrode size. The computed drag force is applied into the free surface cells as a body force in the momentum equation (2.2).

A key feature of the simulation is the representation of melting of the GMAW welding wire and the transfer of resulting droplets to the weld pool. Welding wire melting was modeled as a periodic stream of spherical droplets with velocity vectors in the negative z direction. Conservation of mass was applied to calculate the initial droplet radius from welding wire diameter and welding parameters (wire feed speed and drop frequency). The initial velocity of spherical droplets was directly measured from sequential arc images. Many researchers add the plasma drag force to the transferring droplets, computed as a function of droplet radius, drag coefficient for a sphere, and the plasma gas velocity in momentum equation, acting on the liquid droplet between the electrode tip and the base metal. In this model, the velocity of liquid droplet right before impinging on the weld pool was measured and used as the initial velocity of liquid
droplet, and also the height of liquid droplet is fixed with respect to the free surface of weld pool to maintain the same condition when measuring the velocity of liquid droplet. Due to the small distance traveling of liquid droplet, the model assumes that the plasma drag force exerting on liquid droplet can be ignored.

2.3.4 BODY FORCES IN THE WELD POOL

The body force term in momentum equation (2.2) was comprised of the sum of two terms \( \mathbf{f} = \mathbf{f}_b + \mathbf{f}_L \) where \( \mathbf{f}_b \) is buoyancy force and \( \mathbf{f}_L \) is Lorentz force. For the buoyancy force term, Boussinesq approximation concept was applied to account for the effect of a small density change in the gravity term

\[
\mathbf{f}_b = -\beta \cdot (T - T_b) \mathbf{g}
\]  

(2.19)

where \( \beta = -\frac{1}{\rho} \left( \frac{\partial \rho}{\partial T} \right)_p \) is the thermal expansion coefficient.

Lorentz force was obtained from an analytical solution [37] based on the current flow and associated magnetic field in the substrate material. The electric field is assumed to be quasi-steady state, the electrical conductivity is assumed to be constant and the material domain is a semi-infinite plate. Then, the electric potential field \( \phi \) in the weldment is given by Laplace’s equation
\[
\n\n\n\n\n+\frac{\partial}{\partial z}
\]

\[
\frac{\partial}{\partial r}
\]

\[
\frac{\partial}{\partial z}
\]

\[
\frac{\partial}{\partial r}
\]

The axisymmetric solution of equation (2.20) is obtained using a Hankel transformation with boundary conditions

\[
J_z (r,0) = -\sigma_e \frac{\partial \phi}{\partial z} = \frac{gcc \cdot I}{\pi \sigma_e^2} \exp\left(-\frac{gcc \cdot r^2}{\sigma_e^2}\right)
\]

\[
\frac{\partial \phi}{\partial z} (r,c) = 0 \quad \frac{\partial \phi}{\partial r} (0,z) = 0 \quad \frac{\partial \phi}{\partial r} (\infty,z) = 0
\]

where \(gcc\) is Gaussian current coefficient, \(\sigma_e\) is the electrical conductivity of the weld metal, \(\sigma_c\) is the Gaussian current parameter (m), \(I\) is a current (A), and \(c\) is the thickness of the workpiece. Note that the current distribution on the top of free surface of the material is also described as Gaussian distribution function. This Gaussian distribution is varied as a function of Gaussian current coefficient and the Gaussian current parameter. In past simulations, the Gaussian current coefficient, which is an important factor for the current distribution on the weld pool surface, has been varied from 0.5, used by Taso and Wu [38], to 3, used by Kou and Sun [37]. Kumar and DebRoy [39] used the Gaussian current coefficient as an explicit variable to be adjusted for to accommodate different welding conditions such as torch angle (travel angle and work angle) and arc length. In
In the present work, Gaussian current coefficient is explicitly selected as 0.5 [33, 38] for Lorentz force calculation.

The two dimensional axisymmetric Lorentz force must be converted to three-dimensional Cartesian coordinates for substitution into the momentum equation. The ‘r’ and ‘z’ Lorentz force components were calculated for the individual cells in 3D Cartesian coordinate system based on the analytical solution. The ‘r’ direction component was then split into ‘x’ and ‘y’ components.

2.3.5 NUMERICAL SIMULATION

To perform the numerical simulation of welding process, two regions, void and fluid, were generated in the computational domain with the fluid representing the material with the phase change capability. Due to weld pool surface deformation during the welding process, free surface modeling is applied to track the deformed free surface. In the fluid region either solid or liquid, governing equations (2.1)-(2.4) and (2.8) with the required boundary conditions are numerically solved through following steps [40]. First, the new velocities at the current time level are approximated using the explicit method based on variables for the previous time level. Second, the pressure equation (Poisson equation) was solved by Successive Over Relaxation (SOR) method to satisfy the continuity equation and then the energy equation is solved by the implicit method. Finally, the configuration of the free surface is updated using the VOF equation. These steps are repeated at every time step until the desired simulation time is reached.
There are four free surface boundary conditions to implement the effects of the electric arc on the weld pool; arc heat input, arc pressure, drag force and drop generation. To numerically apply Gaussian heat flux on the free surface, the free surface cells were tracked, and at every time step an appropriate increment is added to their stored energy. The source term ($\dot{U}_s$) in energy equation (2.3) is used to add the calculated thermal energy into the free surface cells. Also, the Gaussian arc stagnation pressure is numerically implemented in the momentum equation (2.2) as a boundary condition at free surface. The corresponding pressure is acts on the surface-normal direction on fluid in cells on the weld pool surface. Similarly, plasma drag force calculated from the theoretical equation as a function of the maximum pressure and the distance from the arc center is applied on the momentum equation (2.2) for the free surface cells. Terms were added to all governing equations to model generation of molten droplets in the void region. To add the mass of droplet, source terms in governing equations (2.1 and 2.5) are modified to create the droplet, and then the momentum equation is used to set the initial velocity of molten metal droplet and their height with respect to the free surface of a weld pool. In the energy equation (2.3), the initial temperature of droplets [41, 42] is used to calculate the amount of enthalpy that deposits into cells that correspond to droplet locations.
2.4 SIMULATION PARAMETERS

The simulation parameters for Gaussian heat input, arc pressure, drag force, drop generation and other physical parameters needed to conduct P-GMAW stationary weld simulations were based on current and voltage waveforms, video images of weld pool and metal transfer and values from literature. Details of the parameters and measurements are summarized below.

![Temperature vs. thermal conductivity](image)

Figure 2.4 Temperature vs. thermal conductivity

Temperature-dependent thermal conductivity for the solid phase, shown in Figure 2.4, was used to accurately evaluate the thermal diffusion. For the generalized solidification model [43], the coherent solid fraction (0.48) and the critical solid fraction
(0.64) were estimated based on application of established theory to the iron-iron carbide binary phase diagram. In order to apply the generalized theory, it is necessary to have a eutectic phase transformation, so the phase diagram in the peritectic reaction region was approximated by a larger triangle, producing a region similar to a eutectic phase transformation. The coherent and critical temperature lines were proportionally drawn onto the modified binary iron-iron carbide binary phase diagram. Two intersection points with vertical lines passing through liquidus and solidus temperatures were found to calculate the coherent solid fraction and the critical solid fraction using tie line and lever rules.

2.4.1 GAUSSIAN HEAT INPUT

Gaussian heat input was defined by the arc power and Gaussian heat distribution parameter as discussed previously. For the pulsed GMAW process, the instantaneous power calculated as the product of simultaneous current and voltage samples varies during the weld, so the instantaneous average power, 8842 W was calculated as the average of these values. This is larger than the actual energy deposited into the weld, so the actual power is adjusted by multiplying by the arc efficiency value measured by liquid nitrogen calorimetry [44] as 0.74, which is typical of GMAW arc efficiencies measured by this technique. This actual power is still an average power, so the peak power and the background power measured from current and voltage waveforms are used to represent the pulsing behavior of actual heat input during the weld and then the actual
power was further split to the arc power and the power consumed for molten drop generation.

To determine the heat input density distribution on the weld pool surface, the Gaussian heat distribution parameter for a direct heat input was estimated based on the empirical equation obtained from the literature [33]. This equation is a function of current for a 4mm arc length case expressed as

\[ \sigma_a = 0.533I^{0.2941} \]  

(2.22)

where \( \sigma_a \) is Gaussian heat distribution parameter (mm) and \( I \) is current (amps). Current measurements from the DAQ system were used to compute this value.

2.4.2 ARC PRESSURE, DRAG FORCE, AND LORENTZ FORCE

From equation (2.17), total arc force and Gaussian pressure distribution parameter are required to calculate the arc pressure on the weld pool. For the arc pressure computation, the current waveforms and the electrode tip angles were measured in order to estimate the arc pressure from literature formulas. According to the previous research papers [3, 15], the empirical equations for the total force and Gaussian pressure distribution parameter as a function of current and electrode tip angle were expressed as
\[
P = \begin{cases} 
-0.04017 + 0.0002553 \cdot I (N) & (60^\circ \text{ tip angle}) \\
-0.04307 + 0.0001981 \cdot I (N) & (90^\circ \text{ tip angle}) 
\end{cases} 
\] (2.23)

\[
\sigma_p = \begin{cases} 
1.4875 + 0.00123 \cdot I (mm) & (60^\circ \text{ tip angle}) \\
1.4043 + 0.00174 \cdot I (mm) & (90^\circ \text{ tip angle}) 
\end{cases} 
\] (2.24)

where \( P \) is the total force (N), \( I \) is current (amps), and \( \sigma_p \) is Gaussian pressure distribution parameter (mm). The time dependent current waveforms and electrode tip angles discussed previously were used in equations (2.23) and (2.24) to compute these values.

The analytical solution [36] for drag force calculation obtained from the literature requires Reynolds number and a ratio of jet height and nozzle diameter. The jet nozzle height was taken as 4 mm based on high speed video arc length measurements and the jet diameter was set at 1 mm, approximately equal to the electrode diameter. Reynolds number contains the plasma jet velocity term that is computed using Bernoulli’s equation based on the maximum arc pressure at the weld pool center computed from the arc pressure calculation at zero radius. Other terms involved in Reynolds number are the material properties of plasma jet found from the literature [45]. The density and dynamic viscosity of argon plasma are 0.046 kg/m\(^3\) and 0.00005 kg/m\(-s\) at 10000K plasma temperature.

The theoretical equation of Lorentz force in the weld pool derived based on the fixed boundary conditions except the free surface boundary condition. The current density distribution on the free surface is varying with time, so the input parameters to
determine Lorentz force are pulse current and relative Gaussian current density parameters obtained from the previous research paper [33]. The empirical equation for Gaussian current density distribution parameter is expressed as

\[ \sigma_c = 0.5342I^{0.2684} \]  

(2.25)

where \( \sigma_c \) is Gaussian current density distribution parameter(mm) and \( I \) is current(amps).

Figure 2.5 Drop velocity measurement for one drop per every two pulses (125 Hz) (a) and one drop per pulse (250 Hz) (b)
2.4.3 DROP GENERATION

For simulated drop generation, velocity, height, frequency, temperature, and drop radius were needed. Most of parameters were obtained from analysis of video images and drop temperature was set from literature values. In the simulation, molten drops were generated as spherical shape droplets. There are two metal transfer behaviors seen in the video images in Figure 2.1. For metal transfer at early weld times (before 1.2sec), the drop frequency was one drop per every two pulses or a drop generation rate of 125 Hz and drop velocity was 0.9 m/s at a distance 1.2mm away from the weld pool surface. These values were measured from the metal transfer sequences observed in the high speed video images shown in Figure 2.5(a). At weld times of 1.2 s and greater, metal transfer was more stable and transfer rate was one drop per pulse, or a drop frequency of 250 Hz. Drop velocity was increased to 1.35 m/s, measured from the video images of metal transfer presented in Figure 2.5(b). The drop temperature for both transfer behaviors was set to be 2400K from previous research reports [41, 42].

2.5 RESULTS AND DISCUSSION

With simulation parameters determined from direct measurements and the literature, the simulation of P-GMA welding was conducted, and simulation results were validated with experimental results as detailed below.
Figure 2.6 Cross sections of actual weld (a) and simulated weld after the solidification of weld (b), and top views of the weld for the experiment and the simulation (c) after the solidification.
Figure 2.7 Cross sections of actual weld (a) and simulated weld at the termination of arc (b). Simulated penetration is deeper than actual weld.

<table>
<thead>
<tr>
<th>Deposit characteristics</th>
<th>Measured</th>
<th>Simulated</th>
<th>Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>Height (mm)</td>
<td>2.3/1.9mm</td>
<td>1.88mm</td>
<td>0.42/0.02mm</td>
</tr>
<tr>
<td>Average radius (mm)</td>
<td>8.25mm</td>
<td>8.5mm</td>
<td>0.25mm</td>
</tr>
<tr>
<td>Center Penetration (mm)</td>
<td>3.2mm</td>
<td>4.6mm</td>
<td>1.4mm</td>
</tr>
<tr>
<td>Edge Penetration (mm)</td>
<td>0.5mm</td>
<td>0.6mm</td>
<td>0.1mm</td>
</tr>
<tr>
<td>Left side toe angle (deg.)</td>
<td>30.4°</td>
<td>31.8°</td>
<td>1.4°</td>
</tr>
<tr>
<td>Right side toe angle (deg.)</td>
<td>32.6°</td>
<td>33.2°</td>
<td>0.6°</td>
</tr>
</tbody>
</table>

Table 2.3 The summary of weld geometry from the experimental results and the simulation.

2.5.1 COMPARISON OF FINAL WELD GEOMETRY

A very common way to validate weld simulations is to compare the dimensions of weld cross-sections measured from experiments with those predicted by the simulation.

