ON THE THERMOMECHANICAL COUPLING EFFECTS IN THE WELDING OF STEEL PLATES USING A CONSTITUTIVE MODEL

Roberto Monteiro Basto da Silva, robert_rj@bol.com.br
Pedro Manuel Calas Lopes Pacheco, calas@cefet-rj.br
CEFET/RJ - Department of Mechanical Engineering - Av. Maracanã, 229, 20271-110 - Rio de Janeiro - RJ - Brazil

Abstract. *Welding is a complex process where localized intensive heat input is furnished to a piece promoting mechanical and metallurgical changes. The temperature gradients developed through the piece promotes residual stresses fields when the piece reaches room temperature. Phenomenological aspects of welding involve basically, three couplings: thermal, phase transformation and mechanical phenomena. The present contribution regards on modeling and simulation of welded steel plates using an anisothermal constitutive model with internal variables. The model includes thermomechanical couplings in the energy equation associated with phase transformation, plasticity and hardening. A numerical procedure is developed based on the operator split technique associated with an iterative numerical scheme. With this assumption, coupled governing equations are solved from four uncoupled problems: Thermal, Phase Transformation, Thermo-elastic and Elastoplastic. The Thermal problem comprises a one-dimensional conduction problem with convection and heat input generated by the weld heat source. An implicit finite difference predictor-corrector procedure is used for numerical solution. For the Phase Transformation and Thermo-elastic problems the volumetric fractions of the phases and the stress distributions are determined, respectively, by integrating the evolution equations using a simple implicit Euler method. Finally, for the Elastoplastic problem stress and strain fields are determined using the classical return mapping algorithm. Numerical simulations are carried out analyzing the thermocoupling effects in the welding of thin steel plates. The proposed methodology can be used as a powerful tool to study the effects of welding parameters, like heat input or welding speed, in the residual stresses of welded mechanical components.*

Keywords*: Residual Stresses, Welding, Thermomechanical Coupling, Modeling, Numerical Simulation.*

1. INTRODUCTION

Welding is a complex process where localized intensive heat input is furnished to a piece promoting mechanical and metallurgical changes. Temperature gradients developed through the piece promote residual stress fields when the piece reaches room temperature (Almer *et al.*, 2000; Fernandes *et al.*, 2003, 2004; Teng and Chang, 2004). These residual stresses result from the thermal cycle caused by localized intensive heat input applied by the weld torch that promotes temperature gradients. Higher temperatures are responsible for lower values of mechanical properties, allowing the development considerable plastic strain. Also, high temperatures developed by the weld heat source promote phase transformation and plasticity. Moreover, phase transformation can promote phase transformation induced strain (Denis *et al.*, 1985; Pacheco *et al.*, 2001; Silva *et al.*, 2004; Ronda and Oliver, 2000). Due to the importance of estimate residual stresses in welding, several investigators had addressed this subject (Zacharia *et al.*, 1995; Taljat *et al.*, 1998; Bang *et al.*, 2002; Fernandes *et al.*, 2003, 2004; Silva *et al.*, 2005a, 2005b; Bezerra *et al.*, 2006).

Phenomenological aspects of welding involve basically, three couplings: thermal, phase transformation and mechanical phenomena. Due to the complexity of these couplings interaction, several authors have addressed these three aspects separately: some authors consider only the thermomechanical coupling (Bang *et al.*, 2002; Fernandes *et al.*, 2003, 2004; Teng and Chang, 2004) but it is important to note that in many situations the phase transformation must be also considered (Zacharia *et al.*, 1995; Taljat *et al.*, 1998; Ronda and Oliver, 2000).

It is well know that residual stress plays a preponderant part in the structural integrity of a mechanical component. Nevertheless, the presence of residual stress is not fully considered in traditional design of mechanical components. Some traditional design methodologies assume that the component is submitted to a null stress state before the application of the operational loading and the use of precise analytic and/or computational methods is not sufficient for a reliable structural integrity life prediction. The presence of tensile residual stresses can be especially dangerous to mechanical components submitted to fatigue loadings. In the presence of tensile stresses promoted by the operational loading conditions, both stresses are added resulting in much higher tensile stress levels than the ones predicted, and may be responsible for nucleation and propagation of cracks.

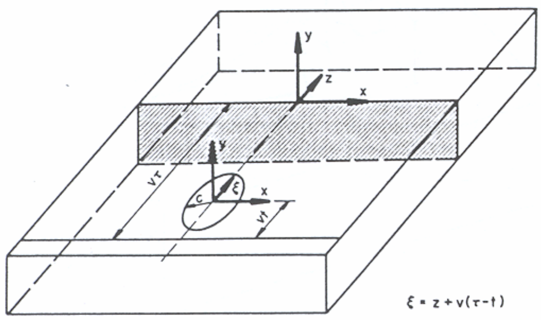
The present contribution regards on the study of the influence of the different coupligs present in the welding process, with the modeling and simulation of welded steel plates using an anisothermal constitutive model with internal variables. The model includes thermomechanical couplings in the energy equation associated with phase transformation, plasticity and hardening. This model was already applied to the study of other thermomechanical problems as the quenching of steel pieces (Pacheco *et al.*, 1997, 2001; Oliveira *et al.*, 2003; Silva *et al.*, 2004; Oliveira *et al.*, 2006) and low cycle fatigue life of metallic materials (Pacheco, 1994; Pacheco and Mattos, 1997; Nolte *et al.*, 2005). In this article, the model is applied to the welding of long thin steel plates, which allows the implementation of simplification hypotheses. A numerical procedure is developed based on the operator split technique associated with an iterative numerical scheme, to identify the influence of radiation effects during the cooling process and the effects promoted by the thermomechanical couplings in the final phase distribution and in the residual stress levels.

