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Large-scale finite element analysis of arc-welding processes

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Abstract
Three-dimensional finite element analysis of arc-welding processes is presented with emphasis on practical applications for numerical simulation. We use an implicit numerical implementation for Leblond’s transformation plasticity constitutive equations, which are widely used in steel-structure welding. Several numerical examples, particularly including a large structure undergoing significant elastic–plastic deformations before welding, are presented to demonstrate the effectiveness of the three-dimensional analysis of welding processes.

1. Introduction
Recently numerical simulations of industrial manufacturing processes have attracted a significant amount of attention due to the availability of increasingly strong computing powers. In particular, welding, which involves thermal, mechanical and metallurgical processes together, is a very complex phenomenon, and various methods for efficient and accurate numerical simulation have been developed (see [1] for a recent review in this area). Among others, Leblond et al [2,3] reported a systematic formulation for phase evolution, heat transfer and mechanical process involving deformations. For the first time, they were successful in linking phase transformation to plastic deformations, which is called ‘transformation plasticity’. Their formulation for welding was initially implemented into a code, SYSWELD, which is the only commercial code that is capable of analysing the effects of phase transformation and transformation plasticity. More recently, Kim and Kim [4] reported a fully linearized tangent-stiffness for the constitutive equation describing the transformation plasticity. In addition, they utilized the so-called hyperelastic stress update, which is more efficient than the conventional hypoplastic approach. Their consistent modulus ensures a quadratic convergence of solution, and their approach makes simulation extremely efficient for welding processes, suggesting a possibility of making it tractable to analyse realistic welding structures.

Despites the decoupling between the thermo-metallurgical process and the mechanical process, finite element analysis of welding is extremely time-consuming owing to the complexity of the governing equations involved. Therefore, it has been restricted to small-scale problems, and simulations of problems with 100 000 degrees of freedom have not been realized. To the authors’ knowledge, no simulation examples involving large mechanical deformations in addition to welding process have been particularly reported. The purpose of the present paper is to provide a large-scale finite element analysis of welding processes based on the implementation of Kim and Kim [4] for transformation plasticity.

The outline of this paper is as follows. First, we review the hyperelastic thermo-metallurgical and structural analysis in section 2. Next, several numerical examples are tested in section 3, including a large-scale problem. Finally, some concluding remarks are given.

2. Formulation of welding problems for thermo-metallurgical and mechanical analysis
We assume that plastic flow is isochoric in stress response. We use hyperelastic stress–strain relations proposed by Simo and Hughes [5]. The model is based on multiplicative decomposition of deformation gradient as the standard additive decomposition loses its physical content in the nonlinear theory. The deformation gradient is decomposed as \( F = F^e F^p \), where \( F^e \) represents the elastic deformation and \( F^p \) the plastic
The elastic stored energy function without a temperature change is defined as

\[ W = U(J) + W\left(\mathbf{b}^e\right), \]  

(1)

\[ U(J) = 0.5 \mu J^2 - \ln J, \]  

(2)

\[ W\left(\mathbf{b}^e\right) = 0.5 \mu \left[ \text{tr}\left[\mathbf{b}^e\right] - 3\right], \]  

(3)

\[ \mathbf{b}^e = J^{-2/3} \mathbf{F}^{-1} \mathbf{F}^e, \]  

(4)

where \( \mu, \kappa \) are the shear and the bulk modulus, respectively, and we call \( U(J) \) and \( W\left(\mathbf{b}^e\right) \) the volumetric and deviatoric parts of \( W \), respectively. \( J \) is the determinant of deformation gradient. The store-energy function results in uncoupled volumetric-deviatoric stress–strain relationships. The relationships are derived as

\[ \mathbf{\tau} = J \cdot dU(J)/dJ \mathbf{I} + s, \]  

(5)

\[ s = \text{dev}[\mathbf{\tau}] = \mu \text{dev}[\mathbf{b}^e], \]  