In Figure 2.6, the actual weld cross-sectional images and the simulated cross-sectional
weld views are displayed in order to compare reinforcement, radius, toe angles, and penetration. The quantitative comparison of the simulation and the experimental results is given in Table 2.3. Weld reinforcement measurements of the actual weld shown in Figure 2.6 (a) vary from 1.9 mm to 2.3 mm depending on the measuring locations (A and B). The average height, 2.1 mm, is comparable to the simulated weld height of 1.88 mm. There are two penetrations observed at the center and the edge of the weld shown in Figure 2.6 (a). The inward circulation developed by drop momentum, arc pressure and Lorentz force generates the center weld penetration, but the outward circulation induced by Marangoni force and plasma drag force produces the penetration at the weld edge.

To compare weld penetrations, the simulation result at the weld termination time (1.8 s) when the maximum penetration occurs during the weld was used to measure the weld penetrations, and two clear circulations observed in Figure 2.7. In summary, differences in weld radius, the height of weld reinforcement, weld toe angles, and penetration at the edge are 10 percent, but the simulated weld penetration at the center is significantly deeper than the experimental measurement. This discrepancy is attributed to the high efficiency of drop momentum transfer and heat transfer mainly due to fluid convection in the simulation. According to Figure 2.8, metal transfer images show that molten droplets at the initial weld time up to 0.5 sec were not spherical and also the location of droplet impingement on the weld pool was somewhat random during the weld. Therefore, at the initial weld time before 0.5 sec, the experimental weld pool was not yet as developed as the simulated one and the weld metal convection that effectively transfers momentum and heat to the bottom of the weld pool was not as strong.
Sequential simulated weld cross sectional images from 0.1 s to 0.5 s are displayed to show the development of fluid flow at the early weld time in Figure 2.9. In simulation, molten spherical droplets were generated at 1.2mm above weld surface at the center of the arc and traveled straight down to the base material every time. At 0.1 s simulation time, the molten metal deposited from the welding wire simply lays on the solid base.
material. The weld does not begin penetrating into the base material until 0.2 s. At the around 0.3 s, the fluid flow in the weld pool is fully developed enhance weld penetration and clockwise and counter clockwise fluid flow circulations are also clearly observed at the edges and the center of the weld pool. The presence of two stable circulations in the weld pool is evidence of stable weld puddle. At 0.5 s, deeper penetration is observed along with larger fluid flow circulations.

Figure 2.9 Cross-section views of the simulated weld at early weld times are showing the progress of the fluid flow development and the weld penetration.
From Figure 2.9, it is concluded that the inward circulation is very significant to increase penetration at the center of weld pool. This inward circulation is caused by drop momentum, arc pressure, and Lorentz force. First, the concentrated droplet impact onto the weld pool transfers their momentum along with their enthalpy (which is high) at the center of weld, promoting the inward circulation. The Gaussian arc pressure distribution generated by the arc plasma jet flow depresses the weld pool surface at the center, also enhancing the inward circulation. Additionally, at the high welding current, Lorentz body force adds to the inward circulation. The temperature distribution coloration of the images shows many red cells at the bottom center of the weld pool and a large temperature gradient at the solid/liquid interface, which accelerates melting of the solid phase. Therefore, it is proposed that the weld penetration in the simulation is deeper.
because the development of inward fluid flow circulation pattern in the simulation occurs earlier rather than in the experiment.

In Figure 2.10, the simulated weld penetration vs. weld time is plotted to show the predicted transient weld penetration. As discussed before, the weld penetration is growing quickly due to the efficient drop momentum transfer and heat transfer when the fluid flow is fully developed. In the simulation, the fluid flow is fully developed at 0.2 s, so the slope of weld penetration curve is steep except the time between 0.8 s and 1.1 s. The absorption of drop momentum depends on the thickness of the molten metal deposit, so the convexity of molten metal at 0.8 s is large enough to absorb the drop momentum that stops penetration into the base material. After 1.2 s, the stronger arc pressure due to the transition to one drop per pulse mode depresses the weld pool surface to enhance the efficiency of drop momentum transfer into the weld pool, so the weld penetration increases from 1.2 s until the arc termination time. Interestingly, if the penetration curve is shifted to right about 0.5 s, which is the time for no penetration period due to the unstable metal transfer during the weld shown in Figure 2.8, the final predicted weld penetration is 3.35 mm which closely matches with the actual weld penetration of 3.2 mm. This also suggests that the time to develop the inward circulation of fluid flow in weld pool is related to the final weld penetration. Therefore, it is supposed that the random behavior of experimental metal transfer is a significant cause of the discrepancy of predicted and experimental weld penetration. Presumably, the discrepancy would be reduced if the computer model simulated the random behavior of metal transfer (globular transfer mode) at the initial weld time.
2.5.2 COMPARISON OF TRANSIENT WELD POOL RADIUS AND TEMPERATURE

The numerical simulation of stationary P-GMAW was also validated by comparing the transient radius of weld deposits. In Figure 2.11, the time-varying radius from high-speed video measurements and simulation predictions are plotted versus weld time. According to both experimental and simulated results, the deposit radius increased rapidly at the beginning of the weld. This is presumed to correspond to rapid spreading of the solidus isotherm on the substrate surface by direct arc heating allowing spreading of the molten metal deposit. The spreading quickly transitions to a more gradual increase. At the later stage of spreading, heat conduction and convection are the main factors to
increase the temperature at the liquid-solid junction to allow spreading of the molten metal. Of these two, heat conduction is usually considered to be less effective for heat transfer than thermal convection by fluid motion.

Figure 2.12 Temperature histories at the location 0.4mm away from the final weld edge for the thermocouple measurement and the predicted temperature from the simulation of two different mesh sizes (small mesh domain and large mesh domain).

In the experimental deposit radius curve, the initial molten metal radius quickly reached 6.5mm, which is a little bit larger than a visible arc radius (4mm) estimated from the video images. The slope of the curve was still steep until the radius reached 6.5mm, an observation that support the explanation of the influence of direct arc heat input on the
spreading of molten metal deposits. The experimental radius fluctuated at early times but is so less as the deposit grew larger. In the simulated radius curve, the deposit radius quickly increases up to 6.5mm (about 0.6 s) due to the rapid direct heating from the arc. The time for the simulated weld deposit radius to reach the gradual increase stage and the radius itself are closely matched to experimental results and the overall trend of the spreading behavior is almost identical between the experiment and the simulation.

Thermocouple measurements taken during welding were also used for simulation validation. In Figure 2.12, three curves showing temperature history at a location 0.4 mm away from the final weld edge are plotted to compare the experimental and predicted thermal history. The difference between two simulation curves is due to a small mesh domain (2.4 cm by 2.4 cm) and a large mesh domain (3 cm by 3 cm). Both simulation curves are closely matched with the experimental results until 1.5 s, but the small mesh domain results are considerably mismatched after 1.5 s due to an edge effect. This effect causes the temperature for the small mesh domain case to become too large after 1.5 s because heat transfer rate through the simulation boundary by natural convection is much smaller than heat conduction in the base material. Interestingly, the rate of temperature increase before 1.5 s is the same for both simulation cases because the conducted heat does not reach the computational boundary until this time. Based on the temperature history validation, 3 cm is considered to be a sufficient computational domain size for the accurate computer simulation of the results over the time being considered.
Figure 2.13 Coordinates of large mesh used for half symmetry simulation of stationary spot welds.

A sketch of the large simulation mesh mentioned in the preceding paragraph and used for subsequent simulations is provided in Figure 2.13. This mesh consisted of 5 blocks with 132,400 cells. The size of cells in the center block was 0.25 mm while that of other blocks was 0.5 mm. The simulations were run on a dual 3.4 GHz Xeon processor workstation with 2 GB of RAM and the software was compiled dual process use. Simulating 3 s of weld time required 62 hrs. of “wall-clock” time. It was found that mesh size was most critical for accurate simulation of transferring droplets and the center block mesh size mentioned above was chosen for mesh size independent simulation of this aspect of the process.
Based on these three validations of the weld simulation, the stationary P-GMAW weld simulation was considered reasonably accurate for prediction of the final and transient weld profiles. In subsequent simulations, Gaussian heat distribution parameter, Gaussian current distribution parameter, Gaussian pressure distribution parameter, and total arc pressure were individually varied to understand their effects on fluid and heat flow and weld pool penetration by comparing with validated simulation results.

Figure 2.14 Comparing the transient radius between the baseline P-GMAW simulation and various cases.
2.5.3 SIMULATION PARAMETER EFFECTS

Gaussian heat distribution parameter was increased by a factor of 1.5 with same total heat input to study its effect on the weld profile and fluid flow patterns. Larger Gaussian heat distribution parameter corresponds to a broader heat distribution and less heat input intensity at the center of the arc, so it is expected that more time would be required to melt the base material underneath of the arc. According to the transient radius plots in Figure 2.14, the weld deposit for the large heat input radius case did not spread quickly as the validated simulation at initial times because the solid base material
adjacent to the molten metal deposit was cooler. At later weld times, the weld pool radius was more well-matched with the validated simulation deposit radius.

Figure 2.16 Cross sections at the initial weld time (a) and at the later weld time (b) for the large Gaussian heat distribution parameter case.

In Figure 2.15, the transient weld penetration was displayed to study its effectiveness on the weld penetration. As a result, the final weld penetration was decreased as expected because less energy is transferring to the bottom of weld pool due to the low energy deposit at the center of weld pool. At the initial weld time, the weld penetration was not generated until 0.2sec due to the low energy deposit from the arc, weld cross sectional view displayed in Figure 2.16(a). The weld started penetrating at 0.3sec due to heat conduction from molten metal deposit and heat convection induced by several forces involved in the weld pool. After 0.3sec, the weld penetration is growing as fast as one for the validated simulation until 0.8sec weld time. The same penetration
behavior (no penetration increase) was observed between 0.8sec and 1.2sec compared to the validated simulation weld because the sufficient molten metal deposited absorbed drop momentum to prevent the penetration. Again, the weld penetration is increasing after 1.2sec due to the increase of arc pressure depressed the weld pool surface, but it transitioned to steady state at the end of a weld time due to the lower enthalpy transferring from the weld pool surface to the bottom of the weld. Figure 2.16(b) presented the low temperature of molten metal near the surface and at the bottom of weld pool compared to the validated simulation shown in Figure 2.7(b).

Figure 2.17 Cross section view at 1.55sec for the smaller Gaussian current density parameter to show the increased weld penetration due to the stronger Lorentz force.

Lorentz force usually becomes the dominant factor at high welding current, so Gaussian current distribution parameter was reduced by the factor of half to study its
effectiveness on the weld pool. The decrease of Gaussian current distribution parameter produces the constricted current flow through the weld pool, so the stronger Lorentz force is generated near the center of weld pool. Due to the strong body force near the weld pool center, the strong inward circulation is expected to increase the weld penetration.

As shown in Figure 2.17, weld penetration increased from 4.6mm to 6.35mm (thickness of plate) and the deep finger like penetration shape is achieved. Another interesting characteristic revealed by the flow vectors in this figure is the small counter fluid flow in the edge of weld pool. Usually, Marangoni force produced by a negative surface tension gradient case and drag force generated by arc plasma jet force the fluid on the weld pool surface to flow outward, a direction that is against that induced by the Lorentz force. In this case, the Lorentz force was strongly distributed near the weld pool center that it dominated the fluid flow pattern, producing a strong inward circulation of fluid flow. The transient weld penetration plotted in Figure 2.15 shows that the slope of curve is steeper than other cases, and the weld penetrated through the base material at 1.55sec before the termination of the arc shown in Figure 2.17. One interesting point in this plot between 0.8sec and 1.2sec is that the inward circulation mainly enhanced by Lorentz force overcomes the barrier generated by the molten metal deposit as a momentum absorber so the weld penetration kept increasing but it slows down. After 1.2sec, the slope is even steeper due to the combination of the increased arc pressure and high Lorentz force. Figure 2.14 shows the transient radius of molten metal deposit. As discussed before, the outward circulation is mainly spreading the molten metal deposit, so the molten metal deposit is spreading as fast as the validated simulation until 0.5sec but it
slows down due to the small outward circulation at the edge of the weld pool shown in Figure 2.17.

For arc pressure, to demonstrate the effect of arc pressure, the total force and Gaussian pressure distribution parameter were individually varied while other simulation variables were held fixed. First, the one fifth of arc force is applied to study because the arc pressure used in the simulation is strong enough to obtain a deep penetration. As discussed in previous section, arc pressure and drag force are coupled together, so their magnitudes are proportional each other. Therefore, the decrease of total force induces the weaker drag force. In Figure 2.14, the trend of transient weld pool radius for the reduced arc pressure is showing no influence on the molten metal spreading. The weld penetration was plotted in Figure 2.15. Before 1.3sec, the trend of weld penetration is following the validated simulation case, but at the later time the weld penetration is suddenly becoming steady. The final weld penetration is eventually decreased by 0.75mm.

As discussed before, the inward circulation of fluid flow causes the increase of weld penetration. The magnitude of this circulation and the amount of enthalpy contained in molten metal are the main factors to determine the depth of weld penetration. Therefore, the stronger inward fluid flow circulation and the hotter molten metal accelerate the weld penetration. The amount of enthalpy in the molten metal on the weld pool surface is proportional to the arc direct heat deposit, so this factor is not required to consider the change of weld penetration as long as the same arc heat input distribution applied. The mechanism to explain the decrease of final weld penetration can be described by the inward circulation of fluid flow. There are three forces involved to
determine the magnitude of inward circulation. First, the lateral distribution of arc pressure causes the inward fluid flow so that the hot molten metal heated by the direct heat input to increase weld penetration. Second, metal transfer is a major source of heat and drop impact momentum to increase GMA weld penetration. Finally, Lorentz force at the high welding current induces the inward fluid flow to increase the weld penetration. During the weld, these forces are simultaneously acting on the weld pool to determine the magnitude of the inward fluid flow circulation.