2. WELD HEAT SOURCE

In a welding process, the modeling of the moving weld heat source is one of the main aspects of study. Thus, the definition of an accurate model for this heat source is imperative. According to some authors, the heat source can be represented as a geometric distribution. Pavelic *et al.* (1969) suggested a Gaussian surface flux distribution disc. Friedman (1975) presents an alternative form for the Pavelic model expressed in a moving coordinate system (x,ζ), with an origin located at the center of the heat source. The surface flux distribution of the source inside the disc is given by:

$$q(x,\zeta) = \frac{3Q}{\pi\, C^2}\,\mathrm{e}^{-3(x/c)^2}\ \mathrm{e}^{-3(\zeta/C)^2} \tag{1}$$

where $\zeta = v\,(\tau - t)$, v is the velocity of the heat source, t is the time, τ is a lag factor needed to define the position of the source at time $t = 0$ and C is the radius of the surface flux Gaussian distribution in a disc with center at (0,0) and parallel to coordinate system, ζ, t. $Q = \eta\, V\, I$ is a power heat input source welding, where η is the heat source efficiency, V the voltage and I the current. Figure 1a shows the model for the surface flux distribution with the coordinate system.



(a)

Figure 1. Gaussian surface flux distribution heat source (a). Arrangement for the section bead on plate welds. (Goldak *et al.*, 1984).

In this work, the welding of a thin plate is considered as an application of the proposed general formulation. With this assumption, heat transfer analysis may be reduced to a one-dimensional problem. The small temperature variation along the thickness allows that the heat flow through this direction to be neglected. Also, for long plates, at distances far from the edges, the heat flow in the welding direction can also be neglected (Goldak *et al.*, 1984). Considering these simplifying hypotheses, the model for a moving weld heat source proposed by Eq. (1) and the symmetry condition observed at the plane yz (adiabatic condition), it is possible to reduce the analysis to a uniaxial thermal problem, with heat flow along the width of the plate (direction x).

3. CONSTITUTIVE MODEL

The phenomenological modeling of welding process is developed using a constitutive model with internal variables formulated within the framework of continuum mechanics and the thermodynamics of irreversible processes. Constitutive equations can be obtained by considering thermodynamic forces, defined from the Helmholtz free energy, ψ, and thermodynamic fluxes, defined from the pseudo-potential of dissipation, ϕ (Lemaitre and Chaboche, 1990; Pacheco, 1994).

With this aim, a Helmholtz free energy is proposed as a function of observable variables, total strain, ε_{ij}, and temperature, T. Moreover, the following internal variables are considered: plastic strain, ε_{ij}^{p}, kinematic hardening, α_{ij}, volumetric fractions of different microstructures, represented by phases in a macroscopic point of view, $\beta = \beta_i$ (i = 1,...,N_{phases}, where N_{phases} is the total number of different phases), volumetric transformation strain, ε_{ij}^{tv}, and transformation plasticity strain, ε_{ij}^{tp}. The volumetric transformation strain is related to volumetric expansion associated with phase transformation from a parent phase. Transformation plasticity strain represents the result of several physical mechanisms related to local plastic strain promoted by the phase transformation (Denis *et al.*, 1985; Sjöström, 1985;

Desalos *et al.*, 1982). It should be emphasized that this strain may be related to stress states that are inside the yield surface. Therefore, the following free energy is proposed, employing indicial notation where summation convention (i = 1,2,3) is evoked (Eringen, 1967), except when indicated:

$$\begin{aligned}\rho\psi(\varepsilon_{ij},\varepsilon^p_{ij},\varepsilon^{tv}_{ij},\varepsilon^{tp}_{ij},\alpha_{ij},\beta,T) &= W(\varepsilon_{ij},\varepsilon^p_{ij},\varepsilon^{tv}_{ij},\varepsilon^{tp}_{ij},\alpha_{ij},\beta,T) = \\ &= W_e(\varepsilon_{ij}-\varepsilon^p_{ij},\varepsilon^{tv}_{ij},\varepsilon^{tp}_{ij},\beta,T)+W_\alpha(\alpha_{ij})+W_\beta(\beta)-W_T(T)\end{aligned} \tag{2}$$

where ρ is the material density. The increment of elastic strain is defined as follows:

$$d\varepsilon^e_{ij} = d\varepsilon_{ij} - d\varepsilon^p_{ij} - d\varepsilon^{tv}_{ij} - d\varepsilon^{tp}_{ij} - \alpha_T dT\delta_{ij} \tag{3}$$

where the last term is associated with thermal expansion and the parameter α_T is the coefficient of linear thermal expansion.

This general model was previously applied to the analysis of steel pieces quenching (Pacheco *et al.*, 2001; Oliveira *et al.*, 2003; Oliveira, 2004; Silva *et al.*, 2004; Oliveira *et al.*, 2006) and also the thermomechanical coupling effects on low cycle fatigue life of metallic materials (Pacheco, 1994; Pacheco and Mattos, 1997; Nolte *et al.*, 2005). It allows one to identify different coupling phenomena, estimating the effect of each one in the process. A detailed description of this constitutive model may be obtained in the cited references.

This contribution considers the welding of long thin plates as an application of the proposed general formulation. With this assumption, heat transfer analysis may be reduced to a one-dimensional problem, neglecting the heat flow through the thickness and also in the welding direction (respectively, directions y and z, in Fig. 1a). A null strain state is assumed in the welding direction ($\varepsilon_z = \varepsilon = 0$) in order to simulate the restriction associated with adjacent regions of the heated region, which are at lower temperatures. Also, null stresses are assumed in the thickness and transversal directions (respectively, directions y and x, in Fig. 1a). Therefore, a one-dimensional stress state is observed, and the only non-null stress is in the welding direction ($\sigma_z = \sigma$). The phenomenological description of the welding phenomenon considers seven phases: austenite (β_A), ferrite (β_1), cementite (β_2), pearlite (β_3), upper bainite (β_4), lower bainite (β_5) and martensite (β_6). Since a one-dimensional model is assumed, tensor quantities presented in the general formulation may be replaced by scalar quantities. For this situation the thermodynamics forces (σ, P, Q, R, X, B^β, s), associated with state variables $(\varepsilon,\varepsilon^p,\varepsilon^{tv},\varepsilon^{tp}\alpha,\beta,T)$, are defined as follows:

$$\begin{aligned}&\sigma=\frac{\partial W}{\partial\varepsilon}=E(\varepsilon\text{-}\varepsilon^p-\varepsilon^{tv}-\varepsilon^{tp})-E\alpha_T(T-T_0) \quad ; \quad P=-\frac{\partial W}{\partial\varepsilon^p}=\sigma \\ &Q=-\frac{\partial W}{\partial\varepsilon^{tv}} \quad ; \quad R=-\frac{\partial W}{\partial\varepsilon^{tp}} \quad ; \\ &\mathrm{X}=\frac{\partial W}{\partial\alpha}=H\alpha \quad ; \quad \mathrm{B}^\beta=-\frac{\partial W}{\partial\beta} \quad ; \quad \mathrm{s}\text{=-}\frac{\partial W_T}{\partial T}\end{aligned} \tag{4}$$

where T_0 is a reference temperature, E is the Young modulus and H is the plastic modulus.

In order to describe dissipation processes, it is necessary to introduce a potential of dissipation $\phi(\dot\varepsilon^p,\dot\varepsilon^{tv},\dot\varepsilon^{tp},\dot\alpha\ ,\dot\beta,q\ )$, which can be split into two parts: $\phi(\dot\varepsilon^p,\dot\varepsilon^{tv},\dot\varepsilon^{tp},\dot\alpha\ ,\dot\beta,q\ )=\phi_I(\dot\varepsilon^p,\dot\varepsilon^{tv},\dot\varepsilon^{tp},\dot\alpha\ ,\dot\beta)+\phi_T(q\ )$. This potential can be written through its dual $\phi^*(P,Q,R,X,B^\beta,g)=\phi^*_I(P,Q,R,X,B^\beta)+\phi^*_T(g)$, defined as follows:

$$\phi^*_I = I^*_f(P,X)+\sum_{i=1}^{6}B^{\beta_i}\dot\beta_i \qquad \phi^*_T=\frac{T}{2}\Lambda\, g^2 \tag{5}$$

where $g = (1/T)\ \partial T/\partial x$ and Λ is the coefficient of thermal conductivity; $I^*_f(P,X)$ is the indicator function associated with elastic domain (Lemaitre and Chaboche, 1990),

$$f(\sigma,X)=\left|\sigma-X\right|-S_Y\le 0 \tag{6}$$

where S_Y is the material yield stress. A set of evolution laws obtained from ϕ* characterizes dissipative processes,

$$\dot{\varepsilon}^{p} = \frac{\partial \phi^{*}}{\partial P} = \lambda \operatorname{sign}\left(\sigma - H\,\alpha\right) \quad ; \quad \dot{\alpha} = -\frac{\partial \phi^{*}}{\partial X} = \dot{\varepsilon}^{p}$$

$$\dot{\varepsilon}^{tv} = \frac{\partial \phi^{*}}{\partial Q} = (\sum_{i=1}^{6} \gamma_i \dot{\beta}_i) \quad ; \quad \dot{\varepsilon}^{tp} = \frac{\partial \phi^{*}}{\partial R} = \sum_{i=1}^{6} \kappa_i\, g(\beta i) \dot{\beta}_i\, \sigma \quad ; \quad q = -\frac{\partial \phi^{*}}{\partial g} = -\Lambda T\, g = -\Lambda \frac{\partial T}{\partial x}$$

$$\dot{\beta}_6 = \frac{\partial \phi^{*}}{\partial B^{\beta_6}} = \varsigma_{A\to M}\left[\left(1-\beta_6\right) k\dot{T}\right] \tag{7}$$

$$\dot{\beta}_i = \frac{\partial \phi^{*}}{\partial B^{\beta_i}} = \varsigma_{A\to phase\,(i)} \left\{ n_i\,(b_i)^{(1/n_i)} \left(\beta_i^{\max} - \beta_i\right) \left[\ln\left(\frac{\beta_i^{\max}}{\beta_i^{\max} - \beta_i} \right) \right]^{\left(1-\frac{1}{n_i}\right)} \right\} \quad \text{for } (i = 1, ..., 5)$$

where λ is the plastic multiplier (Lemaitre and Chaboche, 1990) from the classical theory of plasticity, $sign(x) = x / |x|$ and q is the heat flow. k is a material property related to martensitic transformation and γ is a material phase property related to total expansion. κ_i is a material phase parameter and $g(\beta_i)$ expresses the dependence on the transformation process (i = 1, ..., 6). n_i is the Avrami exponent and b_i is a parameter that characterizes the rate of nucleation and growth processes (Avrami, 1940).

