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INFLUENCE OF MICRO-DAMAGE ON RELIABILITY OF CRYOGENIC BELLOWS IN THE LHC INTERCONNECTIONS

C. Garion[1], B. Skoczen[*2]

Abstract

To achieve maximum beam energy in the LHC the accumulated length of the interconnections between LHC main magnets has been limited to around 3% of the total magnetic length in the Arcs and Dispersion Suppressors. Such a low ratio leads to a very compact design of components located in the LHC interconnections. This implies development and evolution of high intensity plastic strain fields in the stainless steel expansion bellows subjected to thermo-mechanical loads at low temperatures. These components have been optimised to ensure high reliability standards required for the LHC. Nevertheless, initial damage can occur and lead to a premature fatigue failure. For structures in which plasticity is not confined to the crack tip region, standard failure mechanics, based classically on the stress intensity factor or the strain energy density release rate, can not be used. In the present paper, a constitutive model taking into account plastic strain induced γ->α' phase transformation and orthotropic ductile damage is presented. This local approach is used to predict the impact of initial imperfections on the fatigue life of thin-walled LHC bellows expansion joints.

1 CERN, Accelerator Technology Department, Geneva, Switzerland
2 Cracow University of Technology, Krakow, Poland
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C. Garion[1], B. Skoczen[*2]

[1]CERN, European Organization for Nuclear Research
CH-1211 Geneva 23

[2]Cracow University of Technology
31-864 Krakow, Jana Pawla II 37 Ave, Poland

ABSTRACT

To achieve maximum beam energy in the LHC the accumulated length of the interconnections between LHC main magnets has been limited to around 3 % of the total magnetic length in the Arcs and Dispersion Suppressors. Such a low ratio leads to a very compact design of components located in the LHC interconnections. This implies development and evolution of high intensity plastic strain fields in the stainless steel expansion bellows subjected to thermo-mechanical loads at low temperatures. These components have been optimised to ensure high reliability standards required for the LHC. Nevertheless, initial damage can occur and lead to a premature fatigue failure. For structures in which plasticity is not confined to the crack tip region, standard failure mechanics, based classically on the stress intensity factor or the strain energy density release rate, can not be used. In the present paper, a constitutive model taking into account plastic strain induced $\gamma\rightarrow\alpha'$ phase transformation and orthotropic ductile damage is presented. This local approach is used to predict the impact of initial imperfections on the fatigue life of thin-walled LHC bellows expansion joints.

KEYWORDS: Stainless steel, ductile damage, crack propagation, thin walled bellows.

INTRODUCTION

The present paper is driven by an attempt to apply constitutive modeling of metastable stainless steels to thin walled structures subjected to high intensity plastic strain fields at cryogenic temperatures. This model describes texture induced orthotropic plasticity and

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damage in a two-phase continuum resulting from the process of plastic strain induced phase transformation at cryogenic temperatures. In the paper, the constitutive model is applied to evaluate fatigue life of LHC expansion bellows with initial damage.

Several classes of ductile materials are known to behave in a metastable way when cooled down or strained at very low temperatures. Among them the austenitic stainless steels are extensively used to construct pressure and vacuum vessels, components of the superconducting magnets, components of the cryogenic transfer lines and other structural members loaded in cryogenic conditions. Such material has also been used to manufacture thin walled bellows of the Large Hadron Collider interconnections [1]. Failure of these components is usually associated with ductile damage propagation and fatigue crack initiation. In the present paper three phenomena related to plastic strain evolution are taken into account and implemented:

Even if the chemical composition of stainless steel is tailored to avoid spontaneous phase transformation (from FCC (γ) austenitic phase to BCC (α') martensite), the material may become unstable under plastic deformation. Transformation kinetics has been initially described by Olson and Cohen [2]. The authors attributed the nucleation of martensite sites to the shear-band intersections (the shear bands were considered in the form of ε' martensite, mechanical twins or stacking-fault bundles).

A combination of the global strain and the strain induced by the local phase transformation creates favorable conditions for evolution of ductile damage induced by the plastic strain fields. Ductile damage represents the fields of micro-cracks and micro-voids, generated in the material after the plastic strain threshold has been reached (Fig.1). It is worth pointing out that in the case of two-phase materials both the inclusions and the matrix can generate their own damage fields. In particular, martensite platelets are known to be carriers of the so called short cracks [3]. Both the martensite platelets and the microcrack fields develop simultaneously and coexist in the same material volume (Fig. 1a, 1b). Since the evolution of damage, observed in thin-walled shells subjected to plastic straining at low temperatures shows the features that can not be explained by the isotropic model (circumferential cracks), an orthotropic model has been adopted.

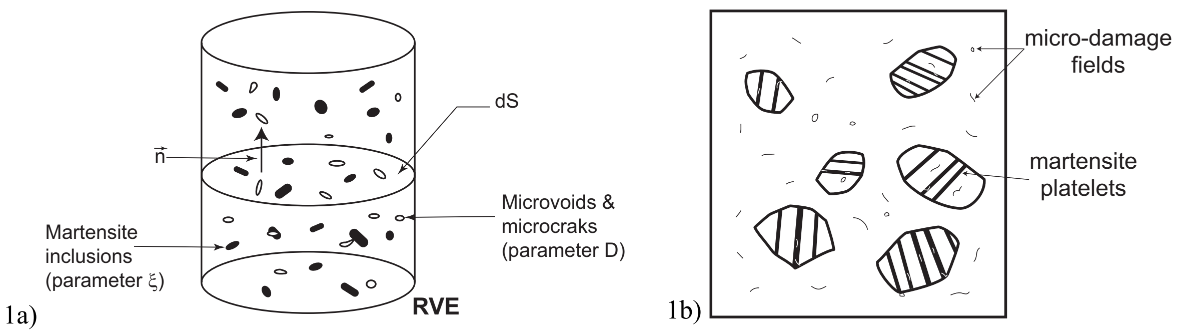

FIGURE 1 The Representative Volume Element (RVE) and a section of damage affected two-phase material

In the case of thin-walled structures one of the fundamental questions is related to the orthotropic properties of the laminated materials. The properties measured in two directions: parallel and orthogonal with respect to the axis of the lamination process, are different. Moreover, the process of hydroforming of thin shells of revolution (for instance expansion bellows) induces enhanced anisotropy and can not be treated by using an equivalent isotropic model. Therefore, a fully orthotropic description in terms of plastic strains and in terms of micro-damage propagation has to be introduced.

Here, the serrated yielding (discontinuous in terms of $d\sigma/d\varepsilon$) which occurs typically at 4.2 K (in liquid helium) and for a plastic strain rate above a given critical value (about 10^{-3} s^{-1}) [4; 5] is not taken into account.

CONSTITUTIVE MODEL OF STAINLESS STEEL AT CRYOGENIC TEMPERATURES WITH ANISOTROPY FEATURES

Plastic strain induced anisotropy

The texture induced anisotropy of plastic response is described by the following set of constitutive equations:

- The basic constitutive law

$$\underline{\underline{\sigma}} = \underline{\underline{\underline{E}}} : \underline{\underline{\varepsilon}}^{e} \tag{1}$$

where

$$\underline{\underline{\underline{E}}} = 3k\underline{\underline{\underline{J}}} + 2\mu\underline{\underline{\underline{K}}} \tag{2}$$

and

$$\begin{cases} J_{ijkl} = \frac{1}{3}\delta_{ij}\delta_{kl} \\ \underline{\underline{\underline{K}}} = \underline{\underline{I}} - \underline{\underline{\underline{J}}} \quad and \quad I_{ijkl} = \frac{1}{2}\left(\delta_{ik}\delta_{jl} + \delta_{il}\delta_{jk}\right) \end{cases} \tag{3}$$

where $\mu = \frac{E}{2(1+\nu)}$, $k = \frac{E}{3(1-2\nu)}$ are shear and bulk moduli, respectively; $\underline{\underline{\varepsilon}}^{e}$ stands for the elastic strain tensor.

- The yield surface is represented by:

$$f_c\left(\underline{\underline{\sigma}}, \underline{\underline{X}}, R\right) = \sqrt{\left(\underline{\underline{\sigma}} - \underline{\underline{X}}\right) : \underline{\underline{\underline{A}}} : \left(\underline{\underline{\sigma}} - \underline{\underline{X}}\right)} - \sigma_p \tag{4}$$

where $\underline{\underline{\underline{A}}}$ is the fourth order tensor that satisfies the condition $A_{ijkl} = A_{klij}$. The fundamental information about the texture induced anisotropy is hidden inside the tensor $\underline{\underline{\underline{A}}}$. It can be expressed in terms of the plastic strains (Eq. 5). The simpliest case is obtained when $a = 0$ which is equivalent to the Huber-Mises-Hencky yield condition (Eq. 6). Here $\underline{\underline{s}}$ denotes the deviatoric stress. Tensor $\underline{\underline{X}}$ represents the back stress and σ_p stands for the yield stress. The yield stress can be expressed in terms of the initial yield point σ_0 and the isotropic hardening variable *R*.

$$\underline{\underline{\underline{A}}} = \frac{3}{2}\underline{\underline{\underline{K}}} + a\underline{\underline{\varepsilon}}^{p} \otimes \underline{\underline{\varepsilon}}^{p} \tag{5}$$

$$f_c\left(\underline{\underline{\sigma}}, \underline{\underline{X}}, R\right) = \sqrt{\frac{3}{2}\left(\underline{\underline{s}} - \underline{\underline{X}}\right) : \left(\underline{\underline{s}} - \underline{\underline{X}}\right)} - \sigma_p \tag{6}$$

- The associated flow rule:

$$d\underline{\underline{\varepsilon}}^{p} = d\lambda \frac{df_c}{d\underline{\underline{\sigma}}} = d\lambda \underline{\underline{n}} = \frac{\underline{\underline{\underline{A}}} : \left(\underline{\underline{\sigma}} - \underline{\underline{X}}\right)}{\sqrt{\left(\underline{\underline{\sigma}} - \underline{\underline{X}}\right) : \underline{\underline{\underline{A}}} : \left(\underline{\underline{\sigma}} - \underline{\underline{X}}\right)}} d\lambda \tag{7}$$

- The mixed hardening law comprising the kinematic and the isotropic hardening.

Evolution of the kinematic and the isotropic hardening variables is expressed in terms of the plastic strain increment and the Odqvist parameter, respectively:

$$d\underline{\underline{X}} = \frac{2}{3} C_X d\underline{\underline{\varepsilon}}^p \tag{8}$$

$$dR = C_R dp \tag{9}$$

where C_X, C_R are functions of the current state variables. Here the increment of the Odqvist parameter is defined by:

$$dp = \sqrt{\tfrac{2}{3} d\underline{\underline{\varepsilon}}^p : d\underline{\underline{\varepsilon}}^p} = d\lambda \tag{10}$$

As an example, the influence of the parameter a on the yield surface is analysed under biaxial test conditions. To show better the impact of the parameter, only linear kinematic hardening is considered (C_R=0). The stress tensor reads:

$$\underline{\underline{\sigma}} = \begin{pmatrix} \sigma_1 & 0 & 0 \\ 0 & \sigma_2 & 0 \\ 0 & 0 & 0 \end{pmatrix} \tag{11}$$

Proportional and non-proportional loading path are considered. The first one is characterized by:

$$\frac{\sigma_1}{\sigma_2} = \frac{700}{-250} \tag{12}$$

The initial yield surface of the virgin material without hardening is defined by a yield stress, σ_0, equal to 500MPa. For both loading cases, the plastic flow is active once the initial yield surface is "touched". Influence of the parameter *a* is shown by high value of the parameter, equal to 10.

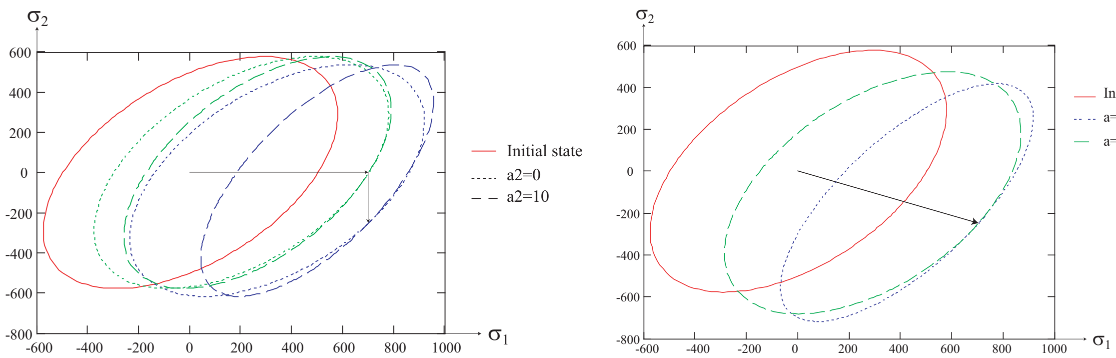

FIGURE 2 Yield surface for anisotropic model under non-proportional and proportional loading.

