Modeling Friction Stir Welding Process of Aluminum Alloys

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The friction stir welding (FSW) process of aluminum alloys has been modeled using a two-dimensional Eulerian formulation. Velocity field and temperature distribution are strongly coupled and solved together using a standard finite element scheme. A scalar state variable for hardening is also integrated using a streamline integration method along streamlines. A viscoplastic constitutive equation to consider plastic flow and strength variations was implemented for the process modeling. Precipitates inside AA6061 alloys are sensitive to elevated temperatures and affect strength evolution with temperature. The overall effects of the precipitate variations with temperature on strength were reflected using temperature-dependent material parameters. The material parameters of constitutive equations were obtained from isothermal compression tests of various temperatures and strain rates. The effects of FSW process conditions on heating and hardening were investigated mainly near the tool pin. The microhardness distribution of the weld zone was compared with the prediction of strength. In addition, crystallographic texture evolutions were also predicted and compared with the experimental results.

**Keywords:** friction stir welding, finite element, aluminum alloys, hardness, crystallographic texture

1. INTRODUCTION

Friction stir welding (FSW) is a solid-state joining technique effective for joining metallic and nonmetallic materials or materials which are difficult for fusion welding [1]. Its advantages include fewer defects, no filler materials, no shrinkage, and no toxic fumes. The process is also easily applied to milling machines and lowers set-up and training costs. Both the tool pin and shoulder jointly create a complicated material flow pattern.

Various modeling approaches have been reported to help understand the thermal and kinematic behaviors of the FSW process. Combined two boundary value problems (BVP) were considered for the heat transfer of the FSW process. A steady state BVP was applied for the tool, and a transient BVP for the workpiece. To quantify the physical values of the process, temperatures in the workpiece and the tool were measured during FSW [2]. A combined experimental and predicted analysis showed that around 95% of the total heat generation during FSW flows into the workpiece and that the fraction of the rate of the plastic work dissipated as heat is about 80%. Colegrove et al. [3] investigated the material flow and temperature around the threaded tool pin. To overcome excessive heat generation, a viscosity relationship was used to include material softening. An analytical thermal model was presented to allow simulation of the asymmetric heat field under the tool shoulder due to viscous and interfacial friction dissipation in [4]. Another analytical model based on Coulomb’s law was also proposed in [5]. A contact variable was used to define the contact condition of stick, sliding, and partial stick/sliding in this model. A coupled thermo-mechanical FE model using the arbitrary Lagrangian Eulerian (ALE) of ABAQUS/explicit code was presented, and void formation was addressed in [6]. The model suggested that the development of the sticking contact condition at the tool pin and matrix interface is important for the success of the deposition process and prevention of the void during FSW.

Most of the previous studies focused on temperature and velocity distributions in the model domains of interest. In addition to thermal and kinematic analyses, strength evolution was carried out by Cho et al. [7-9]. Two- and three-dimensional models of the FSW process using a viscoplastic constitutive model for stainless steel have been discussed. Although modeling FSW processes requires three-dimensional formulations to better understand the complicated material flow, a two-dimensional approach has been still useful to validate material models of interest and to capture features of the process.

A constitutive equation with strength evolution for AA6061
alloys is presented in this paper. Age hardening materials, such as AA6xxx, contain various precipitates and their strength evolutions are closely related to variations of the precipitate size, density, and shape (or microstructural evolutions). Heating by shearing can vary with position during FSW, and the precipitates can dissolve or coarsen in the regions with elevated temperatures. In that case, strength changes with temperature significantly. The measured hardness of stainless steel usually showed increased values after FSW because of strain hardening [10,11]. In previous experiments, aluminum alloys, however, displayed more variable hardness distributions with welded position than stainless steel [12-16]. The hardness usually dropped in the weld zone of AA6061. To reflect the temperature dependence of strength, previous modeling work on stainless steel was modified for AA6061 alloys.

The strength in the viscoplastic model for AA6061 alloys can decrease or increase with welding conditions. Material parameters have been calculated from the reported experimental data for aluminum 6061 alloys. Various data from isothermal compression tests, which cover a temperature range of 298 K to 773 K and a strain rate of 0.001 s$^{-1}$ to 20 s$^{-1}$, were used. The effects of variational precipitates with temperature on strength were indirectly reflected by temperature-dependent material parameters. The computed strength values were compared to the measured hardness. Texture evolution during FSW was also discussed in this paper. Shear deformation was dominant in both experiments and predictions.

2. NUMERICAL MODELING OF THE FORMING PROCESS

2.1. Balance law

The motion and temperature of the workpieces are governed by balance laws for mass, momentum, and energy [17] and are given as follows:

\begin{align}
\text{conservation of mass} & \quad \frac{\partial \rho}{\partial t} + \rho \nabla \cdot \mathbf{u} = 0 \tag{1} \\
\text{balance of linear momentum} & \quad \nabla \cdot \mathbf{\sigma} + \mathbf{b} = 0 \tag{2} \\
\text{conservation of energy} & \quad \rho \frac{\partial e}{\partial t} + \nabla \cdot \mathbf{q} - \dot{Q} = 0 \tag{3}
\end{align}

where $\rho$ is the density, $\mathbf{u}$ is the velocity, $\mathbf{\sigma}$ is the Cauchy stress, $\mathbf{b}$ is the body force, $e$ is the internal energy, $\mathbf{q}$ is the heat flux, and $\dot{Q}$ is the volumetric heat source. Inertia has been neglected, and the heat generation includes a contribution from viscous dissipation. Boundary conditions are specified for the motion and energy of the workpiece. For the motion, either known tractions or velocities are imposed on the surface:

\begin{align}
\mathbf{\sigma} \cdot \eta = \mathbf{T} \quad & \text{on } S_T \tag{4} \\
\mathbf{u} = \mathbf{u}_0 \quad & \text{on } S_u \tag{5}
\end{align}

where $\eta$ is the surface normal vector, $\mathbf{T}$ is a known traction vector, and $\mathbf{u}_0$ is a known velocity vector. $S_T$ and $S_u$ are portions of the total surface, $S$. In the case of sliding friction over a portion of the boundary, the tangential component of the surface traction may be due to the frictional contact with the forming equipment, such as a tool pin or die. In this case the traction vector is written as,

\[ T_t = \beta(u_{tool} - u), \tag{6} \]

where $\beta$ is a coefficient that can depend on the temperature and traction, especially its normal component, and the subscript, $t$, indicates the tangential component.

2.2. Thermal and mechanical responses

The thermal and plastic behaviors of materials are characterized implementing constitutive equations. The internal energy and heat flux for thermal behaviors are written as functions of the rate of deformation, temperature, and state variables of the materials. For thermal behavior, internal energy, $e$, and heat flux, $\mathbf{q}$, are assumed to represent the material approximately,

\[ \frac{D}{Dt}(e) = c_p \frac{D}{Dt}(\theta) \tag{7} \]

and

\[ \mathbf{q} = -k \nabla \theta \tag{8} \]

The specific heat, $c_p$, and conductivity, $k$, are often specified as functions of temperature.

For viscoplastic behavior, the constitutive equations are also required to describe the plastic flow of the metal workpiece. The state variable model originally proposed by Hart [18] was applied to solid-state welding process [19,20]. The modified isotropic model for the flow stress and strength was successfully applied to stainless steel in [7]. The flow stress of the constitutive equations depends on the temperature, the rate of deformation, and state variables. The flow stress varies with the difference between the hardening and recovery, which are related to the accumulation and annihilation of crystal defects (i.e. dislocation), respectively. The simplified Hart’s model incorporates two contributions to the flow stress: one called a plastic contribution and the other a viscous contribution.

\[ \mathbf{\sigma} = \mathbf{\tau} = \mathbf{\tau}^p + \mathbf{\tau}^v \tag{9} \]

where $\mathbf{\sigma}$ is the effective Cauchy stress and $\mathbf{\tau}^p$ and $\mathbf{\tau}^v$ are the
plastic and viscous contributions to the rate and temperature dependent flow stress, $\tau$. These contributions are written as

\[
\tau' = G \left( \frac{D}{\alpha} \right)^{\frac{1}{2}}
\]

(10)

\[
a = a_0 \exp \left( \frac{Q'}{R_0} \right)
\]

(11)

and

\[
\tau'' = \kappa \exp \left[ -\left( \frac{b}{D} \right)^{\lambda} \right]
\]

(12)

\[
b = b_0 \exp \left( \frac{Q}{R_0} \right)
\]

(13)

where $D$ is the effective value of the deformation rate, $D$, and $k$ is the strength. The viscous term is small compared to the plastic part. This term, however, plays an important role in total stress at relatively low temperatures or very high deformation rates. The material model parameters, $G$, $Q$, $Q'$, $M$, $N$, $\lambda$, $b_0$, and $a_0$ for AA6061 have been determined using compression test data [21] and are listed in Table 1.

The rate of the strength change in the Hart model is approximately proportional to the deformation rate at low temperatures or at a high deformation rate. The FSW process is expected to have the highest deformation rate and heating around the tool pin. A limited saturation value for the strength was successfully applied to these severe deformation conditions in the FSW of stainless steel. A Voce-like saturation limit for the strength was introduced:

\[
\kappa_{sat} = \left( \frac{C}{\varphi} \right)^{m_0}
\]

(14)

The saturation limit for the strength varies with both temperature and deformation rate via the Fisher factor. The Fisher factor [22], $\varphi$, is given:

\[
\varphi = \theta \ln \left( \frac{D}{D_0} \right)
\]

(15)

Table 1. Material parameters for the simplified Hart’s model for AA6061

<table>
<thead>
<tr>
<th>$a_0$ [s$^{-1}$]</th>
<th>$b_0$ [s$^{-1}$]</th>
<th>$G$ [GPa]</th>
<th>$\kappa_0$ [MPa]</th>
<th>$Q$ [kcal/mole]</th>
</tr>
</thead>
</table>
| $1.30 \times 10^{
+}$ | $1.03 \times 10^{-1}$ | 70.0 | 100.0 | 37.5 |

<table>
<thead>
<tr>
<th>$Q'$ [kcal/mole]</th>
<th>$\lambda$</th>
<th>$M$</th>
<th>$N$</th>
</tr>
</thead>
<tbody>
<tr>
<td>37.5</td>
<td>0.15</td>
<td>7.8</td>
<td>5.0</td>
</tr>
</tbody>
</table>

Table 2. Material parameters for the strength evolution of AA6061

<table>
<thead>
<tr>
<th>$b_0$ [GPa]</th>
<th>$n_0$</th>
<th>$D_0$ [s$^{-1}$]</th>
<th>$\kappa_0$ [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>5</td>
<td>9</td>
<td>10$\times 10^7$</td>
<td>50</td>
</tr>
</tbody>
</table>

Note: $\theta$ in the table represents temperature. $m_0$ and $C$ are temperature-dependent and are assumed to remain constant below 298 K.

Fig. 1. Linear regression between the Fisher factor and the saturated stress estimation at given temperatures of (a) 473 K, (b) 573 K, (c) 673 K, and (d) 773 K.
In addition to saturation limit of the strength, the temperature-dependent material parameters of $C$ and $m_0(\theta)$ in Eq. 14 were introduced to reflect the strength distribution sensitive to temperature. The evolution is then written as

$$\frac{D}{Dt}(\kappa) = h_0\left(1 - \frac{\kappa}{\kappa_{\text{sat}}}\right)^n D$$  \hspace{1cm} (16)$$

where $h_0$ and $m_0$ are experimentally-determined model parameters.

Table 2 lists the material parameters of AA6061 for strength evolution. Figure 1 shows the estimated saturation stress levels for AA6061 as a function of the Fisher factor during compression at various temperatures [21]. Aluminum alloys usually have higher specific heat capacities and heat conduction coefficients than stainless steel. The specific heat capacity and heat conductivity of aluminum are about twice and ten times higher than those of stainless steel, respectively. Thus, temperature distributions were different from those of stainless steel during the thermo-mechanical processes.

The relationship between the Fisher factor and strength was considered under the conditions of various deformation rates at a constant temperature. At the given temperature, $\ln(1/\phi)$ and $\ln\kappa$ have a linear relationship according to the deformation rate approximately. Using linear regression, $m_0$ and $C$ at each temperature were decided. The distributions of $m_0$ and $C$ with temperature were fitted by polynomial functions (quadratic forms) in Fig. 2. The quadratic distributions of $m_0$ and $C$ values were used to determine the saturated strength and thus to predict the flow stress. Below 298K, $m_0$ and $C$ were assumed to be constant.

Figure 3 depicts the flow stress of AA6061. The flow stress was computed using a modified form of Hart's model, which includes the Voce-like hardening evolution and quadratic assumption of $m_0$ and $C$ distributions with temperature. As discussed before, severe shearing and heat generation occurred around the tool pin, so a limitation of flow stress under such circumstances was made by the saturated strength evolution. In addition, temperature-dependent parameters

Fig. 2. Quadratic fitting of $m_0$ and $C$ for the Fisher factor according to various temperatures. (a) $C$ and (b) $m_0$.

Fig. 3. Flow stress calculated from a modified strength model according to various deformation rates at given temperatures of (a) 373 K, (b) 573 K, and (c) 773 K.
were introduced. At 373 K, the saturated flow stress of AA6061 was in the range of 250 MPa to 350 MPa according to various deformation rates. The saturated stress values at an elevated temperature of 773 K decreased, and the flow stress varied from 20 MPa to 50 MPa. The saturation values of the computed stress matched those of the flow stress obtained from experiments [21]. Figure 4 shows variations of specific heat and conductivity coefficients with temperatures ranging from 296 K to 755 K.

3. RESULTS AND DISCUSSION

The two-dimensional modeling of FSW process was carried out with the model equations provided in the previous sections. The detailed numerical solving procedures are found in [7]. The overall temperature and strength distribution during FSW were understood based on a two-dimensional formulation.

The model geometry of FSW is presented in Fig. 5. The two plates with 50 mm wide and 100 mm long are modeled. The radius of the tool pin is 10 mm. Two different angular velocities or rotational speeds were modeled: Case 1 for $\Omega = 10$[rad/s] and Case 2, 20[rad/s]. Another Eulerian domain consisting of two plates 25 mm wide and 50 mm long (Case 3) was used for a comparison with the experimental results. In the second model, the radius of the tool pin is 5 mm, and the rotational speed is 60[rad/s].

Figure 6 shows material flow along streamlines at various welding speeds. A higher rotational speed of $\Omega = 20$ rad/s causes tighter and more wavy streamlines around the tool pin, while a slower rotational speed generates smoother streamlines surrounding the pin. At the same rotational speed, a lower welding speed shows tighter and narrower streamlines around the pin.

During FSW, a rotating tool pin is inserted into the workpieces and shatters them near it. Temperature increases due to heat generated by shearing between the tool and workpieces. Plastic work increases the strength of the welded materials through strain hardening. Increased temperature through plastic work lowers the strength via recovery of the deformed material, conversely. The softening effects of temperature were particularly accelerated by the change of hardening precipitates. To investigate the variations of temperature and strength during FSW, various welding conditions are presented in Table 3.

3.1. Predictions of temperature and strength

In this section, temperature and strength distributions are addressed under the different welding conditions of AA6061. The temperature distribution during FSW was obtained by solving energy Eq. 3. About 90% of the plastic work is assumed to be converted into heat. The maximum temperature calculated is limited to 90% of the melting temperature of aluminum, 933.47 K.