![Figure 2.18 Cross section views of one fifth of total force at 0.5sec (a) and 1.7sec (b). Final weld penetration is decreased due to the reduced total force.](image)

In Figure 2.18, there are two cross-sectional views at 0.5 s and 1.7 s weld time displayed to explain the weld penetration characteristics during the weld. At the early weld time, there is not enough molten metal deposit to form the sufficient convexity of weld reinforcement acting as a damper to reduce the weld penetration even though the
weak arc pressure was applied on the weld pool surface. During the early weld time, Lorentz force and drop impact momentum are the dominant factors to increase the weld penetration. Molten metal eventually piled up as weld time goes on, so the amount of molten metal deposit on the substrate is sufficient enough to form the thick convexity to absorb the drop impact momentum displayed in Figure 2.18. Therefore, the weld penetration is transitioned to gradual increase as shown in Figure 2.15.

Another variable to change the arc pressure distribution is Gaussian pressure distribution parameter that increased by the factor of 1.5 with a fixed total force. The increase of this parameter decreases the maximum pressure and increases the area to apply. The transient weld pool radius and penetration were plotted in Figure 2.14 and 2.15. The trend of transient weld pool radius is following the validated simulation, but the penetration curve is different from the validated simulation at the later weld time (after 1.2sec). Therefore, the final weld penetration is decreased to 3.6mm due to the decrease of maximum arc pressure at the weld center.

In summary, the weld radius is not strongly influenced by simulation parameters except Gaussian current distribution parameter, but the weld penetration is affected by all simulation parameters tested so far. According to previous demonstrations, it is not easy to modify the final weld profile, especially the spreading of molten metal, by given welding parameters demonstrated so far, thus the additional heat source such as a laser beam can be applied into the welding process in order to increase the controllability of the final weld shape during the weld.
2.5.4 PRELIMINARY STUDY OF ADDITIONAL HEAT SOURCE

Previous sections demonstrated how welding parameters could control the final weld shape, but in this section, the effect of the additional heat source that can enhance the controllability of the final weld shape is investigated. To improve the wetting characteristics of molten metal for the desired weld shape, the additional heat source such as laser beam is applied to demonstrate the controllability of the final weld shape using the numerical simulation as a tool.

For the demonstration of the improvement of molten metal wetting, the simulation of two heat sources is performed to show the effectiveness of additional heat source (it can be any heat source) adjacent to the edge of weld pool. The defocused laser beam as the second heat source was modeled to perform and the results are shown in Figure 2.19. According to Figure 2.19, the molten metal wet over into the additional heat spot (laser spot) and it demonstrates the change of the final weld shape. Especially, toe angle where the wetting occurs became smoother due to the additional heat source. Consequently, this preliminary test results using the 3D numerical simulation as a process development tool can provide the insight for the process development of improving the controllability of the final weld shape for the further investigation.
Figure 2-19 3-D sequential images (a) and cross section of two-heat sources simulation result (b). The additional heat is applied at the right edge of weld pool with 1mm radius of laser beam.
2.6 CONCLUSIONS

VOF technique was used to implement a simulation of stationary P-GMA welding that included non-isothermal free-surface fluid flow. Buoyancy, Marangoni, arc pressure, drag, and Lorentz forces were mathematically modeled and implemented in the numerical simulation. P-GMA arc size and droplet transfer were measured from arc images and used to determine some simulation variables. Direct comparisons of predicted and measured weld geometry, time-varying deposit radius curve, and temperature history from thermocouple measurement were used to validate the P-GMA stationary welding simulation. The simulation predicted deeper weld penetration than those measured experimentally, a difference that was attributed to consistent droplet impact location in the simulation versus random droplet impact in the experiments.

Simulation tests with individual changes of variables (Gaussian heat distribution, Gaussian current distribution, total force, and Gaussian pressure distribution) provided insight into the effects of these variables on fluid flow patterns and weld penetration. The large heat distribution with a fixed total heat input decreased the weld penetration, the constricted current density distribution drastically increased the weld penetration but decreased the weld radius, and the reduced total force and the large arc pressure distribution radius decreased the weld penetration. Overall, the parametric study of accomplished using the weld simulation demonstrated the effects of physical variables on weld pool fluid flow and provided key insights for further application of the model as a tool to assist in weld process development.
2.7 REFERENCES

44. A. Joseph, D. Harwig, D. Farson, and R. Richardson: *Science and Technology of Welding and Joining*, 2003, **8**(6), 400-406
CHAPTER 3

UNDERSTANDING OF BEAD HUMP FORMATION USING NUMERICAL SIMULATION OF ARC WELDING PROCESS

3.1 INTRODUCTION

Increased productivity is desirable in manufacturing industries because it reduces the cost of completed products. For welded products, productivity increases with welding travel speed, but some welding defects related to bead shape, such as formation of bead humps and undercutting at the weld edges limit the maximum feasible travel speed. Because hump formation limits welding productivity, better understanding the humping phenomena during the welding process is needed to arrive at process modifications that decrease the tendency for hump formation and allow higher productivity welding.

Bradstreet [1] was the first researcher to experimentally study bead hump formation in the gas metal arc welding (GMAW) process. Humping was defined as the
series of undulations of the weld bead that was always associated with undercutting along the edges of the humped beads. Inspired by observed experimental results, both welding defects were proposed to be produced by surface tension force (namely, capillary or Rayleigh instability [2]) and fluid flow patterns during the weld. The Rayleigh instability mechanism bears apparent similarities to the humping phenomenon, so it has been adopted and extended by many researchers to explain and describe the shape of molten beads deposited on a solid surface and also determine conditions for instability by capillary forces [3-5]. An example is the theoretical analysis proposed by Gratzke et al. [6] to explain the humping phenomenon based on capillary instability theory. According to the analysis, conditions at the contact line along the edge of a liquid deposit determine whether humps form or not. If a molten bead on a surface satisfies the wetted condition, meaning that the internally measured contact angle is less than $\pi/2$, it is not susceptible to hump formation by capillary instability. Otherwise, according to this analysis, hump formation by capillary instability is possible. However, in the welding application, the wetting and spreading of molten metal is controlled not only by the surface energies at the interface of liquid, solid, and gas phases but also by heat transfer and phase transformation. For example, the spreading of molten metal is arrested when the metal freezes. Therefore, humping phenomenon during the high speed fusion welding process is not fully explained by capillary instability, although it seems evident that this theory provides valuable insights. Farson et al. [7] showed that preheating of thin plates by a defocused laser beam increased the critical travel speed (the speed above which humped
beads form). Analysis of results suggested that capillary instability, by itself, does not explain hump formation.

Other models [8] based on the fluid flow of molten metal in the weld pool have been proposed to explain the humping phenomenon observed during GTAW and GMAW. Mendez and Eagar [9] proposed that strong arc pressure pushes the molten metal back to form humps. More recently, based on inferences from high speed video images, Nguyen et al. [10] proposed a backward flow stream that originates from the bottom of the weld pool crater and sweeps backward along the centerline solidification boundary to the trailing edge of the pool. Based on video images, they propose that two other rearward-directed streams flow further up on either side of the crater and merge with the centerline flow somewhere between the crater and the trailing edge of the weld pool to become the strong backward fluid flow, referred by these authors as a ‘wall jet’. A flow very similar to the wall jet of Nguyen et al. [10] was also observed by Bradstreet under some welding conditions. According to experimental results of Nguyen et al. [10], the backward fluid flow induced by various forces mentioned above was the dominant factor of hump formation in their experiments.

Thermocapillary (Marangoni) flow has been proposed [11, 12] to explain hump formation due to the characteristics of flow produced by this mechanism. The role of surface-active elements (e.g. sulfur) in Marangoni flow was investigated by Heiple and Roper [13, 14]. High sulfur steel has a positive surface tension gradient over a range of temperatures that induces inward fluid flow on the weld pool surface. This flow elevates the pool surface at the center and depresses it near the edge. This shape of molten metal
deposit was proposed to be more susceptible to hump formation. It is noted that a subsequent analysis by Gratzke et al. [6] concluded that thermocapillary forces most likely play a relatively minor role in hump formation. The literature is mentioned here because thermocapillary force is easily-varied and its effects studied. Results will be discussed in this paper.

In previous research, investigation of humping phenomenon has heavily relied on the observation of final weld geometry, microstructures, parametric study of welding parameters and conditions, and transient weld profile images captured using high speed video imaging. According to these observations and experiments, several hypotheses have been proposed to explain bead humping in both autogenous and non-autogenous welding processes at high travel speed. A review of literature suggests that many of the proposed explanations are valid, but it is also clear that bead hump formation is complex and probably involves more than one mechanism depending on process variable settings. The diversity of causes for hump formation that were reasonably identified by Eagar, Bradstreet and other implies that influence of different factors vary with the magnitudes of arc welding current, travel speed, material deposition rate and perhaps other process variables as well. Partly because of this complexity, it can also be noted that there are impediments to understand humping entirely based on experimental measurements. The time varying volumetric temperature and flow velocity distribution measurements that are needed to analyze the phenomenon under varying conditions are very difficult to acquire experimentally.
Because of the detailed knowledge of physical conditions that it offers, computer simulation [15-18] that has been developed for the decades was undertaken to provide the knowledge of heat and fluid flows leading to a deeper understanding of humping phenomenon during the high travel speed weld. In the chapter 2, the stationary pulsed gas metal arc welding (P-GMAW) weld pool and bead deposit associated with heat and fluid flows were mathematically modeled using 3D Cartesian coordinate system, and the governing equations based on mass, momentum and energy conservation relationships were solved numerically to simulate the arc welding process using the Flow3D CFD code. For accurate simulation of boundary and body terms associated with the welding arc, video images and electrical waveforms were acquired to determine relevant simulation parameters. Using experimental results as a guide and simulation results for detailed temperature and flow field information, the mechanisms involved in bead hump formation for P-GMAW at high travel speed are discussed.

<table>
<thead>
<tr>
<th>Nomenclature</th>
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<td>Background power (W)</td>
<td>2400</td>
</tr>
</tbody>
</table>

Table 3.1 P-GMAW current waveform and welding parameters
Figure 3.1 Sequential video images of the formation of first and second humps. The plate was traveling to the left at 25.4 mm/s and the welding arc was just beyond the right side of the image.

Continued
3.2 EXPERIMENTAL APPARATUS AND PROCEDURE

For the investigation of bead hump formation, bead-on-plate welds were made at travel speeds varying from 12.7 mm/s to 25.4 mm/s in 4.23 mm/s increments while other welding parameters were fixed at the values shown in table 3.1. The welds were made on 6.35 mm-thick ASTM A-36 steel, containing 50 ppm sulfur, with sand-blasted surface preparation. The power supply used was a Thermal Arc 500P. At a travel speed of 25.4 mm/s, humped beads were formed, so additional welds were repeated with same parameter settings at this travel speed. The humped welds made at 25.4 mm/s travel speed and uniform weld beads without hump formation that were made at 12.7 mm/s are compared in the analysis.
Material properties of A-36 steel used for the simulations are shown in Table 2.1. Experimental data needed to simulate arc size and heat input were acquired by a high speed CCD camera and a data acquisition system (DAQ). Video images of the arc and molten metal pool were captured by a high speed CCD camera at 250 frames per second with a 950nm +/- 10 nm band pass filter to block unwanted arc light. Welding current and voltage waveforms were acquired at a sampling rate of 10 kHz. Peak values and background values of current and voltage, pulse frequency, and pulse duration were obtained from these waveforms and used to calculate the average power, peak power and background power for the process heat input. The waveform parameters and welding parameters used for the simulation are displayed in table 3.1.

3.3 SIMULATION AND CALCULATED SIMULATION PARAMETERS

Very detailed explanations of the weld pool simulation and experimental validations were presented in the chapter 2. However, experimental measurements and calculations needed to determine arc simulation parameters are described in detail below. In the simulation, the computational domain was meshed with 272,000 cubical cells with a size of 0.25mm. The calculations were done by a system with dual 3.4 GHz Xeon processors and 2 GB of RAM. The number of iterations during the entire simulation was 133198, and time step, limited by the surface tension convergence criteria, was $2.3 \times 10^{-5}$ s. An example of the wall clock computation time was 89 hours to simulate 3 s of weld
time. The Volume of Fluid (VOF) simulation technique was used to track the
deformation of the weld pool surface due to buoyancy, surface tension gradient, arc
pressure, Lorentz and other forces that were mathematically modeled and implemented in
the numerical simulation. An appropriate mesh size was chosen to minimize extraneous
predictions of small deformations in surface shape and bubble and spatter formation.

Surface tension was varied for this study, and it was calculated from the formula
developed by Sahoo, Debroy and Mcnallan [21]. The equation of surface tension as a
function of temperature with sulfur active element in a binary Fe-S system is expressed
by

\[
\gamma(T) = \gamma_m^o - A \cdot (T - T_m) - R \cdot T \cdot \Gamma_s \cdot \ln(1 + k_i \cdot a_i \cdot e^{-\Delta H^o / RT})
\]  

(3.1)

where \(\gamma_m^o\) is the surface tension of pure metal at the melting point, 1.943 N-m, \(A\) is the
negative of surface tension gradient for pure metal, 4.3x10^{-4} N-M/K, \(T_m\) is the melting
point of the material, 1798 K, \(R\) is gas constant, \(\Gamma_s\) is the surface excess at saturation, 1.3
x10^{-8}, \(k_i\) is the entropy factor, 0.00318, \(a_i\) is weight percent of sulfur, 0.005%, and \(\Delta H^o\) is
the heat of absorption, -1.66x10^{+8} J.

In addition to the experimental measurements described above, other arc
simulation parameters were obtained based on values or relations from literature. The arc
heat input was calculated from the average power listed in table 3.1 with an arc efficiency
used of 0.74, measured by liquid nitrogen calorimetry [22]. Based on peak and
background arc powers displayed in table 3.1, the actual power input to the base material was further divided into the arc power and the power consumed for molten drop generation, the latter being subsequently deposited in the weld pool.

