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Residual stress analyses of re-autofrettaged thick-walled tubes

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ABSTRACT

In this paper the effect of the re-autofrettage process on the residual stress distribution at the wall of a thick-walled tube is considered. For accurate material behavior modeling, it is assumed that the yield surface is a function of all the stress invariants. Also for estimating the behavior of the material under loading–unloading process, a modified Chaboche's hardening model is applied.

For evaluation of this unloading behavior model a series of loading–unloading tests are conducted on specimens that are made of the high strength steel, DIN1.6959. In addition the finite element simulations are implemented to simulate the re-autofrettage process and to estimate the residual stresses. The numerical results show that the re-autofrettage process without heat treatment only improves the residual stress distribution in high autofrettage percentage and for low autofrettage percentage this method is not beneficial. However, the re-autofrettage process with "heat soak" treatment generally improves the residual stress distribution.

1. Introduction

Autofrettage process is applied to introduce the compressive residual stresses at the bore of thick-walled tubes. The compressive residual stresses enhance the fatigue life time of the thick-walled tubes. The thick-walled tubes are typically constructed from high strength steels. Some of these materials have nearly elastic-perfectly plastic behavior in the loading phase of a tension-compression test, but in the unloading phase they show a strong nonlinear hardening behavior. Milligan et al. [1] showed that the Bauschinger effect [2] significantly decreases the yield strength of high strength steels in compression as a result of prior tensile plastic overload. The Bauschinger effect also reduces the compressive residual stresses at the bore of the thick-walled tubes [3]. For decreasing the influence of the Bauschinger effect on residual stresses distribution at the bore of thick-walled tubes, the reapplication of the autofrettage procedure was proposed [4]. In swage autofrettage which is carried out by inserting an oversized mandrel into the bore of thick-walled tubes, Iremonger and Kalsi [5] showed that a more uniform stress is obtained by inserting the mandrel two times such that in each time from different end. But their results showed that such process does not increase the compressive residual stresses. For hydraulic autofrettage process, Parker [6] proposed a manufacturing procedure for enhancing residual stresses. The procedure involves initial autofrettage with one or more "heat soak plus autofrettage" sequences. He showed that the Bauschinger effect is significantly reduced with this procedure. Troiano et al. [7] also show the possibility of mitigating the Bauschinger effect via an intermediate heat soak between loading and unloading phase of a tension-compression test. Jahed et al. [8] showed that with applying different pressures in first and second autofrettage processes, higher compressive stress can be obtained. Parker and Huang [9] also showed that the re-autofrettage process can be beneficial for spherical vessels. Parker et al. [10,11] showed that the hydraulic re-autofrettage of a swage-autofrettaged tube with a low temperature post-autofrettage thermal treatment, results in higher compressive residual stresses.

The important aspect of residual stresses estimation in re-autofrettage process is the accurate modeling of loading–unloading behavior. There are many researchers who considered the loading–unloading behavior of high strength steels. Troiano et al. [12] evaluated the uniaxial Bauschinger effect in several high strength steels and showed that the reduction of the Young's modulus and yield strength occurs after unloading. Parker et al. [13] proposed a nonlinear kinematic hardening model for predicting the material behavior during initial load reversal. More recent works such as Jahed et al. [14], Hojjati and Hassani [15] and Farrahi et al. [16,17] introduced methods to predict the accurate unloading behavior and precise residual stress estimation.

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All research mentioned above applied the experimental results of the uniaxial tension-compression tests or reversed torsion tests to describe the material behavior under reversed loading. However, recent research studies have shown that the stress state should be incorporated into the constitutive description of the plastic deformation. Spitzig et al. [18–20] based on experimental study, demonstrated the effect of hydrostatic pressure on yielding of metals. Brunig [21] similar to the Drucker–Prager yield condition in soil mechanics proposed a yield criterion as a function of the first stress invariant to describe the effect of the hydrostatic pressure on metal plasticity. Later Brunig et al. [22] added the third deviatoric stress invariant in the yield criterion to describe the deformation of metals. Bai and Wierzbicki [23] introduced a yield criterion that is a function of pressure and Lode angle and compared their model with experimental tests conducted on aluminum 2024-T351. Mirone and Corallo [24] showed that, for the metals they tested, the hydrostatic stress has a significant effect on fracture and an insignificant effect on the stress-plastic strain relationship, while the Lode angle has an opposite role. Gao et al. [25] introduced a stress-state dependent plasticity model using the non-associated flow rule and Voyiadjis et al. [26] also introduced a yield criterion based on the Drucker–Prager yield condition with incorporating the effect of the Lode angle.