It turns out that anisotropic plastic flow leads to a substantial reduction of the elastic domain (Fig. 2). The yield surface remains an ellipse with orientation depending on the loading path.

Modelling the plastic strain induced martensitic transformation (γ→α')

Kinetic law of martensitic transformation

Kinetics of γ→α' phase transformation, as described in [3], is reflected by a typical sigmoidal curve defining evolution of martensite content (ξ) as a function of plastic strain.

A simplified evolution law for the volume fraction of martensite has been introduced for the linear part (region II) of the sigmoidal curve by Garion and Skoczen [6]:

$$d\xi = A(T)\, dpH\big((p - p_{\xi})(\xi_L - \xi)\big) \tag{13}$$

where *dp* denotes the accumulated plastic strain increment. p_{ξ} denotes the accumulated plastic strain threshold to initiate the phase transformation and ξ_L stands for the saturation level of the martensite content. H is the Heavyside function.

Bi-phase initially isotropic and homogenous material – constitutive model

For the bi-phase (γ+α') isotropic and ductile material [6, 7] the constitutive law has been modified as follows:

$$\underline{\underline{\sigma}} = \underline{\underline{\underline{E}}} : \left(\underline{\underline{\varepsilon}} - \underline{\underline{\varepsilon}}^{p} - \underline{\underline{\varepsilon}}^{th} - \xi \underline{\underline{\varepsilon}}^{PT}\right) \tag{14}$$

where $\underline{\underline{\varepsilon}}^{PT}$ denotes the phase transformation strain and $\underline{\underline{\varepsilon}}^{th}$ stands for the thermal strain.
The evolution law for the mixed kinematic and isotropic hardening of two phase material is obtained from the Mori-Tanaka homogenization [6, 7] and is expressed by:

$$d\underline{\underline{X}} = \frac{2}{3} C_X d\underline{\underline{\varepsilon}}^{p} = \frac{2}{3} C(\xi) d\underline{\underline{\varepsilon}}^{p} + G(\xi) d\underline{\underline{\varepsilon}}^{p} \tag{15}$$

$$dR = C_R dp = b(\xi)(1-\beta)(R_{\infty}(\xi) - R)dp \tag{16}$$

Orthotropic ductile damage

Second order damage tensor: $\underline{\underline{D}}$

Assume at a given point a local set of unit base vectors $\vec{n}_i$, tangent to the principal directions. The relevant damage tensor is introduced in the form [8]:

$$\underline{\underline{D}} = \sum_{i=1,3} D_i \vec{n}_i \otimes \vec{n}_i \tag{17}$$

where $\vec{n}_i$ stands for the base vector associated with the principal direction *i* and D_i denotes the component of damage tensor related to direction *i*. It is defined by:

$$D_i = \frac{dS_{D_{\vec{n}_i}}}{dS_{\vec{n}_i}} \tag{18}$$

where $S_{D_{\vec{n}_i}}$ is the area of damage in the section $S_{\vec{n}_i}$, represented by the normal $\vec{n}_i$.

The effective stress: $\underline{\underline{\tilde{\sigma}}}$

The effective stress $\underline{\underline{\tilde{\sigma}}}$ is supposed to obey the strain equivalence principle [9]:

$$\underline{\underline{\tilde{\sigma}}} = \underline{\underline{\underline{E}}} : \underline{\underline{\varepsilon}}^{e} \tag{19}$$

The relation between the stress and the effective stress tensors is postulated under the following form:

$$\underline{\underline{\sigma}} = \frac{1}{2}\left(\left(\underline{\underline{I}} - \underline{\underline{D}}\right)\underline{\underline{\tilde{\sigma}}} + \underline{\underline{\tilde{\sigma}}}\left(\underline{\underline{I}} - \underline{\underline{D}}\right)\right) \tag{20}$$

with $\underline{\underline{I}}$ being the identity tensor. The general relationship between the stress and the effective stress tensors is given Eq. 21. In the same way, the effective back stress tensor is introduced (Eq. 22).