The heat input and pressure distributions and total arc force were significantly different from previous simulations of the stationary pulsed gas metal arc welding process in the chapter 2 because the video images in Figure 3.1 reveal that welding variables used in this work resulted in a “buried arc” that was confined to a cavity formed in the molten weld pool by arc pressure. Therefore, the heat input and pressure distributions used in the simulation were modified accordingly. First, the direct heat input from the arc was modeled as a Gaussian distribution with Gaussian heat distribution parameter calculated from an empirical equation [20, 23]. This equation is a function of current and for a 4 mm arc length is expressed as

\[
\sigma_a = 0.533I^{0.2941}
\]

(3.2)

where \(\sigma_a\) is Gaussian heat distribution parameter (mm) and \(I\) is current (amperes). To represent the constricted arc, the result of this calculation was divided by 2. Furthermore, material outside of a 2.3 mm radius (measured from experimental welds as the half-width of the gouged region visible in the narrow portion of humped beads) was not allowed to receive any thermal energy from the arc. To conserve the energy input, the thermal energy outside of this truncation radius was added back into the direct heat input by increasing the magnitude of the Gaussian distribution inside of the radius.
For the arc pressure distribution of the constricted buried arc, total arc force for the buried arc was obtained from the literature [24], and Gaussian pressure distribution parameter, is modified by reducing the result from the empirical equation (4) by 2. According to the previous research papers [24, 25], the empirical equations of the total force for zero-immersion case and Gaussian pressure distribution parameter as a function of current were expressed as

\[
P = 0.0000176398 \cdot I + 0.0123291925 \cdot I (g)
\]

\[
\sigma_p = 1.4875 + 0.00123 \cdot I (mm)
\]

where \( P \) is the total force (g), \( I \) is current (amps), and \( \sigma_p \) is Gaussian pressure distribution parameter (mm). Ultimately, the justification for empirical adjustments to the arc heat input and pressure distributions mentioned above depends on comparison to experiments. As shown below, the modifications described here produce reasonable results. Accurate prediction of these distributions by incorporation of a physical simulation of the arc is a future goal of the authors.
3.4 RESULTS AND DISCUSSIONS

Transient weld pool images captured using the high speed CCD camera such as those shown in Figure 3.1 and the final weld geometry such as that shown in Figure 3.2(a) were acquired for the observation of hump formation and the solidified hump shapes. The weld simulation results shown in Figure 3.2(b) are in good correspondence with the experiments. For example, the height of the first humps in both the simulation and the experiment are precisely matched at 6 mm. The period between the first and second simulated humps is 32 mm and that distance was 44 mm in the experiments. As a consequence of this shorter period, the height of the second simulated hump is 3.6 mm and the experimental height is 5 mm. Humping is defined as periodic undulations of the weld bead such as those in Figure 3.2(a), where the first two humps of a humped bead are shown in top and side views. In the seminal work of Bradstreet [1], it was said that hump formation was always associated with undercutting (negative reinforcement). The presence of undercutting is noted along the edges of the weld bead in the depression between humps in both the experimental and simulation top views in Figure 3.2. Overall, the contours of the experimental and simulated beads are also in good agreement so the simulation predictions were further analyzed to understand the physical mechanisms involved in hump formation.
Figure 3.2 Top and side views of humped bead-on-plate welds made at travel speed of 25.4 mm/s: actual weld bead (a), simulated weld bead (b).
3.4.1 HUMP FORMATION: EXPERIMENTAL

Sequential images in Figure 3.1 show the variation of the shape of the molten metal deposit as successive humps were formed. During the initial weld time, the molten metal was pushed to the back of the weld pool, forming a swelling. As time increased, the weld pool became longer, the swelling increased in volume and its shape fluctuated due to oscillations. After 0.6 s, the swelling remained connected to the trailing edge of the continuously-elongating weld pool by a thin liquid channel. However, the initial swelling of liquid metal continued to grow and this configuration persisted until 1.3 s. Then, the bridge connecting between the front and the rear weld pools solidified and trapped the molten metal in the back part of the weld pool, forming the first hump.

After the formation of the first hump, molten metal still accumulated at the back of the weld pool to start forming a new liquid bulge after 1.9 s weld time, as seen in Figure 3.1. During the second hump formation, unlike the first hump, most of the growth was in the middle of the weld pool rather than at the back. As seen in Figure 3.1, the shape of the accumulated melt was almost symmetric with respect to its center during the hump formation. This melt shape appears to be due to restraint by a larger surface tension pressure at the back of the pool and a different surface flow pattern than that observed during the initial stages of the first hump. These differences are examined in more detail below. The temperature and fluid flow distributions of second hump formation at the later stage are same as the first hump formation. The third hump formation begins at 3.0 s weld time.
Figure 3.3 Sequential side view images of temperature (K) profile for the simulated weld deposit and video images during the first hump formation. The solidified metal at the bottom of the back of the weld deposit matches the simulation prediction (solidus temperature $T_s = 1768$ K).
Figure 3.4 Sequential images of temperature (K) predicted profile during the first hump formation in (a) top view and (b) cross sectional view.

3.4.2 HUMP FORMATION: SIMULATION

The experimental images were compared to simulation results to obtain a more complete understanding of the humping phenomenon. In particular, the simulated temperature and velocity distributions of molten metal in the weld pool provided useful information about conditions during hump formation. In Figures 3.3 and 3.4, contour plots of the temperature distribution of the molten metal deposit are displayed. Figure 3.5 displays the magnitude of the longitudinal (x-direction) velocity component of the molten metal in the weld pool.
Figure 3.5 Sequential images of the longitudinal component (m/s) of the velocity field during the first hump formation in cross sectional view (a) and side view (b).

During the early stages of the first hump formation, the cross-sectional views in Figures 3.4 and 3.5 show that a longitudinal flow circulation pushes molten metal back to form the swelling in the trailing of the weld pool. This prediction corresponds to the observations of Nguyen et al.[10], who mentioned that accumulation of liquid at the back to the weld pool initiated hump formation. Due to the subsurface pattern of the longitudinal circulation, the relatively hot molten metal from the front of the weld pool advects a large amount of thermal energy from direct arc heat input and molten droplets along the solid/liquid interface (the bottom fusion line). The energy carried by this flow competes against solidification and maintains the weld pool depth to produce a thick molten metal layer near the arc. It is also notable that the longitudinal flow shown in
Figure 3.5 has a backward direction along the outer sides of the melt deposit and is frontward-directed in the cross-section at the weld centerline. This circulation does not appear to play a major role in hump formation but is not easily detected experimentally, so is worth mentioning.

Figures 3.4 and 3.5 show that after 0.6 s, the liquid swelling becomes elongated because the welding arc is moving to the right at the preset travel speed. Also, the fluid flow pattern gradually shifts from clockwise (subsurface) circulation to counter clockwise (surface) circulation as the molten metal deposit becomes shallower. This change in flow pattern occurs after about 1.2 s weld time in Figure 3.4 and is examined in more detail further below. Also, surface tension normal force is observed to play an important role in the fluid configuration. As shown in Figure 3.4(a), the molten bead becomes narrower to form a thin liquid channel that has a fairly large curvature. Variation of curvature along the liquid channel causes an unbalanced normal surface tension force that leads to constriction of the channel, slowing the exchange of metal between the leading part of the weld pool and the backward portion, where hydrostatic head is larger because of the liquid height.
Figure 3.6 Perspective and cross sectional views at the same location during the first hump formation show the contact angle before and after pinching. The internally measured contact angle shows instability (is greater than $\pi/2$) before pinching and stability afterward.

The simulation results are consistent with a hypothesis that capillary instability plays a role in hump formation. As noted above, when the contact line is arrested, the contact angle is the main factor that determines the stability of a molten bead of liquid. Perspective and cross sectional views at 1.4s (Fig. 3.6) show that the contact line of molten bead is arrested by freezing while the bead above the base material surface level
is still in the liquid state. In the cross sectional view at 1.4 s, the moment before the unstable pinching of the molten bead occurs, the internally measured contact angle (104 deg.) fulfills the condition for instability of molten bead. Pinching of the molten bead is clearly observed at 1.5 s. After pinching, the contact angle at the neck is a more stable 74 deg. The radius of curvature is slightly less at 1.5 s and the surface tension pressure is strong enough to restrain the hydrostatic pressure of the larger mass of accumulated liquid behind the restriction. Finally, at 1.95 s weld time (Figs. 3.7 and 3.8), the thin liquid channel connecting the front and the rear weld pools is pinched again and quickly solidifies. After this time, no more mass is added to the first hump although the fluid is still molten and its shape continues to fluctuate until all of the melt solidifies.
Figure 3.7 Sequential images of temperature profile for the simulated weld deposit in side view (a) and cross sectional view (b) at the end of the first hump formation.

Summarizing, analysis of simulated temperature and fluid flow distributions suggests that there are two main requirements for hump formation: elongation of the molten weld deposit that induces a strong pinching force and rapid solidification that freezes the narrow channel, thus assuring the preservation of the humped shape. A third factor, a strong backward fluid flow that pushes molten metal to the back of the weld pool, is not a sufficient condition for hump formation as will be shown by analysis is welds made at slow travel speeds. However, it is noted that this factor has major
influence on the size of the solidified humps. If not much metal is accumulated by backward flow, the magnitude of the undulations is smaller.

Figure 3.8 Sequential images of velocity profile (m/s) for the simulated weld deposit in side view (a) and cross sectional view (b) at the end of the first hump and beginning of the second.

The interaction of fluid flow dynamics and solidification contributes to the complexity of the bead humping phenomenon. For example, simulations at slower travel speed have shown that transient pinching of the melt does not necessarily result in
humping of the solidified bead. Either oscillations in the melt or backward flow of filler material can cause the pinched region to transition back to the more uniform bead geometry before it solidifies. Thus, the shape of the final solidified bead may not reflect the fact that the liquid deposit had a transient pinched geometry.

Figure 3.9 Sequential longitudinal cross sectional images of temperature (K) (a) and longitudinal velocity (m/s) component (b) at the beginning of the first “bulged” hump formation.
3.4.3 FLUID FLOW PATTERNS DURING FIRST AND SUBSEQUENT HUMPS

Two distinct hump shapes, referred to as bulged and elongated humps in this chapter, can be noted in Figure 3.2(a). The corresponding weld metal temperatures and shapes during formation of these types of humps were also observed to be different in the video images in Figure 3.1. Usually the first hump that occurred in experimental welds was bulged hump. Depending on weld conditions, subsequent humps were either bulged or elongated. If the conditions were such that backward flow rate was not as high, elongated humps were usually formed after the first hump. The shape of humps was strongly related to the fluid flow pattern, so the formation of each hump shape was studied based on the fluid flow patterns obtained from the simulation results.

The simulated longitudinal flow velocity distributions displayed in Figure 3.9(b) and 3.10(b) show two different backward fluid flow patterns during formation of the first hump in the weld bead and the second and subsequent humps. The pattern of the longitudinal fluid flow component during formation of the first hump (Fig. 3.9b) is a clockwise submerged circulation whereas the pattern during formation of the second and subsequent humps is a counter-clockwise surface circulation (Fig. 3.10b). It can be concluded from these plots that the steeply-sloped front edge of the thick molten metal deposit that is present during the initial weld times causes the clockwise circulation in Figure 3.9(b). The pressure needed for redirection of flow of incoming metal droplets to remain on the weld deposit surface is large enough that the flow instead enters into the volume. On the contrary, the counter clockwise circulation seen in Figure 3.10(b) is
developed because the less steeply-sloped surface of the relatively thin molten metal accumulation allows the deposited material to flow on its surface.

Figure 3.10 Sequential longitudinal cross sectional images of temperature (K) (a) and longitudinal velocity (m/s) component (b) during the second “elongated” hump formation.

As expected, the weld pool temperature distribution is strongly related to the fluid flow pattern. As shown in Figure 3.9(a), most of heat input from the electric arc and molten droplets is advected along the fusion line by the fluid flow. This flow supplies thermal energy at the solid/liquid interface that causes melting into the workpiece at the front of the pool. Consequently, the slope of the fusion line is positive (pool depth is increasing) or nearly horizontal closer to the arc and rapidly becomes negative (pool
depth decreases) at the back of the weld pool because of the dissipation of the thermal energy and redirection of the flow by an increased positive z-axis (upwards) component. In contrast, the counter clockwise fluid flow pattern shown in Figures 3.10(b) distributes hot molten metal from the front of the weld pool backwards along the free surface. As a result, there is less thermal energy deposited at the solid/liquid interface and the solidification rate is uniform along the fusion line, which has a uniform negative slope (decrease in pool depth) along the length of the pool. Therefore, the thickness of molten metal deposit is the primary factor that influences the weld pool fluid flow pattern and is consequently a significant factor in weld bead hump formation.

In Figure 3.9, at the initial stage of the bulged hump formation, the spreading of the molten metal deposit arrests quickly at the tail of weld pool due to the cold base material there. There has also been little time for heat flow in lateral directions, so spreading is also constrained and the deposited filler metal simply accumulates in the confined region. This accounts for the larger height and the bulged hump geometry. At later stages of the bulged hump formation, the longitudinal fluid flow pattern transitions to a surface flow. Rapid solidification, which is a necessary step for the hump formation, occurs at the neck, and the bulged hump is formed. Therefore, the presence of the counter-clockwise circulation indicates the final stages of the bulged hump formation.

After the first hump formation, usually there is a thin long liquid layer left behind because surface tension has pushed molten metal back into the hump. The melt flows on the surface of the shallow liquid as shown in Figure 3.10(b). Therefore, two requirements for hump formation, strong backward flow and a thin liquid channel (which leads to
surface tension pinching) are met. An elongated swelling is generated and a hump is formed after rapid solidification of the narrow region. Figure 3.10(a) graphically demonstrates the rapid solidification of molten metal that completes the formation of the elongated hump. The fact that the fluid is not constrained by freezing at the back of this hump causes it to be longer and not as high as the hump having bulged shape.

3.4.4 ROLE OF GOUGED REGION

A couple of effects of a deeply penetrated (“gouged”) region during the hump formation are noted. First, deep penetration was enhanced because the procedure used in the experiments corresponded to a short arc length. The combination of relatively large current and short arc length produced an arc that is sometimes referred to in the literature as a “buried” arc. Prior work, such as that by Adonyi et. al [24], has investigated the strong pressure generated by buried arcs. Basically, the pressure pushes the molten metal back, allowing increased penetration. Also, the buried arc has a constricted heat input distribution with more direct heat input to the base material in the penetration region.

Simulations have show that the deep, narrow penetration region promotes hump formation. The slope of the leading edge of a gouging region shown in Figure 3.4(b) efficiently redirects the downward momentum of incoming droplets toward the back of weld pool, increasing the velocity of the backward fluid flow. As mentioned before, backward fluid flow promotes the initiation of hump formation. However, this is not a
sufficient condition since the non-humped weld shown in Figure 3.14 also has a steep leading edge.

A gouged region also helps to constrain the melt deposit laterally, which promotes hump formation. The steep and high sidewalls prevent wetting of molten metal beyond the gouged region and insufficient liquid metal only partially fills the penetration, so an undercut weld results. The presence of undercutting produces a sharply-curved surface geometry and the associated large surface tension pressure promotes pinching, which accelerates hump formation. The large wetted area of the molten metal in the deeply-penetrated gouged region increases thermal conduction, promoting rapid solidification of the channel. This solidified region separates the weld pool in two and is identified as one of the basic requirements to form a hump. Therefore, for a number of reasons, the presence of a deeply penetrated gouged region encourages hump formation and simulations show that beads with such deep penetration may hump at a slower travel speed, all other conditions being equal.
Figure 3.11 Sequential longitudinal cross sectional images of temperature (K) (a) and velocity profiles (m/s) component (b) for the simulated weld deposit with one third of surface tension force at 1.0 s, 1.2 s, and 1.4 s. The simulation restarted at 1.0 s with one third of surface tension.
3.4.5 ROLE OF SURFACE TENSION AND SURFACE TENSION GRADIENT

Surface tension mainly is comprised of a normal component that produces the pinching force and a tangential component that generates Marangoni flow. As discussed in previous sections, strong surface tension pinching force in a thin liquid channel plays an important role for hump formation. Marangoni flow induced by surface tension gradients also has major effects on fluid flow in the weld pool and it has been proposed that it may play a role in hump formation. Due to the flexibility of numerical simulation, the importance of surface tension pinching force and Marangoni flow in hump formation could easily be investigated by varying corresponding fluid properties. In this section, weld simulation results with surface tension reduced to one third of normal and with no surface tension temperature coefficient (i.e. constant surface tension) are compared to prior results. All previous simulation results correspond to material with negative surface tension temperature gradient.

The simulation with surface tension decreased to one third of normal was accomplished by re-starting a simulation with normal surface tension at 1.0 s. The restarting time was chosen because it is the time when the longitudinal fluid flow pattern had just begun to transition from a sub-surface flow to a surface flow (this transition is seen in Figure 3.5). In the restarted simulation, the surface tension pinching force on the liquid channel is not strong enough to hold the accumulated molten deposit to the back of the weld pool. Thus, the fluid flow pattern shown in Figure 3.11(b) never transitions from subsurface flow to surface flow as the case in Figure 3.5(a), a thick molten layer is
maintained and a hump does not form. This provides further evidence for a conclusion that surface tension pinching force is a significant factor in hump formation.

Figure 3.12 Sequential cross-sectional images of temperature distribution (K) from 1.0 s to 1.9 s for the constant surface tension case.

The role of Marangoni flow in humping was studied. In previous results in Figure 3.7, the phase of hump formation where surface tension gradient is deemed most likely to have an effect is in the thin liquid channel between the first and second humps. As shown by the surface flow at 1.2 s in Figure 3.4, the trailing end of the channel is somewhat cooler than the leading end so Marangoni flow pushes hot molten metal backward, thus somewhat aiding hump formation. In the simulation result for constant surface tension shown in Figure 3.12, no tangential force is generated, so fluid flow is mainly due to drop
momentum and arc pressure. Even though the material surface tension does not vary with temperature, the radius of the liquid channel varies along its length, so there is a gradient of surface tension normal pressure along the length. As fluid moves in response to this gradient, the radius of the liquid channel also varies and interaction of momentum and surface normal pressure creates a fluid oscillation that drives the molten metal back and forth along the channel. This oscillation is also observed in the “normal” case for negative surface tension temperature coefficient shown Figure 3.4, but it is distinctly smaller than the oscillation seen in Figure 3.12. At about 1.9 s, the liquid channel becomes thinner and solidifies in both Figures 3.7 and 3.12. The pinched region that solidifies is somewhat thinner in Figure 3.7. Thus, relatively small differences noted in the final stages of the formation of the first hump for the positive and zero surface tension temperature gradient cases and the shape of humps are quite similar. It is concluded that Marangoni flow has relatively small influence on the hump shape in our simulations.
Figure 3.13 Actual final weld bead on plate using P-GMAW showing no hump formation at 12.7 mm/s weld travel speed in top (a) and side views (b).
3.4.4 COMPARISON TO A SMOOTH WELD MADE AT LOWER TRAVEL SPEED

Previous sections discussed temperature and fluid flow distributions associated with bead humping at high travel speed. In this section, comparison is made to experimental (Fig. 3.13) and simulation results (Fig. 3.14) for non-humped welds made at
lower travel speed, but with all other welding parameters remaining the same. As shown in the temperature and fluid flow velocity distributions (Fig. 3.14), no solidified humps are observed at the slower travel speed. Significant differences are noted when these results are compared to humped weld simulation results shown previously, notably those in Figure 3.5. In that figure, the weld pool is observed to become elongated and longitudinal fluid flow transitions from subsurface flow to surface flow. These transitions are critical to hump formation since they lead to the solidification of a thin channel of fluid that separates the weld pool into two fluid regions. In the lower travel speed case, no thin liquid channel develops and the trailing of weld pool is thicker, with reinforcement height about 3 mm above the base metal surface. Thus, the spherical surface has radius of about 3.8 mm while the radius at the point where necking occurs in the thin liquid channel in Figure 3.2(a) is estimated to be 2.4 mm. As a result, the surface tension normal force acting on the weld made a slower travel speed is about 40% less than that acting on the thin channel. Also, the longitudinal component of the fluid flow is along the fusion line rather than on the melt surface, another factor that prevents the premature solidification needed to separate the weld pool into two fluid regions. Thus, hump formation is not observed in the slower travel speed case.
3.5 CONCLUSIONS

Based on the experiment and the simulation, two mechanisms responsible to the hump formation during the pulsed gas metal arc welding at the high travel speed were identified and demonstrated. The first requirement is for a thin liquid channel generated by elongation of the molten metal deposit. The high curvature associated with a narrow, thin channel causes a strong surface tension normal force and that prevents backfilling of metal from any accumulation at the back of the pool. The final requirement for hump formation is pinching and rapid solidification of the thin channel, dividing the molten pool into front and rear sections. An important factor that increases the size of humps is strong backward fluid flow induced by the arc pressure and the drop momentum redirected by the sloping edge of gouged region at the front of the weld pool. This flow results in an increased accumulation of molten metal at the trailing of weld pool.

Simulations also show that, at least in the case studied, Marangoni flow is not a significant factor in hump formation. Based on the analysis of temperatures and flows during hump formation, the formations of bulged and elongated hump shape are distinguished. Finally, the simulations of lower travel speed welds predicted no hump formation, in agreement with experiment. In the simulation at low travel speed, there is no thin liquid channel formation and thus no rapid solidification that results in hump formation.
3.6 REFERENCES

4.1 INTRODUCTION

In manufacturing, higher welding travel speed is desired for increased welding productivity. However, one factor that often limits maximum welding travel speed is bead hump formation. The term “humps” refers to the periodic undulations seen, for example, in the gas metal arc weld (GMAW) beads displayed in Figure 4.1(a). Because of its importance, weld bead humping has been investigated in the past to attempt to understand its physical mechanisms and some techniques to increase the travel speed limit for hump formation have been proposed. A recent thorough review of prior literature has been published [1] so, for brevity, only the most relevant work will be summarized here.
In the earliest experimental studies of weld bead humping in GMAW, Bradstreet [2] identified the humping defect as one of welding defects that occurred at high travel speeds. Based on weld images, capillary instability as well as weld metal flow and wetting were identified as factors in hump formation. Capillary instability has been recognized as a fundamental mechanism for bead hump formation. Lord Rayleigh’s [3] analysis showed that a liquid cylinder with radius $r$ having periodic axisymmetric perturbations of wavelength $\lambda$ whose magnitude exceeds a critical value $\frac{\lambda}{2\pi r} > 1$ are unstable and identified wavelengths having the greatest growth rate.

The capillary stability of molten materials deposited on solid substrates has been analyzed [4-6] by other prior researchers. Comparisons to experiments show that capillary instability often provides a reasonable explanation for the formation of humps in molten beads deposited on solid surfaces. From analysis of the variation of surface energy with changes in liquid deposit shape, it can be shown that the stability of a long molten bead on a flat substrate with parallel, immobile contact lines is determined by the contact angle. If the internally-measured contact angle is greater than $\pi/2$, the molten bead is unstable to perturbations with wavelength $\lambda$ greater than a critical value, which grow exponentially in size. The critical wavelength for instability decreases as wetting angle increases and becomes equal to the wavelength predicted by Rayleigh stability analysis in the limit where the deposit does not wet the substrate at all. Deposits with fixed contact lines and contact angles less than $\pi/2$ are stable. It is mentioned that heat transfer and non-isothermal wetting, for example as analyzed by Schiaffino and Sonin
[5], are important to wetting, spreading and final shape of molten material deposited on a cool substrate.

Other researchers [7-9] have noted that relatively rapid weld pool flows are observed from video images of GTAW and GMAW weld bead hump formation and have proposed explanations for humping in terms of these metal flows. The work of Nguyen et al. [9] thoroughly characterized the strong backward fluid flow observed during GMAW process. The momentum of transferred filler metal droplets was identified as a source of the observed weld pool metal flow. This contrasts with the findings of Mendez and Eagar, where the pressure and plasma jet flow at the anode of high current GTAW arcs was identified as the main source for backward flow and humping. However, it must be remembered that the conditions at a cold cathode material such as steel are not well known but are generally considered to differ dramatically than those at arc anode on the same material. In the cold cathode material, it is usually presumed that the cathode current is largely carried by electrons emitted from a number of small, highly mobile cathode spots [10, 11]. Current constriction at these spots would produce Lorentz pressure gradients away from the surface and the spots are also known to be a prolific source of vaporized cathode material with momentum directed away from the surface (normal pressure on the cathode surface would correspond to the vapor flow momentum). Thermocapillary (Marangoni) flow [12, 13] has also been proposed to play a role in bead hump formation, but others [6] have proposed that this flow is not as strong as that induced by other forces and could be expected to play relatively a minor role.
Lately, Cho and Farson analyzed numerical simulation results for pulsed gas metal arc (P-GMA) welds and listed three essential events that were always observed in hump formation. One was the formation of an elongated, narrow molten deposit, which is more prevalent at high welding travel speeds. Due to high curvature and a tendency for unstable contact angles, such narrow deposits have high surface tension forces and are subject to capillary instability. When pinching occurs, the resulting thin liquid channel is prone to solidify prematurely, dividing the molten weld pool into two sections and guaranteeing the formation of a hump of some magnitude. A strong backward fluid flow was noted as a factor in the size of humps, but was not analyzed in detail. In the simulation, backward flow was generated by redirection of the momentum of deposited filler metal on the sloping leading edge of the weld pool. Arc pressure may also play a role in generating backward flow momentum in the actual welding process, but its magnitude in P-GMAW has not been characterized.

Prior researchers have proposed and demonstrated a number of techniques and process modifications that suppress the humping defect. In the work of Nguyen et al [9], a downhill part angle, a leading (“push”) weld torch travel angle, and reactive shielding gases suppressed hump formation by the decrease of the backward momentum in the weld pool. Because arc pressure was dominant in their process, Mendez and Eagar [8] suggested techniques to reduce the maximum arc pressure and thereby suppress hump formation at high travel speeds. For example, the use of tandem torches would reduce the maximum arc pressure by splitting the current between two torches and the use of blunt
electrode tip for GTAW would also generate less pressure. Subsequent literature [14-16] has described these affects in more detail.

It was recently reported [1] that a hybrid laser beam welding + gas metal arc welding (LBW+GMAW) process could suppress weld bead hump formation. In the hybrid process, the defocused laser beam was directed a short distance in front of the GMA weld pool to control the weld bead shape by increasing the spreading and wetting of molten weld pool. According to parametric studies of the variation of laser power and size, sufficient laser power density was required to suppress weld bead humping. Based on comparison of the toe angels of humped and non-humped weld beads made by the hybrid process and GMAW processes, it was concluded that capillary instability was likely responsible for hump formation, but that other factors also played a role in determining the severity of bead humping.

In this article, humped bead-on-plate welds were numerically simulated (as illustrated in Fig. 4.1(b)) so that the physical mechanisms of hump formation could be studied. Also, the hybrid welding process experimentally demonstrated by Choi et al [1] was numerically simulated using a Flow3D CFD commercial code so that the simulation results could be used to understand the role of various process variables in bead hump formation.
Figure 4.1 Top views of two different humped weld beads made using pulsed gas metal arc welding process (P-GMAW) were displayed: A deep penetration weld on thick plate (6.35mm) with 211.7mm/s wire feed speed (WFS) at 25.4mm/s travel speed (a), A shallow penetration weld on the thin plate (2.657mm) with 254mm/s wire feed speed (WFS) at 42.3mm/s travel speed (b). The simulated weld for (b) is shown in (c).
4.2 EXPERIMENTAL APPARATUS AND PROCEDURE

In this section, the experimental apparatus and procedure are briefly explained. More details of the experiment and the results are discussed in the work of Farson et al [1]. Base material used for the experiments was the hot-rolled A-36 steel, the material properties of which are summarized in Table 2.1. The welding wire was ER70S-6 with diameter of 0.045 in. (1.1 mm) and a pulsed GMAW power source (Thermal Arc 500-P) was used to form bead-on-plate welds in the flat position.