Besides, the non-diffusive austenite-martensite phase transformation (Koistinen and Marburger, 1959) considers the following condition:

$$\varsigma_{A\to M}\left(\dot{T}, T\right) = \Gamma\left(-\dot{T} - rMs\right)\Gamma\left(M_s - T\right)\Gamma\left(T - M_f\right) \tag{8}$$

where rM_s is the critical cooling rate for the martensite formation, defined from the Continuous-Cooling-Transformation diagram (*CCT*) diagram; $\dot{T}$ is the cooling rate, and $\Gamma(x)$ is the Heaviside function. M_s is the temperature where martensite starts to form in the stress-free state and M_f is the temperature where martensite finishes its formation in the stress-free state.

The diffusion-controlled transformations related to the formation of pearlite, cementite, ferrite and bainite are predicted through an approximate solution using data from Time-Temperature-Transformation diagrams (*TTT*) (Oliveira *et al.*, 2003; Oliveira, 2004). The phase transformation analysis using this diagram is done considering that the cooling process may be represented by a curve divided in a sequence of isothermal steps. Through each isothermal step, the phase evolution is calculated considering isothermal transformation kinetics expressed by a *JMAK* law (Avrami, 1940; Cahn, 1956;). The following condition must be defined to incorporate the temperature dependent functions, t_i^s and t_i^f that limits the beginning and the ending of the phase transformation, and also to assure its irreversibility:

$$\varsigma_{A\to phase\,(i)}\left(\dot{T}, t\right) = \Gamma\left(-\dot{T}\right)\Gamma\left(t_i^f - t\right)\Gamma\left(t - t_i^s\right) \qquad (i = 1, ..., 5) \tag{9}$$

A detailed description of the phenomenological aspects of phase transformation process using the proposed model can be obtained in (Oliveira *et al.*, 2003; Oliveira, 2004).

Now, by assuming that the specific heat is $c = -(T/\rho)\,\partial^2 W / \partial T^2$ and the set of constitutive Eqs. (4) and (7), the energy equation can be written as (Pacheco, 1994):

$$\frac{\partial}{\partial x}\left(\Lambda\,\frac{\partial T}{\partial x}\right) - \rho c\dot{T} + \frac{Per}{A}\left[-\mathrm{h}(\mathrm{T}-\mathrm{T}_{\infty})\right] + \rho \mathrm{r} = -a_I - a_T \qquad \begin{cases} a_I = \sigma\,\dot{\varepsilon}^{p} - X\dot{\alpha} + B^{\beta}\dot{\beta} \\ a_T = T\left(\dfrac{\partial \sigma}{\partial T}\left(\dot{\varepsilon} - \dot{\varepsilon}^{p}\right) + \dfrac{\partial X}{\partial T}\dot{\alpha} - \dfrac{\partial B^{\beta}}{\partial T}\dot{\beta}\right) \end{cases} \tag{10}$$

where h is the convection coefficient, T_∞ is the surrounding temperature, r represents the weld heat source, Per is the perimeter and A is the cross section. Terms a_I and a_T are, respectively, internal and thermal coupling. The first one appears in the right hand side of the energy equation and is called internal coupling. It is always positive and has a role in the energy equation similar to a heat source in the classical heat equation for rigid bodies.

In this article, special considerations are used for radiation and coupling effects. The radiation heat transfer influence is relatively small, compared to conduction and convection influence. The radiation effect is considered in the model through a radiation coefficient h_{RAD} (Andersen, 2000):

$$h_{RAD} = \frac{em \cdot \tau \cdot \left[\left(T + 273\right)^4 - \left(T_\infty + 273\right)^4 \right]}{T} \tag{11}$$

where τ is the Stephan-Boltzmann constant (5.6697 x 10^{-8} W / m² K^{-4}) and em is the surface emissivity. Although the emissivity is a temperatura-dependent property, in this article a constant value of 0.7 is adopted, an average value for steel submitted to temperatures above 1000 °C (Carpinteri and Majorana, 1995). The coefficient defined by Eq.(11) is added to the convection coeffient in Eq.(10), resulting in a surface heat loss coefficient.

A simplification is also adopted for the internal and thermal couplings. In this work, the internal coupling is represented by the latent heat caused by phase transformation (Denis *et al.*, 1987):

$$a_I + a_T = \dot{Q} = \sum_{i=1}^{6} \Delta H_i \cdot \dot{\beta}_i \tag{12}$$

where ΔH_i is the entalpy variation that occurs during the transformation of auestenite in the microstructural phase i.

4. NUMERICAL PROCEDURE

The numerical procedure here proposed is based on the operator split technique (Ortiz *et al.*, 1983; Pacheco, 1994) associated with an iterative numerical scheme in order to deal with non-linearities in the formulation. With this assumption, coupled governing equations are solved from four uncoupled problems: thermal, phase transformation, thermo-elastic and elastoplastic.

Thermal Problem - Comprises a uniaxial conduction problem with convection, radiation and heat input generated by the weld heat source. Material properties depend on temperature, and therefore, the problem is governed by non-linear parabolic equations. An implicit finite difference predictor-corrector procedure is used for numerical solution (Ames, 1992; Pacheco, 1994).

Phase Transformation Problem – The volumetric fractions of the phases are determined in this problem. Evolution equations are integrated from a simple implicit Euler method (Ames, 1992; Nakamura, 1993).

Thermo-elastic Problem - Stress is evaluated from temperature distribution. Evolution equations are integrated from a simple implicit Euler method (Ames, 1992; Nakamura, 1993).

Elastoplastic Problem - Stress and strain fields are determined considering the plastic strain evolution in the process. Numerical solution is based on the classical return mapping algorithm (Simo and Miehe, 1992; Simo and Hughes, 1998).

5. NUMERICAL SIMULATIONS

The proposed model is applied to the welding of a thin plate of AISI 4140H steel by manual shield metal arc welding (SMAW) process, air-cooled. A butt-joint is considered for a plate with a thickness of 10 mm, a length (L) of 500 mm, a width (w) of 300 mm and a root opening of 2.4 mm. Thermal and mechanical properties are temperature dependent and are fitted by polynomial equations from experimental data (Pacheco *et al.*, 2001; Oliveira *et al.*, 2003; Oliveira, 2004; Silva *et al.*, 2004). For the presented simulations the following parameters are used: Q = 3500 W, C = 10 mm, v = 0.5 mm/s, T_∞ = 20°C, ρ = 7800 kg/m^3 and initial temperature of 20 °C. Regarding phase transformation, the following parameters are used: γ = 1.33 x 10^{-2}, k = 1.1 x 10^{-2} /C°, κ = 1.0 x 10^{-10}, M_s = 370 °C, M_f = 260 °C, rMs = 8 s and austenitization and solidification temperatures of 843 °C and 1400 °C, respectively. Also, an initial condition relative to the volumetric phases present at the beginning of the process considers the plate with 30% of ferrite and 70% of pearlite.

As mentioned before the heat flow through the thickness and in the welding direction is neglected. These hypotheses are justified by the small thickness of the plate and also because the region of interest is located far from the edges. Moreover, symmetry condition at the yz plane (adiabatic condition) is considered. An important point is that the null strain state assumed in the welding direction ($\varepsilon_z = \varepsilon = 0$) in order to simulate the restriction associated with adjacent regions of the heated region promotes higher levels of stress than the ones observed in actual situations as the material is not completely restricted in this direction.

Numerical simulations are performed with a computational software developed in C programming language. A spatial discretization of 160 nodes is employed. The final mesh and the time step are defined after a convergence analysis. The analysis considers two stages: *welding process* and *cooling*. In the *welding process* stage, the specimen is subjected to the weld heat source surface flux distribution represented by Eq. (1) during the heat source disc passage through the plane of the model (xy). The cooling stage is prior to operation, and is responsible for the development of the residual stress field. During this process, the plate exchanges heat with the surroundings by convection and radiation until it reaches thermal equilibrium. In order to simulate the absence of material at the root opening before the weld deposition, it is assumed null values to the mechanical properties at this region until the solidification temperature is reached during the *cooling* stage. Figure 3 represents the spatial discretization mesh used in the numerical simulations developed.

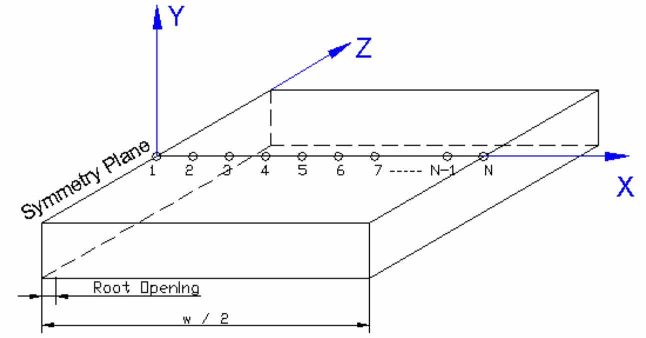


Figure 3 - Spatial discretization.

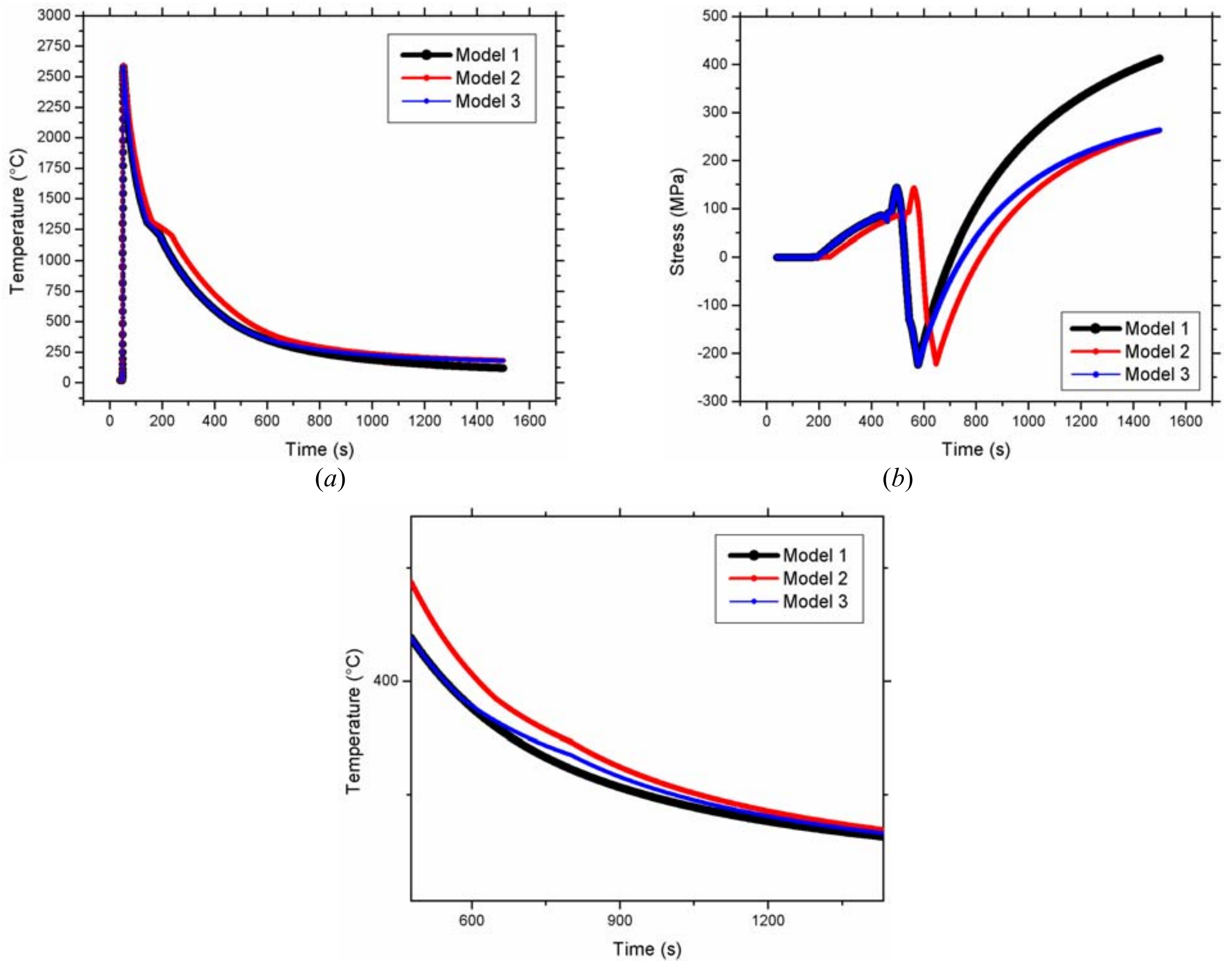
In order to analyse the influence of the thermomechanical coupling and radiation in the behavior of the material in the welding process, three models are considered:

Model 1: neglects thermomechanical coupling and considers radiation;

Model 2: includes thermomechanical coupling and neglects radiation;

Model 3: presents the complete model, with both thermomechanical coupling and radiation.

Figure 4 presents a comparison between numerical results for the three proposed models, showing the evolution of temperature and stress for the position located at x = 0, localized on the symmetry plane inside the weld zone.



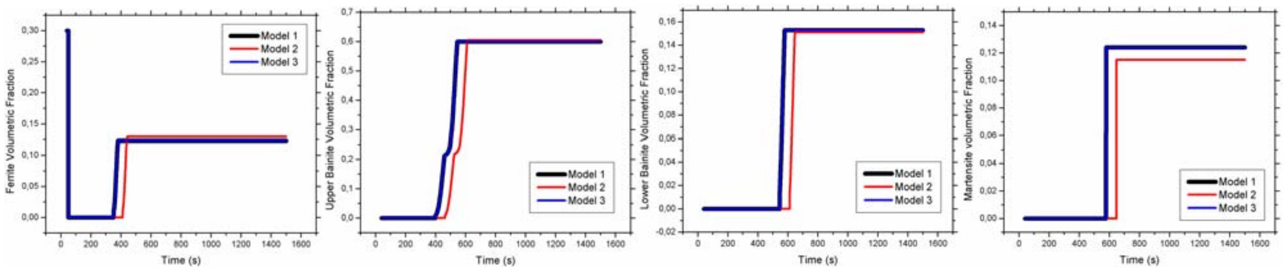
(*a*) (*b*)

(*c*)

Figure 4 – Temperature (*a*) and stress (*b*) evolution for the three models at symmetry plane. Detailed view of the temperature evolution (*c*).

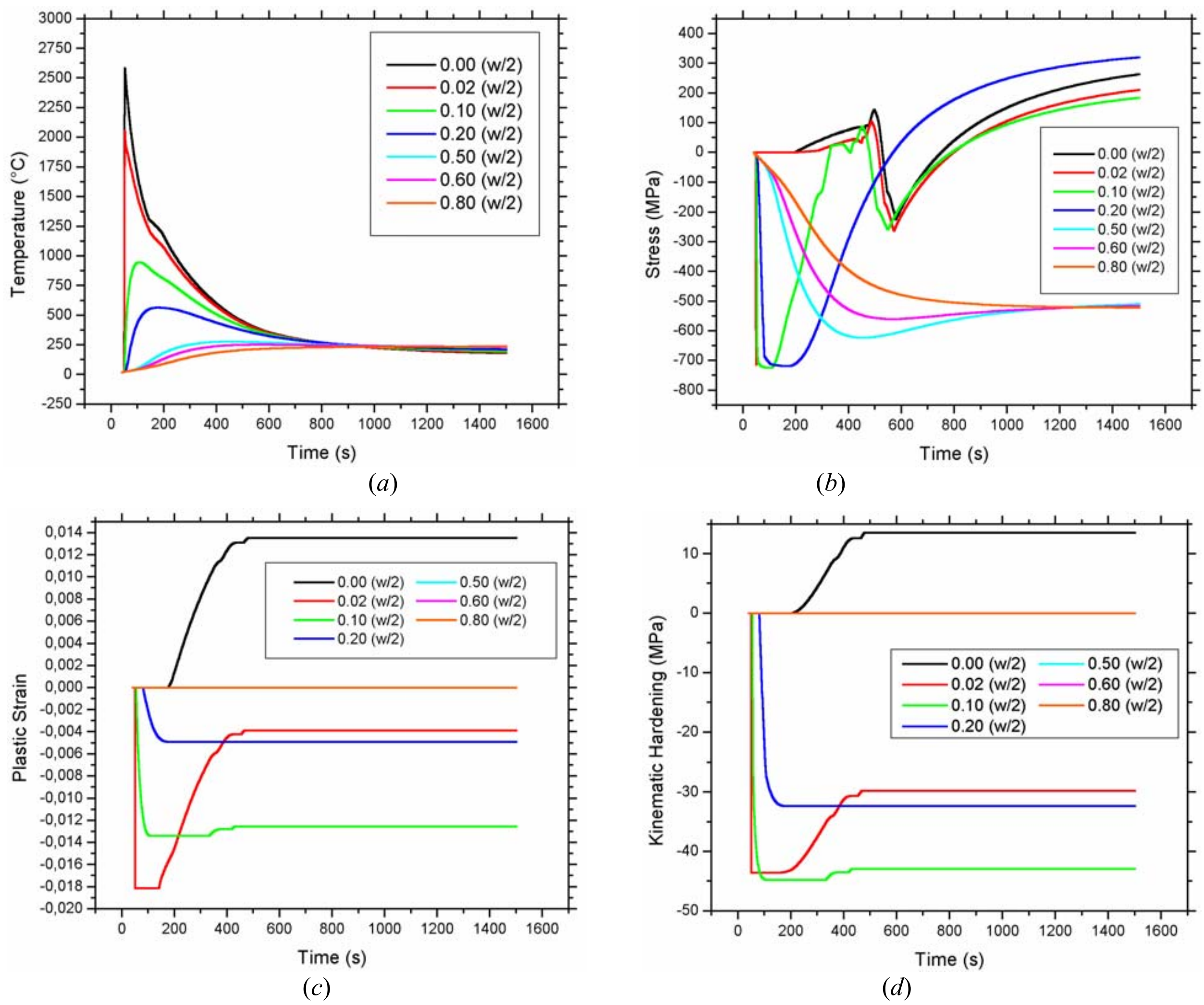
Figure 4*c* shows a detailed view of the temperature evolution for a region associated to martensitic phase transformation. A comparison between data from *Model 1* and *Model 3* shows how the thermomechanical coupling affects the cooling rate of the steel plate, resulting in a slight decrease in the cooling rate. Stress evolution is also affected by the thermomechanical coupling, as shown in Fig. 4*b*. A comparison between the results obtained from *Model 2* and *Model 3* shows that radiation promotes an advance in the evolution of both temperature and stress.

Figure 5 presents the volumetric phase fraction evolution for the three models. At this position (at the symmetry plane), thermomechanical coupling does not affect phase transformation. As for temperature and stress, radiation promotes an advance in phase transformation which slightly affects the final amount of predicted phases.



(*a*) (*b*) (*c*) (*d*)
Figure 5 – Volumetric phase fraction evolution: ferrite (*a*), upper bainite (*b*), lower bainite (*c*) and (*d*) martensite.