Figures 7 and 8 show the results of a rotational speed of $\Omega = 10$[rad/s] (Case 1). Welding speeds vary from 1 mm/s to 7 mm/s. The predicted temperature shows a peak tempera-
ture of approximately 700 K. Usually, the shearing deformation is severer on the advancing side than on the retreating side. This results in a higher temperature in the same direction. The asymmetric temperature distributions were also predicted in modeling of FSW for stainless steel [7]. Both heat conduction and convection terms contribute to heat transfer. As the welding speed increases from 1 mm/s to 7 mm/s, the higher temperature regions are reduced because the welding speed determines the overall material flow. Heat convection effects closely related to the velocity of material flow become stronger with weld speed. Heat transfer to the outlet part is mainly the result of convection. Conduction contributes to heat transfer to both sides. The slower welding speed of 1 mm/s has the higher temperature field in Fig. 7.

The difference of thermal conduction and heat capacity between aluminum and stainless steel results in different profiles of temperature. In particular, heat conduction of aluminum is about 10 times greater than that of stainless steel. Assuming that welding speeds are the same, the heat conduction effect is more dominant in aluminum than stainless steel. The high thermal conduction coefficients of aluminum make high temperature regions wide in the model domain, while the temperature profiles of stainless steel have shown a narrow comet-like tail along the welding zone [7]. Strength predictions during FSW also display asymmetric distributions in Fig. 8. Strength evolution is a function of the plastic deformation rate, temperature, and strength itself. An initial strength of 100 MPa was given for AA6061 at the inlet. The strength of the welded region has lower values than that of

**Table 3. Boundary conditions of two-dimensional modeling for AA6061**

<table>
<thead>
<tr>
<th>Condition</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$T_0$ [K]</td>
<td>298</td>
</tr>
<tr>
<td>$T_\infty$ [K]</td>
<td>300</td>
</tr>
<tr>
<td>$T_m$ [K]</td>
<td>933.47</td>
</tr>
<tr>
<td>$\kappa_0$ [MPa]</td>
<td>100.0</td>
</tr>
<tr>
<td>$h$ [W/(m$^2$K)]</td>
<td>50 $\times 10^{-10}$</td>
</tr>
<tr>
<td>$\beta_{\text{toolpin}}$ [N/s/m$^3$]</td>
<td>10, 20, 60</td>
</tr>
<tr>
<td>$\Omega$ [rad/s]</td>
<td>1, 2, 3, 7</td>
</tr>
<tr>
<td>$u_x$ [mm/s]</td>
<td></td>
</tr>
</tbody>
</table>

Note: $T_0$ is the initial temperature, $T_\infty$ is tool temperature, $T_m$ is melting temperature of Aluminum, $\kappa_0$ is the initial strength, $h$ is a heat coefficient, $\beta$ is a frictional coefficient. $\Omega$ is the angular velocity of tool. Case1 (Figures 7 and 8) for $\Omega = 10$ rad/s, Case2 (Figures 9 and 10) for $\Omega = 20$ rad/s, and Case3 (Figure 11b) for $\Omega = 60$ rad/s.
Fig. 7. Case 1 $\Omega = 10$ rad/s: temperature distributions according to welding speeds of (a) $v = 1$ mm/s, (b) $v = 2$ mm/s, (c) $v = 3$ mm/s, and (d) $v = 7$ mm/s.

Fig. 8. Case 1 $\Omega = 10$ rad/s: strength distributions according to welding speeds of (a) $v = 1$ mm/s, (b) $v = 2$ mm/s, (c) $v = 3$ mm/s, and (d) $v = 7$ mm/s.
base materials in the case of a welding speed of 1 mm/s. Considering that the welding speed of 1 mm/s has the highest temperature, it seems that temperature plays an important role in the strength decrease. Other cases with higher welding speeds (2 mm/s, 3 mm/s, and 7 mm/s) have increased strength in the weld zone. Heat seems to be quickly convected to the outlet with material flow.

As the rotational speed increases from 10 rad/s to 20 rad/s, more heating is expected. In Fig. 9, temperature distributions reach the maximum temperature (about 840 K) calculated. The lower the welding speed, the less the heat convection. The welding speed of 1 mm/s has wider high temperature regions than others. Asymmetric temperature distributions found in Fig. 7 are also found in the Fig. 9.