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Table 4.1 P-GMAW current waveform and welding parameter for 6.35mm-thickness plate

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<td>Peak voltage (V)</td>
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</tr>
<tr>
<td>Pulse time (ms)</td>
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<td>Background voltage (V)</td>
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</tr>
<tr>
<td>Pulse frequency (Hz)</td>
<td>300</td>
<td>Peak power (W)</td>
<td>16990</td>
</tr>
<tr>
<td>Shielding gas/flow rate</td>
<td>Ar,CO₂ /40 CFH</td>
<td>Background power (W)</td>
<td>1328</td>
</tr>
</tbody>
</table>

Table 4.2 P-GMAW current waveform and welding parameter for 2.6mm-thickness plate
In the first phase of experimental work, process variables that produced humped welds in two plate thicknesses were determined. The welds were made on 0.1 in. (2.6 mm) steel plate at a fast weld travel speed of 42 mm/s (1.65 in/s). Other welding parameter and waveform settings for two cases are summarized in Tables 4.1 and 4.2. The suppression of hump formation by hybrid process was investigated. As shown in the sketch in Figure 4.2, the laser beam was directed on the base plate at a 60 deg. angle. The focus spot was located in front of the leading edge of the GMAW weld pool and the focal point elevation was adjusted to vary laser beam focus spot diameter. During hybrid welding experiments, the GMAW process settings displayed in Table 4.2 were used and the laser beam incident angle remained fixed. The laser focus spot size and power were varied to determine the effect on hump formation.

![Figure 4.2 Schematic of hybrid (P-GMAW + laser) process setup.](image)

Figure 4.2 Schematic of hybrid (P-GMAW + laser) process setup.
4.3 SIMULATION PARAMETERS

Details of the numerical simulation and simulation parameters for pulsed gas metal arc welding and experimental validations for the thick plate weld contained in chapters 2 and 3 are only summarized in this section. However, the experimental measurements and determination of simulation parameters for the arc in the thin plate welds and for the defocused laser beam used in the hybrid process simulations are described in detail below.

General information about the numerical simulation is summarized in the following. Volume of Fluid (VOF) simulation technique was utilized to track the free surface of weld pool deformed by buoyancy, Marangoni flow, arc pressure, Lorentz and other forces that were mathematically modeled and implemented in the numerical simulation. The computational domain was meshed with 0.25 mm cubical cells for accurate and efficient simulation results. These mesh parameters were particularly important for mesh size independence of the surface tension calculations and mesh size was selected based on accurate prediction of the spherical shape of transferring metal droplets. Time step size was limited by the surface tension convergence criteria to 2.3 x 10^{-5} s. The number of cells needed for a simulation depended on the size of computational domain which varied depending on weld time and travel speed. For example, the simulation of a 1s bead-on-plate weld at 42.3mm/s travel speed required a computational domain at least 50 mm long or a total of 102,400 cells. For such a domain, the wall clock computation time was 14 hours using a workstation with dual 3.4 GHz
Xeon processors and 2 GB of RAM. For a weld time of 2 s, the computation time increased to the four times because the computational domain doubled in size.

The total heat input to the workpiece was obtained by multiplying an average arc power by arc efficiency, taken as 0.62 from the literature [17]. Based on the peak and background arc powers shown in Table 4.1, the actual power input to the base material was further divided into a direct heat input and a power consumed for molten drop generation, the latter being subsequently deposited in the weld pool. As shown in the high-speed video image in Figure 4.3, the shape of the arc was skewed in the travel direction when a weld travel speed was high. Thus, the usual radial-symmetric Gaussian density function used in previous chapters was not appropriate for direct arc heat input and pressure distributions. A three-parameter multivariate Gaussian density function was used to represent direct heat input and pressure distributions. The function for the heat input distribution $q(x, y)$ is

$$q(x, y) = \frac{wQ}{2\pi \sigma_s \sigma_f} \exp \left( -\frac{x^2}{2\sigma_f^2} - \frac{y^2}{2\sigma_s^2} \right)$$

(4.1)

where $Q$ is the total direct arc heat input, $w$ is a weight function, $\sigma_s$ is Gaussian distribution parameter normal (y-axis direction) to the travel direction (x axis), $\sigma_f$ is the Gaussian distribution parameter for the front part of arc in the x-axis, and $\sigma_b$ is Gaussian

$$w = \frac{2\sigma}{\sigma_f + \sigma_b}, \quad \sigma = \begin{cases} \sigma_f & x > \text{arc center} \\ \sigma_b & x < \text{arc center} \end{cases}$$
distribution parameter for the back part of the arc. Also, to represent the pulsed power behavior, the variation of Gaussian heat distribution parameters with pulse current was calculated from an empirical equation [19]. For an arc length of 4 mm, this equation is

$$\sigma_b = 0.533I^{0.2941}$$

(4.2)

where $I$ is welding current (amperes). From measurements of the images of the arc shape as shown in Figure 4.3, the back arc radius was estimated to be 8 mm and the front arc radius was taken as 3.5 mm. To estimate Gaussian heat distribution parameters, the arc radius ratio between the front radius and the back radius was tuned to be 1 to 3 that was used to determine $\sigma_f$ and $\sigma_e$ based on $\sigma_b$ obtained from the equation (4.2).

Figure 4.3 The electric arc image captured at the high travel speed (25.4mm/s) presents the skewed shape. The back arc radius (8mm) is more than twice of the front arc radius (3.5mm).
For the arc pressure distribution of the skewed arc, total arc force was obtained from literature [20] and Gaussian pressure distribution parameter is treated as same as Gaussian heat distribution parameter obtained above. According to previous research [20, 21], the empirical equations of the total force is expressed as

\[ P = -0.04017 + 0.0002553 \cdot I (N) \]  

(4.3)

where \( P \) is the total force (N) and \( I \) is current (amps). In a view of likely differences in GMAW and GTAW arcs noted previously, comparisons to experiments are relied on to judge the appropriateness of these assumptions for direct arc heat input, arc pressure, and the other calculated distributions used in the simulations.

To simulate the defocused laser beam, a Gaussian density distribution function with radial symmetry was used to implement the surface heat flux to the substrate and an absorptance of 0.4 was used. The corresponding formula for the laser beam heat input distribution \( q(r) \) is [22]

\[ q(r) = \frac{Q}{\pi \sigma_i^2} \exp \left( -\frac{r^2}{\sigma_i^2} \right) \]  

(4.4)

where \( Q \) is the actual heat input from the laser beam to the substrate and \( \sigma_i \) is the Gaussian distribution parameter, which was varied in the experiments. The distribution
was projected onto plane to represent the 60 deg. incident angle of the laser beam used in
the experiments.

4.4 RESULTS AND DISCUSSIONS

4.4.1 COMPARISON OF TWO DIFFERENT WELDS USING FUNDAMENTAL
CALCULATIONS

The differences in these welding parameter sets used to produce the two different
humped welds shown in Figure 4.1 can be characterized their linear heat input and the
filler metal deposit area. The average power for the experimentally-measured P-GMAW
current and voltage waveforms were used to calculate the linear heat input as

\[ H_p = \frac{P_A}{S} \]  \hfill (4.5)

where \( H_p \) is the linear heat input (J/mm), \( P_A \) is the average instantaneous power (W),
and \( S \) is the weld travel speed (mm/s). The linear heat input calculated for the thick plate
weld, 324.134 J/mm, is 54.2% larger than one for the thin plate weld, 210.236 J/mm, and
their ratio is 0.65. The deeper penetration observed for the end of the thick plate weld in
Figure 4.1 is expected from these values. The formula for the calculation of filler metal
deposit area \( A \) (mm\(^2\)) is
\[ A = \frac{\pi D_w^2 \cdot WFS}{4S} \]  \hspace{1cm} (4.6)

where \( D_w \) is the diameter of welding wire (mm) and \( WFS \) is the wire feed speed (mm/s).

From the values in Tables 4.1 and 4.2, the deposit area was \( 8.551 \text{mm}^2 \) for the thick plate weld, which is 38.9\% larger than the value of \( 6.156 \text{ mm}^2 \) for the thin plate weld and their ratio is 0.72.

Figure 4.4 Longitudinal cross section images of temperature (K) profile for the simulated weld deposit during the first stage of hump formation.
4.4.2 COMPARISON OF TWO DIFFERENT BEAD HUMP FORMATIONS

Weld simulation results of the thin plate weld shown in Figure 4.1(c) are comparable with the experimental shapes shown in Figure 4.1(b). For instance, the height of the first hump for the simulation is 4mm and one for the experiment is 3.3mm, and the period between the first and second simulated humps is 30 mm and that distance was 24 mm in the experiment. Basically, the simulation predicts humps that have the same general shape as the experiments, but are somewhat larger and more widely spaced than the actual hump. Therefore, simulation predictions of the thin plate weld displayed in Figure 4.1(c) were further analyzed to compare with the thick plate weld simulation.

Figure 4.5 Top view images of temperature (K) profile for the simulated P-GMAW weld deposit during the first stage of hump formation.
In the discussion from chapter 3, requirements were identified for formation of humps in thick plate bead on plate welds. The thin plate welds in this chapter had no undercutting along the edges of the weld bead in the depression between humps and a deep gouged region at the end of weld bead was not observed. Even though the deep gouging and undercutting are not present during the formation of humps in the thin plate weld, three stages for formation of the first hump in thick plate welds are also reasonable to explain hump formation of thin plate welds. The first two stages are the elongation of molten weld deposit and capillary pinching of the thin molten bead, and the last stage is the rapid solidification at the thin liquid channel to form the humped bead. The elongation of the deposit is fundamentally related to the fact that the welding travel speed was significant relative to the rate of thermal diffusion in the material. For representative values shown in Table 3, $Pe \approx 35$ when using travel speed as the characteristic velocity and the longitudinal dimension of the Gaussian heat input as the characteristic length. The formation of a long weld pool is also related to the fact that the heat capacity of the steel is significant relative to the heat losses through the liquid surface and conduction into the weldment. These affects are discussed further below.

The first stage of hump formation is displayed in Figures 4.4 and 4.5. During the first stage, the accumulation of molten metal at the trailing of weld pool produced by the backward fluid flow due to arc pressure and the re-directed droplet momentum is observed in Figure 4.4. During this stage, the accumulated molten deposit is quickly arrested in the back of weld pool due to the relatively cold base material there, so the backward momentum is transferring in the lateral direction. The small Weber number for
this case, $We \sim 0.06$, indicates that normal surface tension pressure at the back of molten metal deposit was significant compared to the backward fluid flow inertia. Thus, the backward flow was constrained by surface tension and the back of the weld pool was maintained in a spherical shape as shown in Figure 4.5.

The elongation of the weld pool due to fast arc travel starts around 0.3 s. The transition to the second stage is earlier for the thin plate weld than for comparable thick plate weld simulations demonstrated in chapter 3. This difference can be explained by three factors; the higher travel speed, the narrower weld pool and smaller variation of molten bead width at the beginning of the weld. The latter produces relatively uniform normal surface tension along the molten metal deposit that allows more recirculation of molten metal to maintain the cross section of the molten bead.
During the second stage of hump formation, molten weld metal oscillates back and forth in the trailing of weld pool. The backward fluid flow induced by arc pressure and deflected droplets is not strongly affecting on the accumulated molten deposit in the second stage because the pinching that is the key event in this stage, occurring at the back of the longer weld pool. Thus, the pinching of the thin liquid channel produces backward fluid flow during the second stage. At the same time, the forward flow generated by the hydrostatic pressure of the large bulge and the normal surface tension force acting on the back of the large hump is pushing molten metal forward. A more uniform molten bead is generated if the recirculation flow forces are strong enough to resist backward flow from
pinching. Also, it is notable that experience with the simulation has shown that if pinching occurs, there is also always oscillation of molten metal deposit. But it is also true that even if weld bead pinches are temporarily removed by fluid oscillations, they were always observed to eventually re-form and generate a bead hump of some magnitude.

Figure 4.7 Longitudinal and transverse cross sectional views of temperature (K) profile demonstrate the instability of molten bead due to capillary instability.

Figure 4.7 Longitudinal and transverse cross sectional views of temperature (K) profile demonstrate the instability of molten bead due to capillary instability.

Capillary instability along the liquid channel was observed to be a key event in the formation of the hump and so bears further consideration. Prior investigations have established that the internally measured contact angle determines the stability of molten
deposit bead when the contact line is immobile. As shown in Figure 4.7, a molten bead with contact angle greater than $\pi/2$ is unstable to perturbations longer than a critical wavelength. It is noted that all of thin molten beads where pinching was observed were relatively long and had lengths greater than the critical length. In the prior work by Gratzke et al. [6], it was noted that humping occurred when the weld pool width/length ratio was less than about 0.1. The width/length ratio of simulated weld pool results at the occurrence of pinching was approximately 0.05. Also, it is noted that the simulations depict conditions that are more general than assumed in prior analyses, which needed simplifying assumptions for closed form mathematical solutions. Parallel contact lines are assumed, which was not true for the welding simulations presented here. Also, flow produced by interactions between capillary and other pressures played a role in the simulations. It is expected that simulations of the type shown here will allow additional insights into capillary stability of liquid deposits under more general conditions.
Figure 4.8 Transverse cross sectional views of x direction velocity (m/s) profile at the same pinched location.