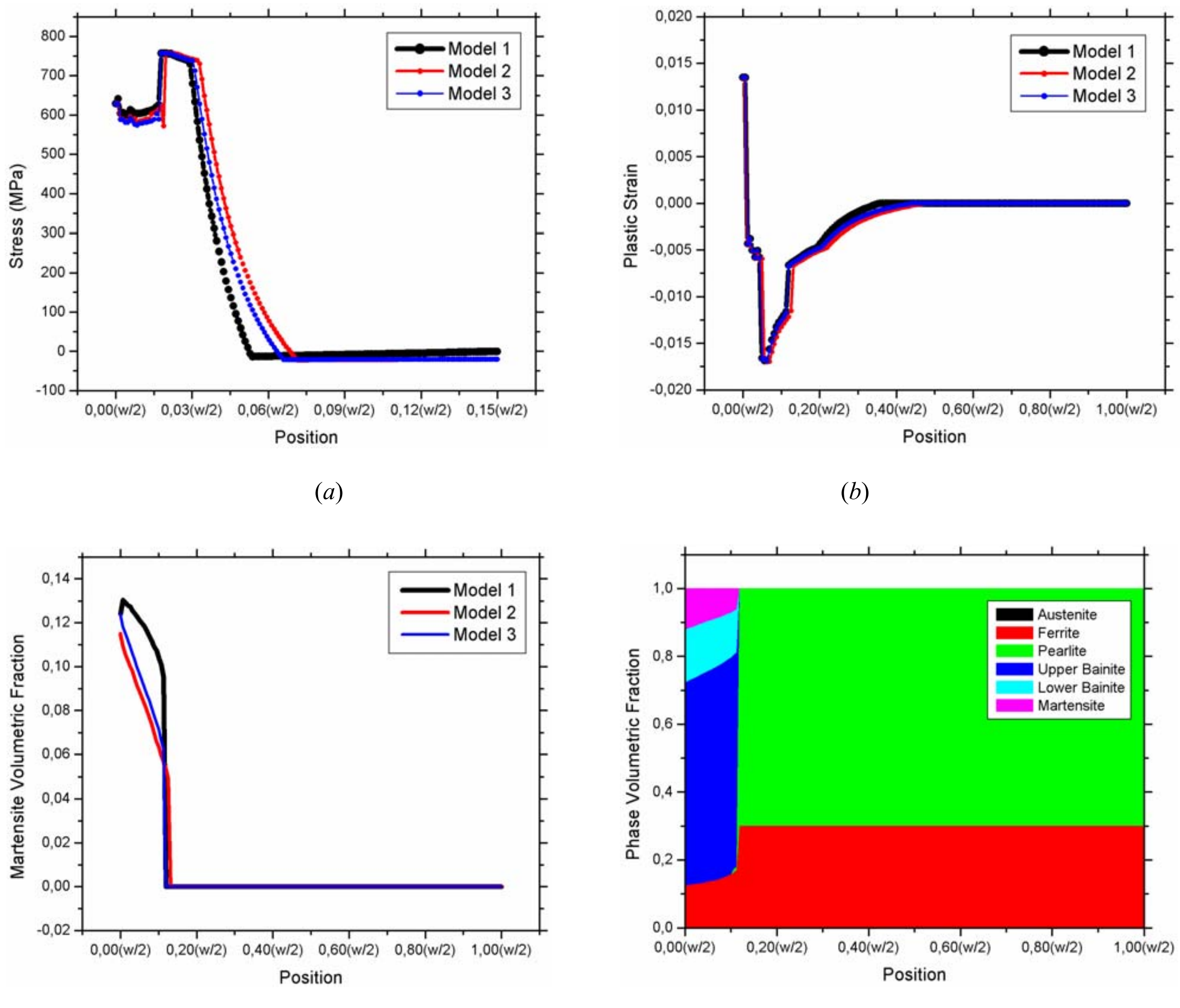
At this point, a detailed analysis is performed considering the complete model, *Model 3*. Figure 6 presents the temperature, stress, plastic strain and kinematic hardening evolution obtained for selected points. A complex behavior is observed for the different regions of the weld joint.



(*a*) (*b*)
(*c*) (*d*)
Figure 6 – Temperature (*a*), stress (*b*), plastic strain (*c*) and kinematic hardening (*d*) evolution for *Model 3*.

In Fig. 6*a* it is possible to observe that high temperature gradients develop through the plate. Region from $x = 0$ to $x = 0.10(w/2)$ experiments higher temperature values than the austenitization temperature of (843 °C). The stress evolution in Fig. 6*b* shows the complexity of the mechanical behavior of the welded joint. Positions located near the root opening are submitted to compressive stresses, caused by the restraint imposed by the surrounding material in lower temperatures. These same positions present a stress inversion during cooling. Phase transformation acts as a stress-relief mechanism, resulting in a second stress inversion. Some positions experience a slight perturbation of this behavior, caused by different phase transformation start times, like position $0.10(w/2)$ at time $t = 400$ s.

At this moment, it is important to analyse the variables distribution along the width of the plate, comparing the behaviors of the three proposed models. Figure 7*a* presents the residual stress distribution along the half-width of the plate: the traditional behavior of stresses along the welding direction is verified, with tensile stresses near the weld bead and compressive stresses far from that position. The three models present a similar final distribution of stresses and strain along the plate. Figure 7*c* presents a comparison between the martensite phase distribution for the three models, showing that the thermomechanical coupling associated to the latent heat, active in *Model 2* and in *Model 3*, affects the phase transformation phenomena, resulting in a lower martensite fraction in the region between the symmetry plane and the position $0.10(w/2)$. Figure 7*d* shows the volumetric phase fraction distributions along the plate at the final time for *Model 3*, indicating that phase transformation develops in the region $0 \leq x \leq 0.10(w/2)$. This is the same region where larger compressive strains values and considerable stress reduction are observed (Figs. 7*b* and 7*a*, respectively), showing an association between phase transformation and stress reliving, already appointed in previously works (da Silva and Pacheco, 2005).



(*a*) (*b*)

(*c*) (*d*)

Figure 7 – Stress (*a*), strain (*b*) and martensite volumetric fraction (*c*) distributions along the plate for the final time for the three models.
Volumetric phase fraction distributions along the plate for the final time for *Model 3* (*d*).

6. CONCLUSIONS

This work presents a study of residual stresses in welded thin steel plates. A constitutive anisothermal model that includes seven phases (austenite, ferrite, cementite, pearlite, upper bainite, lower bainite and martensite) is developed in order to estimate the residual stress distribution after the welding process. An iterative numerical procedure is developed in order to deal with the nonlinearities of the formulation. Thin plates are analyzed allowing the use of one-dimensional models. Three different models are considered, estimating the influence of the effect of radiation and thermomechanical coupling associated to latent heat during welding process. Numerical simulations show that high values of residual stresses (of the initial yield strength magnitude) are present at the end of welding process. The results indicate that it is important to consider phase transformation in the prediction of residual stresses in welded steel plates as phase transformation may significantly change the residual stresses distribution. Also, the numerical simulations show the influence of the thermomechanical coupling on the final phase distribution. The proposed methodology can be used as a powerful tool to study the effects of welding parameters, as heat input or welding velocity, in the determination of residual stresses and phase distribution of welded mechanical components. Moreover, an experimental program must be established to validate the proposed model.

7. ACKNOWLEDGEMENTS

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