(6)

where \( \mathbf{\tau} \) is the Kirchhoff the stress tensor and \( s \) is the stress deviator. \( \mathbf{F}^e \) can be decomposed as \( \mathbf{F}^e = \mathbf{F}^{me} \mathbf{F}^{te} \), where \( \mathbf{F}^{me} \) represents the mechanical elastic deformation caused by a change of stress and \( \mathbf{F}^{te} \) represents the thermoelastic deformation caused by a change in temperature. The isotropic thermal expansion law can be expressed by \( \mathbf{F}^{th} = f_{\text{th}} \mathbf{I} \), where \( \theta \) denotes temperature. For generalized small strain expansion law, we choose \( f_{\text{th}} = 1 + \alpha(\theta - \theta_0) \) where \( \alpha \) is the coefficient of isotropic thermal expansion. To account for temperature dependence, we modify \( J \) as

\[ J_{\text{new}} = (f_{\text{th}})^3 J, \]  

(7)

\[ \mathbf{b}^e_{\text{new}} = (f_{\text{th}})^{-2} \mathbf{b}^e. \]

Next we use Leblond’s flow rule for the isotropic hardening case. This model categorizes metallurgical phases of steel into two groups. Phase 1 is the weaker phase austenite and phase 2 is the harder phase, the sum of the other phases including ferrite, pearlite and martensite. Yield stress is defined as

\[ \sigma^{\text{eff}}(\dot{\varepsilon}_1^{\text{th}}, \dot{\varepsilon}_2^{\text{th}}, \theta) = [1 - f(z)]\sigma_1^{\text{eff}}(\dot{\varepsilon}_1^{\text{th}}) + f(z)\sigma_2^{\text{eff}}(\dot{\varepsilon}_2^{\text{th}}), \]  

(8)

where \( f(z) \) indicates a function of non-linear mixture rule, \( z \) the proportion of phase 2 and \( \dot{\varepsilon}_1^{\text{th}} \) and \( \dot{\varepsilon}_2^{\text{th}} \) are hardening variables of phases 1 and 2, respectively. The flow rule and the evolution of hardening according to Leblond’s transformation plasticity are given as follows [6, 7] for transformation plasticity (\( \dot{\sigma} < \sigma^{\text{th}} \)):

\[ \dot{\varepsilon}^P = \dot{\varepsilon}^P + \dot{\varepsilon}^P, \]  

(9a)

\[ \dot{\varepsilon}^P = (-3)3 \Delta \varepsilon^\text{th}_{1=2} \left(\sigma_1^{\text{eff}}(\dot{\varepsilon}_1^{\text{th}})^{-1} \cdot h(\dot{\sigma}/\sigma) \right) \cdot s (\ln z)/z, \]  

(9b)

\[ \dot{\varepsilon}^{\text{th}} = 3(1 - z) \cdot \left(2\sigma_1^{\text{eff}}(\dot{\varepsilon}_1^{\text{th}})^{-1} \cdot (g(z)/E) \cdot \sigma \right) \]  

\[ + 3(\alpha_1 - \alpha_2) \cdot \left(\sigma_2^{\text{eff}}(\dot{\varepsilon}_2^{\text{th}})^{-1} \cdot (\ln z) s / \theta, \right), \]  

(9c)

\[ \dot{\varepsilon}_1^{\text{th}} = -2 \Delta \varepsilon^\text{th}_{1=2} \cdot (1 - z)^{-1} \cdot h(\dot{\sigma}/\sigma) \cdot (\ln z)/z \]  

\[ + (g(z)/E) \cdot \dot{\sigma} + 2(\alpha_1 - \alpha_2) \cdot z \ln z \cdot (1 - z)^{-1} \cdot \dot{\theta,} \]  

(9d)

\[ \dot{\varepsilon}_2^{\text{th}} = - (\dot{\varepsilon}_1^{\text{th}}) \cdot \sigma^\text{eff}_{1/2} + (\dot{\sigma}/\sigma) \cdot \dot{\varepsilon}_1^{\text{th}} \]  

(9e)

\[ \dot{\sigma} = \sqrt{1.5s} \cdot s, \]  

(9f)
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Figure 3. The stress history at point A of flat plate welding problem; (a) S11(perpendicular to the welding line) and (b) Mises stress.

Figure 4. The stress history along line L1(welding line) of flat plate welding problem: (a) S11(perpendicular to the welding line) and (b) Mises stress.

Figure 5. The stress history along line L2(perpendicular line to the welding line) of flat plate welding problem: (a) S11(perpendicular to the welding line) and (b) Mises stress.

where $\alpha_1$, $\alpha_2$ are the thermal expansion coefficient of phases 1 and 2, respectively, $\Delta \varepsilon_{1\rightarrow 2}^{th}$ is the difference in thermal strain between the two phases. In addition, $h$ is the correction function for experimental results, $\omega$ the factor for memory of history and $g(z)$ the correction function with respect to experimental results. The relation is so complex that it seems to be difficult to integrate the internal state variables.