In the thin plate weld as shown in Figure 4.6(a), the first pinching occurs at 0.8 s weld time but pinching pressure in the neck was not strong enough to prevent the recirculation of molten metal from the trailing of weld pool. Transverse cross sectional views of x direction velocity (m/s) profile at the pinched location in Figure 4.8 indicates that magnitude of forward flow at the weld center increased until 0.9 s, producing a fairly uniform weld bead at that weld time. In Figure 4.7(a), the contact angle of molten bead at the previous pinched position was increased from 53 deg. (stable) to 115 deg. (unstable)
by the recirculation. This geometry was once again susceptible to capillary instability, so the pinching occurred again at 1.0 s as seen in Figure 4.6(a). After the occurrence of this second pinching, the swelling of molten metal at the back of the weld was maintained because the narrow bead and strong pinching force prevented a backfilling. This is seen the transverse cross section showing x-axis velocity contour at the pinched bead location at 1.0 s (Fig. 4.8(a)) where there is no positive direction velocity fluid flow observed and thus no backfilling from the back of the weld pool. This is the indication of permanent pinching that induces the hump formation during the last stage of hump formation.

During the last stage of hump formation, molten metal at the thin liquid channel connecting the front and the rear weld pools is quickly solidified due to a small thermal mass. Figure 4.9 shows the complete solidification at the pinched molten bead to form the first hump at 1.15s. It is interesting to note that durations of each phase of humping in the thin plate weld were shorter than those for the thicker plate weld described in chapter 3. This difference may have been due to the fact that large linear heat input and high weld metal deposition rate per unit distance for the thick plate weld generated the large thermal mass that required the longer cooling time for the complete solidification of the pinched molten metal. The ratio of times of the first hump formation between two thicknesses of welds is 0.59 which closely matches the ratio of 0.65 for the calculated linear heat inputs of two cases. Based on this simple calculation, the large linear heat input in the weld could be more resistant to hump formation.
The results predicted by the simulation can be explained by the relative magnitudes of dimensionless quantities associated with the heat and mass transfer in the weld pool. The significance of thermal energy lost from the surface of the long, narrow weld pool to the heat capacity of the liquid contained in it can be assessed by the Stanton number $St$. For representative values, the small Stanton number, $St \sim 1 \times 10^{-4}$, shows that surface heat loss from the weld pool was not significant in comparison to its heat capacity. The Nusselt number, $Nu \sim 0.09$, indicates that conduction was somewhat more significant than surface heat loss. However, the value of the Grätz number, $Gr \sim 0.06$, also indicates that heat capacity was still important relative to heat conduction from the weld pool. The value of Stefan number $St \sim 0.15$ indicates the heat loss by radiation is comparable to heat conduction. In general, the oscillatory nature of the fluid flow in the narrow weld pool indicates that inertial and surface tension forces were comparable and that viscous losses were not high. Indeed, the Froude number value, $Fr \sim 0.6$, suggests
comparable inertial and gravitational affects. The Bond number value, \( Bo \sim 0.4 \), shows comparability of capillary force relative to gravitation and also to inertia, considering the value of \( Fr \). The value of Capillary number, \( Ca \sim 3 \times 10^{-4} \), confirms that viscosity of the molten weld metal had minor effects compared to its surface tension. The Reynolds number value \( Re \sim 390 \) indicates that the flow is laminar and that viscosity is not negligible for the flow calculations. The value of Marangoni number, \( Ma \sim 1 \times 10^5 \), Magnetic Reynolds number, \( Rm \sim 2 \times 10^6 \), and the Grashof number, \( Gr \sim 782 \) indicate that Marangoni, Lorentz, and buoyancy forces are both important relative to viscosity for inducing weld pool flow. Also, Marangoni and Lorentz forces are more significant to buoyancy force due to the relatively larger values of \( Rm \) and \( Ma \) compared to \( Gr \). In general, it can be concluded from the above analysis that all of the effects that were assessed must be considered for accurate prediction of weld pool heat and fluid flow and weld bead shape.

<table>
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<th>Group, symbol</th>
<th>Definition</th>
<th>GMAW</th>
<th>Hybrid</th>
</tr>
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<tr>
<td>Péclet travel, ( Pe )</td>
<td>( SD / \alpha )</td>
<td>35</td>
<td>35</td>
</tr>
<tr>
<td>Péclet flow, ( Pe )</td>
<td>( VD / \alpha )</td>
<td>120</td>
<td>170</td>
</tr>
<tr>
<td>Weber, ( We )</td>
<td>( \frac{\rho L V^2}{\sigma} )</td>
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<td>0.08</td>
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<tr>
<td>Stanton, ( St )</td>
<td>( \frac{h}{\rho c_{p} V} )</td>
<td>( 1 \times 10^{-4} )</td>
<td>( 1 \times 10^{-4} )</td>
</tr>
<tr>
<td>Nusselt, ( Nu )</td>
<td>( \frac{hL}{k} )</td>
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<td>0.17</td>
</tr>
<tr>
<td>Grätz, ( Gr )</td>
<td>( \frac{(L/D)}{Pe} )</td>
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<td>0.07</td>
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Table 4.3. Magnitudes of dimensionless groups GMAW and characteristic values used in calculations.

continued
Table 4.3 continued

<table>
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<th>Parameter</th>
<th>Formula</th>
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<th>Value 2</th>
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<tr>
<td>Stefan, St</td>
<td>$\frac{\sigma R T^3_i}{k}$</td>
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<td></td>
</tr>
<tr>
<td>Froude, Fr</td>
<td>$\frac{V}{\sqrt{gD}}$</td>
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<td>0.5</td>
</tr>
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<td>Bond, Bo</td>
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</tr>
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<td>Capillary, Ca</td>
<td>$\frac{\mu V}{\sigma}$</td>
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<td>$3 \times 10^{-4}$</td>
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<tr>
<td>Reynolds, Re</td>
<td>$\frac{L V}{\mu}$</td>
<td>390</td>
<td>520</td>
</tr>
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<td>Marangoni, Ma</td>
<td>$\rho \left(\frac{D}{2}\right)^3 \left(\frac{\partial \gamma}{\partial T}\right) \Delta T$</td>
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<td>$1.3 \times 10^5$</td>
</tr>
<tr>
<td>Magnetic Reynolds, Rm</td>
<td>$\frac{\rho \mu m I^2}{4\pi^2 \mu^2}$</td>
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<td>Grashof, Gr</td>
<td>$g \left(\frac{D}{2}\right)^3 CTE \Delta T$</td>
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</tr>
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<td>Weld pool width (m)</td>
<td>$D$</td>
<td>$3 \times 10^{-3}$</td>
<td>$4 \times 10^{-3}$</td>
</tr>
<tr>
<td>Weld pool length (m)</td>
<td>$L$</td>
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<td>$4.6 \times 10^{-2}$</td>
</tr>
<tr>
<td>Welding travel speed (m/s)</td>
<td>$S$</td>
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<td>$4.2 \times 10^{-2}$</td>
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<tr>
<td>Surface tension gradient (N/mK)</td>
<td>$\frac{\partial \gamma}{\partial t}$</td>
<td>$-3 \times 10^{-4}$</td>
<td></td>
</tr>
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Figure 4.10 This figure shows laser melt track produced by a defocused 3.3 kW laser beam at a scanning speed of 42.3 mm/s. The width of the laser width is 4 mm.

Figure 4.11 Transverse cross sectional views of laser skin melt simulation at the middle of the computational domain for 0.2 s, 0.25 s, and 0.3 s scanning times.
4.4.3 SIMULATION OF SKIN MELT BY DEFOCUSED LASER BEAM

Simulation of the effects of the defocused laser beam was required to complete a hybrid process simulation. The heat input by laser beam was simulated as a Gaussian density function (Eq. 4.4). To determine the proper Gaussian beam radius, simulation results were generated for 3.3 kW laser power with radius varying from 1 mm to 4 mm and the resulting predicted melt widths were compared to experiment. Figure 4.11 shows a typical result for predicted laser melt temperature contour at 0.5 s and cross sectional views at a fixed location for the defocused laser beam using 2.2mm Gaussian radius. Simulation predictions of the laser melt half-width are plotted in terms of Gaussian radius in Figure 4.12. As the radius was increased, the maximum laser intensity at the center of the beam decreased and the melt half-width also began to decrease after beam radius increased above about 2 mm. Based on the laser melt analysis, a simulated Gaussian laser beam radius of 2.2 mm predicted a melt half-width of 2 mm that matched the experimental melt width of 4 mm so it was selected for subsequent simulations. Also, the solidified pattern of simulated skin melt shown in Figure 4.11 is in a good correspondence with the experimental results of Figure 4.10. The sequential cross sectional images of simulated laser melt in Figure 4.11 and the experimental image in Figure 4.10 both show accumulation of fluid along the outer edges of the laser-melted region. Analysis of predicted flow patterns verified that the pattern was due to Marangoni stress on the melt surface.
Figure 4.12 Skin melt half-width versus laser beam Gaussian radius.

Figure 4.13. The perspective views at 1.9s for humped weld on the thin plate and non-humped weld made by the simulated hybrid process (Pt=3.3kW, radius=2.2mm).
4.4.4 SUPPRESSION OF HUMP FORMATION BY HYBRID PROCESS

In Figure 4.13, simulation results for P-GMAW alone and the hybrid process demonstrate the effectiveness of the hybrid process to suppress hump formation. There is also good correspondence between dimensions of the simulated hybrid weld shown in Figure 4.14(b) and the actual hybrid weld bead in Figure 4.14(a). The reinforcement height and width at the beginning of the simulated weld bead were 3.5 mm and 4.5 mm while the experimental values were 2.7 mm and 5.5 mm. In the middle of the simulated weld, the bead height and the width were 2.5 mm and 4 mm and the experimental values were 1.8 mm and 5 mm.

Figure 4.14 Top views of (a) the actual hybrid weld and (b) the simulated hybrid weld are displayed.
Figure 4.15 Sequential cross sectional view images of temperature (K) profile for the simulated weld of hybrid process.

Figure 4.16 Sequential top view images of temperature (K) profile for the simulated weld of hybrid process.
At the initial weld time of simulated hybrid process as shown in Figures 4.15(a) and 4.16, molten metal droplets impinged on a flat surface and quickly spreading to the back and front of weld pool because the preheating of base material in the back (the laser was impinged on the plate before the arc heat input was initiated) and the presence of laser skin melt in the front. In contrast, such longitudinal spreading of molten metal was not observed in the simulations of P-GMAW welds shown in Figure 4.4 and this led to the accumulation of material at the beginning of the weld that produced a bulged hump at later times.

After 0.3s weld time, when the momentum source moved away from the accumulated weld deposit, forward flow of molten metal produced a more uniform weld bead shape in the hybrid process and suppressing hump formation. The magnitude of recirculation was strongly influenced by the surface tension pressure on the initial accumulation of melt. The elongated weld deposit shape at the back of the hybrid weld pool had a stronger capillary pressure than the rounded shape because the effective radius was smaller. From analysis of flows at 0.3s weld time, the backward fluid flow was still effective to push molten metal in the trailing of weld deposit, but the recirculation induced by the backward momentum quickly leveled the molten deposit at the back of the weld pool, producing the more uniform weld pool seen in sequential images after 0.4s weld time. Also, the variation of molten bead width as shown in Figure 4.16 was small, so the normal surface tension was also uniform along the liquid channel, also tending to stabilize the hybrid bead in comparison to the P-GMAW bead, which had a large variation in width and thus a large variation in surface tension normal force along the
weld, promoting instability. As time increased, the molten weld bead shape of the hybrid weld was maintained in a quasi steady state as shown in Figure 4.15(b).

For the deposit area used in the simulations, the minimum molten bead width required for the capillary stability was 3.96 mm based on assumptions of a hemispherical cross section and contact angles of $\pi/2$. This is in good correspondence with the stability of the 4mm molten bead width in these simulations. Because of relatively small contact angles, the molten bead produced by the simulated hybrid process was inherently stable as graphically illustrated in Figure 4.17(a). In Figure 4.17(b), the transverse cross section views illustrate x direction velocity magnitude and direction at a fixed position in the middle of the weld pool at 1.2 s. For the humped weld shown in Figure 4.8, there was no recirculation (no positive x velocity component) observed at the end of hump formation. In contrast, for the non-humped hybrid weld, exchange of molten metal in both directions was continuous and balanced until complete solidification, so the molten bead shape was maintained. Consequently, it may be concluded that the maintenance of a sufficiently uniform weld bead with large enough width that is stable from a capillary viewpoint are essential to suppress hump formation.
Figure 4.17 Transverse cross sectional views of temperature contours (a) and x direction velocity (m/s) profile (b) at the middle part of weld pool at 1.2 s weld time.
Comparing the dimensions and flow velocities for the weld pools shown in Figures 4.14 to those in Figure 4.8, the primary differences were larger molten bead width (4 mm vs. 3 mm) and length (40 mm vs. 23.5 mm). Values of the various nondimensional groups for the hybrid welds, shown in Table 4.3, were comparable to those for the mentioned earlier GMAW in most cases. The fact that hump formation was very different for the two cases is confirmation of the approximate nature of dimensional analysis and its limits for detailed process analysis and optimization.

Figure 4.18 Perspective views of two simulated hybrid welds using 1mm (a) and 3mm (b) Gaussian distribution parameters.

Figure 4.18 Perspective views of two simulated hybrid welds using 1mm (a) and 3mm (b) Gaussian distribution parameters.
4.4.5 PARAMETRIC STUDY OF HYBRID PROCESS

In the previous section, the ability to suppress hump formation by hybrid process with a laser beam focus of 3.3 kW and 2.2mm Gaussian distribution parameter was demonstrated. In this section, the laser beam focus size and laser power were varied to determine their effect on hump suppression. For the study of laser beam size, simulations of the hybrid process with 1mm and 3mm laser focus radii were conducted, both with 3.3kW laser power. The perspective view of simulation results as shown in Figure 4.18 demonstrated that hybrid process using 1mm and 3mm Gaussian distribution parameters did not prevent hump formation. More details of each case are described in the following.