A higher rotational speed of 20 rad/s also varies the strength distribution in Fig. 10. The highest temperature region or welded zone decreases strength overall. The advancing sides have lower strength than the retreating sides. In the welding speed of 7 mm/s, the weld zone is divided into two parts, the higher strength part on the retreating side and the lower strength on the advancing side. The complex distribution reflects the complicated compromise between both effects of the deformation rate and temperature. As discussed, the trends of strength distributions of AA6061 are different from those of stainless steel [7]. The predicted and experimental strengths of stainless steel showed that the welded region had elevated strength distributions. The region behind the tool pin of the advancing side has higher strength than other regions. The region with higher strength was also consistent with the region with higher temperature. Considering the elevated strength, the hardening effect due to strain hardening (or plastic deformation) appeared to be dominant over the softening effect due to the temperature of stainless steel. In AA6061, however, the region behind the tool pin of the advancing side has high temperature and low strength, simultaneously. The main strengthening mechanism of AA6061 is precipitates hardening. The strength distributions can be affected by precipitate changes with temperature. The hardening precipitates can dissolve or coarsen, and the strength significantly decreases as compared to the base material [16, 24]. The temperature reached in the weld zone may be high enough to dissolve the hardening precipitates.

3.2. Experimental hardness

The workpieces to be welded were Al6061-T6 plates with thicknesses of 4 mm. The high thermal conduction coefficients of aluminum make high temperature regions wide in the model domain, while the temperature profiles of stainless steel have shown a narrow comet-like tail along the welding zone [7]. The tool is made of SKD-11 tool steel, and the

Fig. 9. Case 2 $\Omega = 20$ rad/s: temperature distributions according to welding speeds of (a) $v = 1$ mm/s, (b) $v = 2$ mm/s, (c) $v = 3$ mm/s, and (d) $v = 7$ mm/s.
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The diameters of the tool pin and shoulder are 4.3 mm and 13 mm, respectively. The welding and rotational speeds are 100 mm/min (about 1.67 mm/s) and 2000 rpm (about 209 rad/s). The Vickers hardness profiles for AA6061 after FSW are presented in Fig. 11(a). The hardness values were measured across the advancing side, weld zone, and retreating side in the mid-plane of the plate, where the tool pin effect is more dominant than the tool shoulder. The hardness in the base zone is approximately 100 HV. Approaching the weld zone, the hardness values decrease. The hardness values diminish about 60 HV between the thermo-mechanically affected zone (TMAZ) and heat treated zone (HAZ). The hardness bounces back to 80 HV and has a plateau past the region. A significant decrease in hardness distributions during FSW of AA6061 is associated with the change in precipitate distributions. The predicted strength value can be understood as a measure of the dislocation density and can be compared with measured hardness. In Figs. 8 and 10, the predicted strength distributions were already discussed. The strength distributions depend on the welding conditions and have a tendency to decrease in the weld zone. Figure 11(b) shows the strength distribution predicted using a smaller tool pin (Case3: \( \pi_v = 2 \) mm/s and \( \Omega = 60 \) rad/s). Around the region where the tool pin was passing, the strength greatly decreased and its overall distribution matched that of the experimental hardness. The initial strength for the model was 100 MPa, and it diminished to about 60 MPa to 90 MPa. Two local minimums in the measured hardness distribution were found between TMAZ and HAZ or HAZ and TMAZ. They are located both on the advancing and retreating sides. In particular, the minimum along the advancing side has a lower value than the other. The minimum in the prediction was also found between TMAZ and HAZ, which is located on the advancing side.

The purpose of this article illustrates that constitutive equations with temperature-dependent material parameters can improve the prediction of strength evolution with temperature. There are still some limitations to reflect effects of the precipitates using the constitutive equations proposed here. It also should be noted that the traction imparted by the pin is the product of the friction coefficient and the rotational speed of the tool. In this respect, the rotational speed is somewhat arbitrary in the simulations, as its total effect in the simulations is to establish the traction. In other words, the rotational speed could be higher if the friction coefficient was made correspondingly smaller. The combination of values was determined by comparison to experimentally measured temperatures.

3.3. Crystallographic textures

The simulations presented here decouple the computations for the velocity and temperature fields from the integration

Fig. 10. Case 2 \( \Omega = 20 \) rad/s: strength distributions according to welding speeds of (a) \( v = 1 \) mm/s, (b) \( v = 2 \) mm/s, (c) \( v = 3 \) mm/s, and (d) \( v = 7 \) mm/s.
of the texture evolution. The approach has been applied previously to FSW [7,8]. After computing the velocity and temperature, thermomechanical histories along streamlines were derived. They were used for the integration of the texture evolution equations along streamlines. The plastic anisotropy present in the yield surface due to texture is assumed to have little effect on the overall flow field. The two-dimensional model provided an effective tool for examining the dynamics of the texture evolution. Some combinations of the symmetric and skew parts of the velocity gradient were found to induce constantly changing texture patterns, while others promote monotonic texturing. Major texture components during two-dimensional modeling of FSW were simple and pure shear components. In this study, we compared textures predicted and measured from AA6061.

Figure 12 shows streamlines and their corresponding textures computed from FSW predictions (Case 2). The streamlines (Fig. 12(a)) are traveling from the inlet to the outlet boundaries. Nine streamlines were selected, and each position is enumerated in the figure. The streamlines display material points moving in the domain during FSW. Based on crystal plasticity [8], the evolution of initial homogeneous texture was predicted along the streamlines. Texture evolutions were computed using velocity gradients of the streamlines. Streamlines of 1, 2, and 9 display homogeneous textures because they are located away from the tool pin. Streamline 3 displays the longest streamline surrounding the whole tool pin. From streamline 3 to streamline 6, two types of fiber exist: one is the mixed fiber with simple and pure shear textures, and the other is the drawing type fiber frequently found in fcc wires. The pole figure for streamline 7 in Fig. 12(b) has a weakly-developed and six-fold fiber. The pole figure for streamline 8 shows the typical pure shear textures that occur during plane strain deformation.

Figure 13 shows experimental pole figures measured from
the mid-plane of the plate, where the tool pin effect is more dominant than the tool shoulder. As pointed out before, two-dimensional predictions focus on the effect of the tool pin only, and this region is suitable for the comparison between predictions and experiments. Six-fold fibers created by shear deformation were measured. The experimental pole figures changed with position, and it seems that some rotational and tilting operations were made from position to position. A complicated texture evolution during FSW resulted from the complex material motion induced by the three-dimensional tool pin and shoulder. Three-dimensional approaches using AA6061 model have yet to be investigated for thorough predictions.

4. CONCLUSIONS

Two-dimensional modeling and simulation during FSW of AA6061 were carried out. Using a constitutive equation including strength evolutions, the predicted strength and experimental hardness were compared. Features of crystallographic textures were also presented.

(1) Asymmetric distributions of temperature and strength occurred because of the differing shearing rates between the advancing and the retreating sides. Higher temperatures are found on the advancing side than on the retreating side. Lower strength values are also predicted in the advancing side.

(2) Increases in the rotational speed affect both the heating and plastic deformation. Increased rotational speeds contribute to more softening in the welded zone than strengthening. A strength decrease was predicted in the weld zone of AA6061 alloys.

(3) AA6061 with hardening precipitates shows a significant drop in strength with temperature, and this is associated with precipitate changes by heating during FSW. Softening effect by heating appears to be dominant over strengthening by deformation.

(4) This article illustrates that a constitutive equation with temperature-dependent material parameters can clearly reflect the strength behaviors sensitive to temperature.

(5) Crystallographic textures were predicted along streamlines and compared with the experimental results. Overall pure and simple shear texture components due to severe shearing deformation were found in both the predictions and experiments.

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