$$\underline{\underline{\tilde{\sigma}}} = \underline{\underline{\underline{\underline{M}}}}^{-1} : \underline{\underline{\sigma}} \tag{21}$$

$$\underline{\underline{\tilde{X}}} = \underline{\underline{\underline{\underline{M}}}}^{-1} : \underline{\underline{X}} \tag{22}$$

Kinetic laws of damage evolution for orthotropic ductile materials

It is assumed that the driving force of evolution of orthotropic ductile damage remains the accumulated plastic strain. Thus, the kinetic law of damage evolution is postulated as an extrapolation of isotropic law in the following form [8]:

$$d\underline{\underline{D}} = \underline{\underline{C}}\,\underline{\underline{Y}}\,\underline{\underline{C}}^{T} dpH\left(p - p_D\right) \tag{23}$$

Tensor $\underline{\underline{C}}$ is defined by Eq. 24. It can be classified as the symmetric tensor containing the material moduli. The strain energy density release rate tensor is defined from the Helmholtz free energy potential, ψ:

$$\underline{\underline{C}} = \sum_{i=1,3} C_i \vec{n}_i \otimes \vec{n}_i \tag{24}$$

$$\underline{\underline{Y}} = -\rho \frac{\partial \Psi}{\partial \underline{\underline{D}}} \tag{25}$$

COMBINED SET OF EQUATIONS FOR A TWO-PHASE MATERIAL WITH PLASTIC ANISOTROPY AND DUCTILE ORTHOTROPIC DAMAGE

The final set of the constitutive equations for a two-phase heterogeneous material with orthotropic ductile damage takes the following form:

$$d\xi = A(T)\, dpH\left(\left(p - p_\xi\right)\left(\xi_L - \xi\right)\right) \tag{26}$$

$$d\underline{\underline{D}} = \underline{\underline{C}}\,\underline{\underline{Y}}\,\underline{\underline{C}}^{T} dpH\left(p - p_D\right) \tag{27}$$

$$\underline{\underline{\tilde{\sigma}}} = \underline{\underline{\underline{\underline{E}}}} : \left(\underline{\underline{\varepsilon}} - \underline{\underline{\varepsilon}}^{p} - \underline{\underline{\varepsilon}}^{th} - \xi \underline{\underline{\varepsilon}}^{PT}\right) \tag{28}$$

$$\underline{\underline{\sigma}} = \frac{1}{2}\left(\left(\underline{\underline{I}} - \underline{\underline{D}}\right)\underline{\underline{\tilde{\sigma}}} + \underline{\underline{\tilde{\sigma}}}\left(\underline{\underline{I}} - \underline{\underline{D}}\right)\right) \tag{29}$$

$$\underline{\underline{X}} = \frac{1}{2}\left(\left(\underline{\underline{I}} - \underline{\underline{D}}\right)\underline{\underline{\tilde{X}}} + \underline{\underline{\tilde{X}}}\left(\underline{\underline{I}} - \underline{\underline{D}}\right)\right) \tag{30}$$

$$f_c\left(\underline{\underline{\tilde{\sigma}}}, \underline{\underline{\tilde{X}}}, R\right) = \sqrt{\left(\underline{\underline{\tilde{\sigma}}} - \underline{\underline{\tilde{X}}}\right) : \underline{\underline{\underline{\underline{A}}}} : \left(\underline{\underline{\tilde{\sigma}}} - \underline{\underline{\tilde{X}}}\right)} - \sigma_y - R \tag{31}$$

$$d\underline{\underline{\varepsilon}}^{p} = \frac{\partial f_c}{\partial \underline{\underline{\sigma}}} d\lambda \tag{32}$$

$$d\underline{\underline{X}} = \frac{2}{3} C_X(\xi) d\underline{\underline{\varepsilon}}^p \tag{33}$$