Figure 4.19 Sequential cross sectional view images of temperature (K) profile for the simulated weld of hybrid process with 1mm Gaussian distribution parameter.

(a) Initial hump formation

(b) The end of first hump formation
In the simulated hybrid process using 1mm Gaussian distribution parameter, the defocused laser beam applied in front of GMA weld pool increased the thermal energy intensity at the centerline of weld pool, but the laser beam size was not wide enough to provide a sufficient skin melt width. Therefore, bead width was only 3.5 mm as it was susceptible to hump formation by capillary instability. It is noted that, for the deposit area used these tests, a bead width of 3.96 mm was critical because at this width, a calculated contact angle was exactly $\pi/2$. Figure 19(a) shows longitudinal cross sectional views of a hybrid process weld bead that is very similar to the previous one in Figure 4.15. However, the top view images of weld pool shape in Figure 4.20 show that the weld bead width less than the critical width and there was also a relatively large variation of molten bead width, which also causes capillary pressure differences along the liquid channel that also promote hump formation. Consequently, the pinched molten bead occurred at 1.0 s.
weld time as shown in the cross sectional view in Figure 4.19(b). At the thin pinched cross section, the rapid solidification occurred from the lateral direction due to the small thermal mass and the first hump was formed at 1.3s weld time by the disconnection of the front and the rear parts of the weld pool.

Figure 4.21 Sequential cross sectional view images of velocity (m/s) profile in the x direction for the simulated weld of hybrid process using 1mm Gaussian distribution parameter for the defocused laser beam (a) and the thick plate weld at the initial weld time (b).

Figure 4.21 Sequential cross sectional view images of velocity (m/s) profile in the x direction for the simulated weld of hybrid process using 1mm Gaussian distribution parameter for the defocused laser beam (a) and the thick plate weld at the initial weld time (b).
The velocity profiles at the initial weld time displayed in Figure 4.21(a) provide the fluid flow pattern for the further analysis of the first hump formation. In spite of the hybrid process, hump formation was not prevented by the additional laser beam because of the insufficient beam size that constrained the skin melt width in front of GMA weld pool. The hybrid humped weld images in Figure 4.18(a) compared to the humped P-GMAW welds in Figure 4.13(a) show that the hump shape for the hybrid humped weld is more similar to the bulged hump rather than the elongated first hump in the P-GMAW weld. The difference in hump shape can be explained by the fluid flow patterns. From the simulated hybrid process velocity profiles in Figure 4.21(a), the depressed weld pool due to the concentrated laser beam provided a slope at the front weld pool to deflect the drop momentum into the back of weld pool, enhancing the backward fluid flow as demonstrated in Figure 4.21(a). This strong backward fluid flow produces the counter clockwise circulation that is identically same as the fluid flow pattern observed in the thick plate weld hump as shown in Figure 4.21(b). This fluid flow pattern induced a bulged hump as discussed in chapter 3.

As mentioned before, molten bead simulated by hybrid process using 1mm Gaussian distribution parameter of the defocused laser beam was subject to capillary instability, but after the first hump formation, the long thin liquid channel was stable until 1.6s weld time because the first hump drew such a large amount of molten metal from the front weld pool that the molten bead was uniform. As time increased, the length of molten bead became shorter because the back of molten bead was quickly solidified due to the lack of thermal mass. Thus, the molten bead grew more unstable as more weld
metal was deposited and, as shown in Figure 4.18(a), humps later formed due to capillary instability. However, the subsequent humps were small in size compared to the first hump because not as much metal was accumulated by the backward fluid flow during the formation of these humps.

A perspective view of a simulated hybrid process weld bead using 3mm Gaussian beam radius at 1.5 s weld time is shown in Figure 4.18(b). It reveals that a minor hump formed at the beginning of weld bead, but no subsequent humps were formed. The molten bead should be stable from a capillary viewpoint because the weld bead width was 4mm, larger than the critical bead width of 3.96 mm. As expected, no subsequent humps were formed.

Figure 4.22 Sequential top view images of temperature (K) profile for the simulated weld of hybrid process using 3mm Gaussian distribution parameter for the defocused laser beam.
Figure 4.23 Sequential cross sectional view images of temperature (K) profile for the simulated weld of hybrid process using 3mm Gaussian distribution parameter for the defocused laser beam.

Even though the molten bead was basically stable, the formation of the first hump can be explained by the large variation in weld bead width as shown in Figure 4.22. The difference of the molten bead width was about the same as the variation for the P-GMAW case, which was also large enough to cause hump formation. This case illustrates that the unbalanced normal surface tension force induced by bead width variation is large enough to cause the pinched molten bead. In this particular case, the pinch split the weld pool into the front and the rear parts which were maintained until the complete solidification, causing the first hump as shown in Figure 4.23. However, the defocused
laser beam maintained 4mm molten bead width after the first hump formation, so no
subsequent humps were formed.

Figure 4.24 Perspective views at 1.5s for humped weld made by hybrid process using
3.3kW laser power (a) and non-humped weld made by hybrid process using 5.0kW laser
power (b).

To investigate the effect of laser power on hump formation, the focus radius was
maintained at 3mm and the laser power was increased from 3.3kW to 5kW. The
perspective view of simulated welds for 3.3kW and 5kW, shown in Figure 4.24,
demonstrate that the hump formation was suppressed by the increase of laser power, in
spite of the wide focus that permitted humping at the lower power. In the simulated weld with 3.3kW laser power, the pinched weld pool was observed, but no pinch was seen for the 5 kW case. Also, due to the increased laser power, the weld bead width increased from 4 mm (in the 3.3kW case) to a more stable value of 4.5 mm. Even though there was still significant variation of molten bead width at the weld start, no prematurely-solidified pinched region was observed and no hump was formed. Also, the bead height variation that is the quantitative value to determine hump formation is significantly decreased from 2.5mm (3.3kW laser power case) to 0.75mm (5kW laser power case) at the initial weld bead that indicates the more stable weld bead formation due to the increase of laser power.

In the analysis of hybrid process by varying Gaussian distribution parameter and laser power, the major requirements of humping prevention at the high speed weld on the thin plate are the wider weld bead width that should be larger than the critical value in order to lower the contact angle below the critical value ($\pi/2$) and the uniform weld bead width that produces the consistent normal surface tension force along the liquid channel even though molten bead width is close to the critical bead width. Therefore, the appropriate laser beam intensity and its proper beam size are required to provide the sufficient skin melt size that produces the appropriate molten metal deposit shape for the suppression of hump formation. It is interesting to compare the simulated laser spot intensities that prevented hump formation to experimental results. In a recent paper for comparable experimental conditions published by the authors [1], at least a laser focus spot intensity of 14 kW/cm$^2$ was needed for hump suppression. The nominal 3.3 kW
power and 2.2 mm radius that prevented hump formation in the simulations corresponds to an intensity of 13 kW/cm². The wider 3 mm radius spot that did not completely prevent humping had an intensity of 5.8 kW/cm² and the intensity of the 5 kW, 3mm radius spot that provided the complete humping suppression was 9 kW/cm². The intensity of the 3.3 kW, 1 mm radius spot that produced a large first hump and small subsequent humps was 52 kW/cm². Thus, the laser beam intensity between 6 and 9 kW/cm² is the transition value to suppress hump formation (the less bead height variation) according to the simulated hybrid process with different laser intensities and power that was comparable to experimental results.

4.5 CONCLUSIONS

P-GMAW and laser+P-GMAW hybrid welding processes were numerically simulated using a Flow3D CFD code and the results studied to understand the mechanisms of weld bead hump formation and its suppression. The events that occurred during hump formation in the thin-plate welds were seen to be formation of a thin elongated molten bead that divided the molten pool into two sections and which was susceptible to capillary instability. After surface tension pinching of this thin bead, rapid solidification of the small amount of remaining molten material divided the larger mass of melt into two sections, guaranteeing the preservation of a humped shape. The simulation was used to demonstrate and study suppression of hump formation by a hybrid
process, where a defocused laser beam melted material in front of the weld pool.

Conditions for formation of a hump at the beginning of a weld bead and formation of subsequent humps were different, but the events leading to hump formation were the same for both. The laser beam intensity and spot size applied in front of the GMA weld pool had to be sufficient to provide a bead width large enough to prevent capillary instability. A critical bead width could be estimated from a simple calculation based on capillary stability of the molten deposit. Beads having greater widths had smaller initial humps and no subsequent humps.

4.6 REFERENCES

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CHAPTER 5

CONCLUSIONS

The numerical simulation of the arc welding process offers many capabilities to understand the physical phenomena associated with arc welding for process development, such as process modifications to suppress welding defects. In this dissertation, the pulsed gas metal arc welding process was numerically simulated using a code based on the Volume of Fluid (VOF) technique, which was chosen primarily for its ability to accurately calculate the shape and motion of free fluid surfaces. Buoyancy, Marangoni, arc pressure, drag, and Lorentz forces were mathematically modeled and implemented in the numerical simulation. For the accurate simulation of the arc welding process associated with the mathematical models, effects associated with the arc were modeled as boundary and body pressures and necessary parameters were obtained from the analysis of high-speed video images and arc current and voltage. The stationary P-GMAW welding was simulated and then the predictions were validated by comparison with measured weld deposit geometries, transient molten deposit radii and temperature histories from thermocouple measurements. The weld simulation was generally found to
be quite accurate but predicted slightly deeper weld penetration than those measured experimentally, a difference that was attributed to consistent droplet impact location in the simulation versus random droplet impact in the experiments.

Simulation tests with individual changes of variables (Gaussian heat distribution, Gaussian current distribution, total force, and Gaussian pressure distribution) provided insight into the effects of these variables on fluid flow patterns and weld penetration. The large heat distribution with a fixed total heat input decreased the weld penetration, the constricted current density distribution drastically increased the weld penetration but decreased the weld radius, and the reduced total force and the large arc pressure distribution radius decreased the weld penetration. Overall, the parametric study accomplished using the weld simulation demonstrated the effects of physical variables on weld pool fluid flow and provided key insights for further application of the model as a tool to assist in weld process development. The numerical simulation method used for the stationary pulsed gas metal arc welding process provides the fundamental basis for the simulations of humped welds at the high travel speed and hump suppression by the hybrid (P-GMAW + laser) process.

Pulsed gas metal arc welding (P-GMAW) at high travel speed on thick plate was simulated to study the formation of humped beads based on temperature distributions and fluid flows. According to the analysis of the experimental and simulation results of humped welds, two mechanisms responsible for the hump formation during the pulsed gas metal arc welding at the high travel speed were identified and demonstrated. The first requirement is a thin liquid channel generated by elongation of the molten metal deposit.
The high curvature associated with a narrow and thin channel causes a strong surface tension normal force and that prevents backfilling of metal from any accumulation at the back of the weld pool. The final requirement for hump formation is pinching and rapid solidification of the thin channel, dividing the molten pool into front and rear sections.

An important factor that increases the size of humps is strong backward fluid flow induced by the arc pressure and the drop momentum redirected by the sloping edge of gouged region at the front of the weld pool. This flow results in an increased accumulation of molten metal at the trailing of weld pool. The simulation results clarified the fluid flow associated with two different hump shapes; the bulged hump is produced due to the strong subsurface flow and the elongated hump is formed by the surface flow.

According to the analysis of simulations conducted by the various surface tension (e.g. constant surface tension and one third of surface tension), surface tension force plays an important role for hump formation, but flow induced by surface tension gradients (Marangoni flow) is not significant for hump formation. Experimental welds without bead humping were made at a lower travel speed and also simulated. In the simulation at low travel speed, there is no thin liquid channel formation and thus no rapid solidification that results in the hump prevention.

The 3D numerical simulation of the pulsed gas metal arc welding on the thin plate at the high travel speed demonstrated that the mechanism of hump formation is governed by basically the same two mechanisms as the thick-plate case. In this chapter, the hump formation associated with capillary instability is described in more detail. According to cross sectional images of the simulated thin plate humped welds, pinching occurs more
than once, but the shape of the pinched weld pool is not restrained after every pinching because of the oscillation of weld pool induced by the recirculation and capillary instability of molten bead. However, the pinching at the neck is stronger for the thin plate welds because the pinched molten bead is thinner. As before, hump formation occurs after complete solidification at the thin pinched liquid channel.

Simulation results for the hybrid laser process demonstrated the ability to suppress hump formation and facilitated understanding of the suppression mechanism. A shallow “skin” melt in front of GMA weld pool produced by the defocused laser beam promoted a wider weld bead with a smaller contact angle that was less susceptible to capillary instability. Also, the width of the molten bead varied less along the travel direction with laser pre-heating, which also made it more stable. In agreement with experiment, at 3 kW power, a defocused laser beam Gaussian radius of about 2 mm provided the maximum skin melt width and maximum hump suppression capability. Based on the parametric study of laser beam in the hybrid process simulation, hump formation was not prevented using either 1mm or 3mm Gaussian distribution parameters at 3kW beam power due to the insufficient skin melt area produced by the defocused laser beam. However, at higher power, humping was suppressed by the wider molten bead produced by a larger Gaussian laser focus spot radius.

Some unresolved problems remain for the future work. For example, the cathode spot region of the welding arc is not possible to simulate due to the lack of physical understanding. The modeling of plasma flow-induced normal pressure on the pool surface as a Gaussian distribution is a dramatic simplification of the actual situation. The
shear stress on the melt pool from plasma flow was modeled by an axisymmetric analytical solution in the stationary weld case but this approach was not applicable in the traveling weld case. Also, the mathematical model at the interface associated with three phases (solid, liquid, and gas) at the contact line where the molten pool wets the adjacent solid is oversimplified in the current code. Sufficiently small simulation mesh size allowed predictions that generally agreed with experiments, but a better algorithm would improve the accuracy of numerical simulation. Also, there are more problems associated with the physical understandings and the mathematical modeling of the physical phenomena. Therefore, the study of numerical simulation during the arc welding process will be continued for the better understandings of its physical phenomena.