First we focus how to relate the flow rule to the $J_2$ theory associative flow with Mises yield condition. Equations (9(a)–9(c)) are equivalent to the general $J_2$
assessing flow rule with Von-Mises yield condition, \( i^p = \gamma n \), where \( \gamma \) is loading index. Simo showed that the flow for the Mises yield condition and the elastic potential takes the form of

\[
L_i \dot{\varepsilon}^e = -2/3 \cdot \gamma \cdot \text{tr}(\dot{b}^e) n, \quad (10)
\]

where \( L_i \) denotes the Lie derivative and \( n \) is the normal vector of Kirchhoff stress. We relate \( \gamma \) to other state variables used in Leblond’s transformation plasticity. For the transformation plastic-flow case, the equations of flow rule and hardening law are rewritten as

\[
2/3 \cdot \gamma = (1 - z) \cdot (\sigma_i^e(\varepsilon^e))^{-1} (\varepsilon^e_1)^{-1} \varepsilon^e_1, \quad (11a)
\]

\[
\varepsilon^e_1 = -2\Delta x_1^{ch} \cdot (1 - z)^{-1} (\ln z) \cdot \text{d}h(\|\varepsilon^e\|, \varepsilon^e_1, \varepsilon^e_2) + \sqrt{\varepsilon^e_1 \cdot \varepsilon^e_2}, \quad (11b)
\]

\[
\varepsilon^e_2 = - (\dot{z}/z) \varepsilon^e_2 + \alpha (\dot{z}/z) \varepsilon^e_2. \quad (11c)
\]

We use radial return mapping to update stress and strain and use a Euler backward scheme to integrate the other state variables for consistency with the return mapping. For transformation plasticity case, we can derive the integration procedure. Integration of (11a) as can be expressed [2] as

\[
\Delta \varepsilon = K(\Delta \varepsilon_{\text{eff}}, \dot{\mu}) \|s_{\text{trial}}\|^2, \quad (12)
\]

where \( K(\Delta \varepsilon_{\text{eff}}, \dot{\mu}) = (1 - \alpha z_{as})(\Delta \varepsilon_{\text{eff}} - 2\mu(1 - \alpha z_{as}) + 1.5\sigma_1(\Delta \varepsilon_{\text{eff}})^{-1} \). Integration of (10b) and (10c) can be written as

\[
\Delta \varepsilon = -2\Delta x_1^{ch}(1 - z_{as})^{-1} (\ln z_{as}) \cdot \text{d}h(\|s_{\text{trial}}\|, \varepsilon^e_1, \varepsilon^e_2) \cdot \text{d}z_0 \cdot \text{d}h(\|s_{\text{trial}}\| - \|n\|) \cdot \frac{n}{\|n\|}, \quad (13)
\]

\[
(\dot{\varepsilon}^e_{\text{trial}}) = \omega(\Delta z_{as}/z_{as}) \cdot \omega(\Delta z_{as}/z_{as}) \cdot (1 + \Delta z_{as}/z_{as})^{-1} (\Delta \varepsilon_{\text{eff}} + [(\varepsilon^e_2)^2]_{\text{trial}} + \omega(\Delta z_{as}/z_{as}) \cdot (1 + \Delta z_{as}/z_{as})^{-1} \cdot (\dot{\varepsilon}^e_1)^2), \quad (14)
\]

The temperature and proportions of the phases are the known variables from thermo-metallurgical analysis. Therefore, the unknown variables are \( \Delta \gamma \), \( \dot{\varepsilon}^e_{\text{trial}} \), \( \Delta \varepsilon^e_{\text{eff}} \), \( \|s_{\text{trial}}\| \) and there are four equations for solving these four unknowns. Eliminating \( \Delta \gamma \) and \( \dot{\varepsilon}^e_{\text{trial}} \) from (12) and (14), equation (13) can be rewritten as

\[
\Delta \varepsilon^e_{\text{eff}} = a \hat{h}(\Delta \varepsilon^e_{\text{eff}}, \dot{\mu}, \|s_{\text{trial}}\|^2) + b[1 - 2\mu K(\Delta \varepsilon^e_{\text{eff}}, \dot{\mu})] \times \|s_{\text{trial}}\|^2 + c, \quad (15)
\]

where

\[
a = -2(\Delta x_1^{ch}) (1 - z_{as})^{-1} (\ln z_{as}) \Delta z_{as}, \quad (16a)
\]

\[
b = \sqrt{2/\gamma(\gamma - 1)} / E, \quad (16b)
\]

\[
c = -b\|s_{\text{trial}}\| + 2(\alpha_1 - \alpha_2) z_{as} (\gamma - 1)^{-1} \Delta \theta_{as}. \quad (16c)
\]

We find out \( \Delta \varepsilon^e_{\text{eff}} \) by solving (15) by Newton’s method and update the other state variables using (13) and (14). The consistent elastoplastic tangent moduli for the transformation plasticity are given as follows (see [4] for details):

\[
\varepsilon^p = c_{\text{vol}} + c_{\text{dev}}, \quad (17a)
\]

\[
c_{\text{vol}} = (JU)^' I \otimes I - 2JU'' I, \quad (17b)
\]

\[
c_{\text{dev}} = 2\mu[I - (1/3)I \otimes I] - (2/3)\|s_{\text{trial}}\|^2 [n \otimes I + I \otimes n], \quad (17c)
\]

\[
\beta_0 = 1 + k_{0 p}/3 \mu, \quad (18a)
\]

\[
\beta_1 = 2\mu \Delta \gamma / \|s_{\text{trial}}\|^2, \quad (18b)
\]

\[
\beta_2 = (2/3)[1 - 1/\beta_0] \cdot \|s_{\text{trial}}\|^2 \Delta \gamma / \mu, \quad (18c)
\]

\[
\beta_3 = 1/\beta_0 - \beta_1 + \beta_2, \quad (18d)
\]

\[
\beta_4 = [1 - 1/\beta_0] \cdot \|s_{\text{trial}}\|^2 / \mu, \quad (18e)
\]

\[
k_{0 p} = (3/2) \cdot (1 - a(\Delta \varepsilon_{\text{eff}})) K[1 - a(\Delta \varepsilon_{\text{eff}})] + (\Delta K / \Delta \varepsilon_{\text{eff}}) \|s_{\text{trial}}\|^2 (a(\Delta \varepsilon_{\text{eff}}) \cdot b)^{-1}. \quad (18f)
\]

3. Numerical examples

The first example is the flat plate welding problem. We assume that the heat source moves along an edge of the plate, and double ellipsoidal heat source [8] is used as below:

\[
q(x, y, z, t) = 31.46 \exp(-(y - 20t)^2/\alpha_c^2) \exp(-(z - 15)^2/\alpha_c^2) [\text{W mm}^{-3}], \quad (19)
\]

where

\[
\alpha_c = 6 \quad \text{if} \ y > 20t > 0
\]

\[
\alpha_c = 10 \quad \text{if} \ y < 20t < 0.
\]

Convection and radiation conditions are applied on the upper and lower surfaces for thermal boundary conditions and we use \( \chi = 2.5 \times 10^{-5} [\text{W mm}^{-3}]. \chi = 2.5 \times 10^{-8} [\text{W mm}^{-3}] \) as convection heat coefficient and Stephen–Boltzman coefficient. This model is assumed to be symmetric along the x-axis and no mechanical loadings are applied. We use two finite element models: the first one is a coarse mesh model composed of 1400 elements and the second one is a fine mesh model composed of 9400 elements as shown in figure 1.
Figure 7. Mises stress contour plots of CTBA model. (a) 10 s, (b) 20 s, (c) 30 s, (d) 40 s, (e) 50 s, (f) 60 s, (g) 100 s, (h) 1000 s.
There occurs a big difference of problem. CTBA is an auto part used near rear-wheel application of the present scheme for a large-scale welding axle (CTBA) manufacturing via welding to demonstrate the present approach yields solutions in good agreement with SYSWELD. Next we choose coupled torsion beam (CTBA) and the Mises stress along the weld line L1 after the cooling process is completed. They indicate that for the weld line there is no significant difference between the results from SYSWELD and from the present solution when the fine mesh is used. Furthermore, the stresses from the coarse mesh does not deviate much from the fine-mesh solution. However, along line L2, which is perpendicular to the weld line, there exists a substantial error in the coarse-mesh solution compared with the fine-mesh solution (see figures 5(a) and (b)). Particularly, near the weld line (around x = 20 mm) there occurs a big difference of σ_{11} between the two meshes, as seen from figure 5(a).

So far we have compared the present solution with the SYSWELD results for coarse and fine meshes, and the present approach yields solutions in good agreement with SYSWELD. Next we choose coupled torsion beam axle (CTBA) manufacturing via welding to demonstrate the application of the present scheme for a large-scale welding problem. CTBA is an auto part used near rear-wheel suspension systems of automobiles (see figure 6). The material is the same as in the previous test. Our finite element mesh has 141 513 eight-noded solid elements, which amounts to 327 075 degrees of freedom. There are no mechanical loadings applied other than the constraints to prevent the rigid body motion. There are total eight different welding paths, along which two neighbouring parts are joined by welding.

The heat source is modelled as an ellipsoidal form as follows:

\[ q(x, y, z, t) = q_0 \exp\left(-\left(\frac{y - y_w}{b}\right)^2\right) \exp\left(-\left(\frac{z - z_w}{c}\right)^2\right) \exp\left(-\left(\frac{x - x_w}{a}\right)^2\right) [\text{W mm}^{-3}] \]  

where \( (x, y, z) \), \( (x_w, y_w, z_w) \), \( q_0 \), \( a \), \( b \) and \( c \) denote each nodal point, the current position of welding bead, total heat input and shape parameters of the heat source model, respectively. In this problem, we use \( q_0 = 11.0 \) (W mm\(^{-3}\)), \( a = b = c = 4.0 \) (mm) and each of the welding beads moves along the prescribed lines with velocity \( v = 8.0 \) (mm s\(^{-1}\)).

It takes about 13 days for completing the computation with Pentium 4 processor with 3.4 GHz clock speed. Figure 7 shows the contour plots of the Mises stress for various times elapsed, and each legend scale in figure 7 means 10 MPa. More detailed data and a movie file can be found at http://me.kaist.ac.kr/~sim/kor/recent/welding.html.

The welding process is a multi-physics phenomenon, which requires consideration for the mechanical process as well as for the thermo-metallurgical process. The residual stress is generated due to elastoplastic deformation in the mechanical process in addition to the thermo-metallurgical process. To illustrate the effect of mechanical deformation process taking place prior to welding on the residual stress, we now choose the manufacturing process of spiral seam-welding pipes. This is a typical production process for pipes of practically large diameter. It starts with the bending of a flat plate into a spiral shape, which results in a cylindrical geometry. In this bending process there occurs plastic deformation through thickness due to very large strain. The bending process is followed by the welding process, wherein the bead moves along the spiral line along which the two edges meet (figure 8). The welding process first takes place on the interior surface, and about 67 s after the start of this interior welding the second path occurs from the exterior path along the spiral line.

The model geometry is shown in figure 9 and the total number of elements is 70 800, which amounts to 293 085 degrees of freedom for this FE model. We choose the length of the model to be 1 pitch. Figure 10(a) shows the circumferential residual stress contour plots after cooling in
the polar coordinates, while figure 10(b) shows the residual Mises stress. In this simulation, our major concern lies in the normal residual stress perpendicular to the weld line and the shear residual stress along the weld line. Figure 11 shows this residual normal and shear stress along the weld line. Because of the free edge effect, the stress components are up and down near the zero distance, but they remain invariant, which should be the case, when the distance is greater than 300 mm. Note that the shear residual stress occurs due to the asymmetry of the spiral weld line, and it disappears when the spiral angle becomes 90 degrees. High shear stress along the weld line is not desirable from the aspect of structural integrity of the spiral seam-weld pipes. The animation of the simulation for this deformation and welding process can be found at http://me.kaist.ac.kr/~sim/kor/recent/welding.html.

4. Concluding remarks

In this paper, we have demonstrated that Kim et al’s [4] finite element for transformation plasticity, wherein the consistent linearization and the hyperelastic formulation are employed, may be applied for large-scale welding problems of practical concern. The two examples show that the present finite element scheme is a useful tool to capture the residual deformation and stress resulting from the welding of real structures, of which finite element mesh includes more than several hundred thousands.

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