$$dR = C_R(\xi) dp \tag{34}$$

APPLICATION TO THIN-WALLED CRYOGENIC BELLOWS

The constitutive model has been implemented in a FE code (CASTEM2000). In case of active plastic process, the equations are integrated by using Ortiz and Simo algorithm [11]. It has been applied to thin-walled stainless steel corrugated shells, known as bellows expansion joints. They are used as compensation elements in the interconnections of the Large Hadron Collider at CERN. The model has been used to determine the evolution of damage in normal service conditions but also to evaluate the impact of initial damage on the fatigue life. This is a critical issue since damage may occur during the installation phase of large project such as LHC. Main parameters have been identified for 316L grade in liquid nitrogen and liquid helium environment [7]. As an example, a half convolution of a critical U-type bellows has been considered. An axisymmetric model has been used. This bellows is applied in the beam lines and is manufactured from 0.15 mm fine gauge stainless steel. Typical thickness at the convolution crest is around 0.11 mm. Moreover it corresponds to the location where the bellows is usually damaged during assembly due to bulges, small groves and scratches... The bellows has been subjected to thermomechanical cycles between room temperature and 2 K associated to an axial displacement cycle between -20% and +54% of its initial length (- denotes compression and + denotes extension). An initial crack is considered on the external face at the convolution crest. It is simulated by relaxing axially the nodes on the crack lip. Thus the crack can open itself under tensile stress. Unilateral boundary conditions are implemented to allow the crack closure. Critical damage corresponding to the crack propagation has been defined at the level of 0.8. The damage evolution across the thickness is determined as a function of number of cycles for different initial crack length, as shown in Fig. 4.

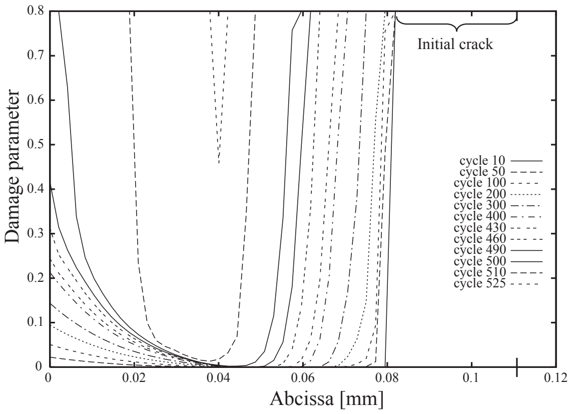

FIGURE 4 Damage evolution across the wall thickness under cyclic loading

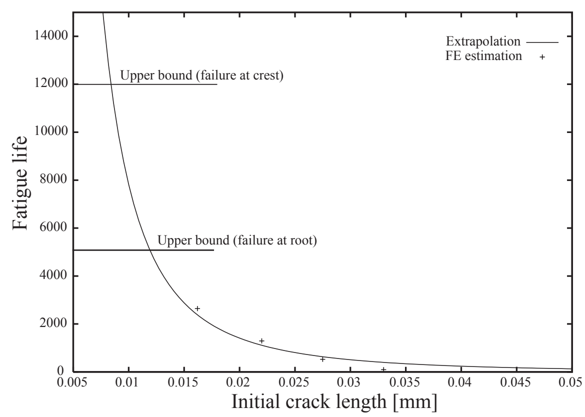

FIGURE 5 Fatigue life of cryogenic bellows as a function of the initial crack length

From this analysis, the crack propagation velocity has been evaluated as a function of the crack length, c_l. The equation is postulated in the following form:

$$v = \frac{dc_l}{dN} = \alpha c_l^{\ \beta} \tag{35}$$

The fatigue life is determined by integration as a function of the initial crack length, as illustrated in Fig. 5 at room temperature. Two upper bounds are defined if no initial damage is considered. A relative discrepancy can be observed for high initial crack length due to the influence of damage on the internal face of the ply. It is worth pointing out that the fatigue is highly sensitive with respect to the initial damage. In this case (type of bellows and loading), initial damage less than about 10% of the thickness (small scratch) doesn't lead to major fatigue life reduction.

CONCLUSIONS

In the present paper, a constitutive model of ductile material for low temperature applications has been developed taking into account plastic strain induced austenite to martensite phase transformation, ductile damage and plastic anisotropy. A simplified linear evolution law has been used for the $\gamma \rightarrow \alpha'$ phase transformation. In order to describe correctly the evolution of damage in thin-walled plates and shells a second order damage tensor has been introduced. The orthotropic law is a generalization of the well known isotropic law of damage evolution, introduced by Chaboche and Lemaitre [11, 12]. The damage rate is driven by the accumulated plastic strain rate. The plastic anisotropy has been introduced via the yield surface formulation as the texture induced anisotropy.
The model has been implemented in a finite element code to simulate the crack propagation in a thin walled shell subjected to high plastic strains. Great interest of the method for thin walled shells consists in the fact that the stress redistribution is taken into account when the crack propagates. The model has been used to analyze the impact of an initial damage on the reliability of the LHC cryogenic bellows expansion joints.

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