In this paper for estimating the residual stresses at the bore of the thick-walled tubes, the plasticity model proposed by Voyiadjis et al. [26] is considered. This model was used by Farrahi et al. [27] to analyze the residual stresses which are introduced into the bore of thick-walled tubes under single autofrettage process. In this plasticity model, the yield criterion and the hardening parameters are affected by stress invariants. In addition the effect of the previous plastic deformation history is incorporated into the hardening parameters. For evaluating the loading–unloading behavior a series of tension–compression tests are conducted on specimens which are made of a high strength steel, DIN1.6959. A finite element procedure is also implemented to assess the mentioned loading–unloading model. Good agreement is obtained between the experimental loading–unloading results and the simulations from the numerical modeling. Using this model and numerical methods, the re-autofrettage process is simulated in a thick-walled tube and the effect of the "heat soak" process is also considered.

2. Formulation of the plasticity model

The elasto–plastic behavior of the material considered in this section is assumed to be rate independent and elastically isotropic. Since the strains are infinitesimal, the total strain rate is decomposed into the elastic and plastic components:

$$\dot{\varepsilon}_{ij} = \dot{\varepsilon}^{e}_{ij} + \dot{\varepsilon}^{p}_{ij} \tag{1}$$

where the superscripts e and p designate the elastic and plastic components, respectively. The accumulated plastic strain rate, $\dot{p}$, is expressed as:

$$\dot{p} = m\sqrt{\dot{\varepsilon}^{p}_{ij}\dot{\varepsilon}^{p}_{ij}} \tag{2}$$

where m is the inverse magnitude of the normal vector to the yield surface. The Cauchy stress tensor is defined as:

$$\sigma_{ij} = \tilde{D}_{ijkl}\left(\varepsilon_{kl} - \varepsilon^{p}_{kl}\right) \tag{3}$$

where σ is the Cauchy stress tensor, and $\tilde{D}$ is the fourth-order isotropic elastic stiffness tensor that is affected by the damage through the plastic deformation. Isotropic elastic stiffness tensor is defined as:

$$\tilde{D}_{ijkl} = \tilde{\lambda}\delta_{ij}\delta_{kl} + \tilde{G}\left(\delta_{ik}\delta_{jl} + \delta_{il}\delta_{jk}\right) \tag{4}$$

where $\tilde{\lambda}$ and $\tilde{D}$ are Lame constants that are defined as:

$$\tilde{\lambda} = \frac{\nu\tilde{E}}{(1+\nu)(1-2\nu)}, \qquad \tilde{G} = \frac{\tilde{E}}{2(1+\nu)} \tag{5}$$

where $\tilde{E}$ is the Young's modulus in the damaged configuration and is defined as:

$$\tilde{E} = E(1-\phi) \tag{6}$$

where E is the initial Young's modulus and ϕ is a scalar damage function which is defined as a function of the accumulated plastic strain. This damage function has no effect on the yield surface and is determined experimentally. In this study, the Poisson's ratio is assumed to remain unchanged through the plastic deformation. The yield function, f, is defined as follows:

$$f = \sqrt{\frac{3}{2}(s-X):(s-X)} - Y \le 0 \tag{7}$$

where X is the kinematic hardening back stress tensor that describes the movement of the yield surface in the deviatoric space and Y is the radius of the yield surface. For accurate estimation of the yield surface movement, the back stress tensor can be divided into finite components, where each component is evaluated independently. Kinematic hardening back stress according to the additive decomposition method proposed by Chaboche and Rousselier [28,29] can be expressed as follows:

$$X_{ij} = \sum_{k=1}^{M} X^{(k)}_{ij} \tag{8}$$

where $X^{(k)}_{ij}$ is the k-th component of the back stress tensor. The radius of the yield surface, Y, can be defined as:

$$Y = \sigma_y + R \tag{9}$$

where R is the isotropic hardening force and describes the change in the size of the yield surface. In this work the plastic strains are evaluated using the normality rule

$$\dot{\varepsilon}^{p}_{ij} = \dot{\lambda}\frac{\partial F}{\partial \sigma_{ij}} \tag{10}$$

where $\dot{\lambda}$ is the multiplier of time-independent plasticity which will be determined using the consistency condition.

In this research, the general formulation of the yield condition and hardening parameters proposed by Voyiadjis et al. [26] are considered. This model modifies the Drucker–Prager yield condition with incorporating the Lode angle parameter. Therefore the linear effect of the first stress invariant and nonlinear effect of the Lode angle on the yield condition are assumed. The yield condition is defined as follows:

$$\sigma_y = \sigma_0 - \alpha_1 I_1 - \alpha_2\overline{\theta} \tag{11}$$

where σ_0, α_1 and α_2 are the material constants, I_1 is the first Cauchy stress tensor invariant and $\overline{\theta}$ is the Lode angle parameter [26]. The Lode angle parameter is defined as follows: