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Creep fatigue in extrusion dies – modelling and simulation

During hot extrusion of aluminium alloys, extrusion tools experience cyclic temperature changes as well as multiaxial loading. To improve the service life of the tools, cleaner materials with advanced hot strength are designed and optimised process control is performed. For the improvement of process guiding and a comparison of lifetime behaviour of different hot work tool steels, modelling and simulation are appropriate means. The extrusion process for both a circular solid shape and a complex section was simulated with a finite element programme to find the stress and temperature history within the dies and additionally, for the complex section, to define the boundary conditions for a subsequent cyclic simulation of the die loads during service. Inelastic constitutive equations were implemented into a FEM code to describe the strain hardening and time recovery effects. They include a thermo-mechanical isotropic hardening and two non-linear kinematic hardening laws. A damage-rate model predicts failure and thus the lifetime of the extrusion die.

Extrusion tools exhibit a complex strain-time pattern under a variety of cyclic loading conditions and thus are prone to failure due to creep-fatigue interactions [1; 2]. Elevated temperature failure by creep-fatigue processes is time dependent and often involves deformation path influenced interactions of cracks with grain boundary cavities [3]. The extrusion industry tries to speed up the process by accelerating the press speed in dependence on the billet temperature that raises the loading of the tools. In addition, tool steel producers develop more homogeneous and cleaner materials in order to increase tool lifetime. Finite element method (FEM) simulations of the extrusion process calculate the temperature and stress evolution in the tools. They are coupled with constitutive equations as well as with a lifetime consumption model in order to receive both the inelastic strains and the tool lifetime, and thus help to optimise the extrusion process and to compare the feasible operating time of different hot work tool steels.

Viscoplastic constitutive models were developed in the past to take into account the inelastic behaviour of the material during creep-fatigue loads, see e. g. [4...6]. In the present study the Chaboche model was selected and calibrated to the material response of EN 1.2343 hot work tool steel supplied by Böhler, between 470 °C and 590 °C [7]. To extend the prediction capability of Chaboche's model for non-isothermal processes, a temperature-rate term was added to the isotropic hardening rule [8]. Additionally, a creep-fatigue lifetime rule for complex processes that is independent of single loading parameters like stress and strain ranges was used for the description of an entire cycle. Instead this rule evaluates the total damage in each time increment and accumulates that with the lifetime consumption.

The present paper shows the development of temperatures, stresses and lifetime consumption in the die during three extrusion cycles of an AA6082 aluminium alloy for a circular solid shape as well as for a more complex extrusion geometry.

Modelling and simulation

The numerical extrusion simulation consists of the plastic simulation of the billet extrusion with rigid tools as well as of the subsequent simulation of several cycles of the same process, only considering the elastic die and using the dependent temperature and pressure boundary conditions already determined at the contact surface billet-die.

Extrusion model. To predict damage, the accurate knowledge of the unsteady local thermal and mechanical loading on the die within one cycle is of particular importance. Hence, during extrusion this load of the die of both a circular solid shape and of a more complex extrusion section was simulated by means of FEM.

A circular solid shape. Here, the extrusion process was simulated over three cycles. The axisymmetric model, realised by Deform-2D, consists of an elastic die and a deformable billet as well as of a rigid container and pressing pad, **fig. 1**. The purpose of this simple model was to test the implemented constitutive as well as lifetime consumption model. The initial temperature was set to 590 °C for the die and to 400 °C for the billet. The relatively high initial temperature of the die was chosen in order to cause a noteworthy damage per cycle. However, lower more realistic initial temperatures of the die would lead to very low damage rates due to relatively low stress amplitudes per cycle resulting from the simple die geometry. The extrusion process was controlled by the displacement of the extrusion die. To get significant data for the damage model three extrusion cycles were simulated. At the beginning of each cycle the temperatures for the billet and die were preset to the start conditions described above.

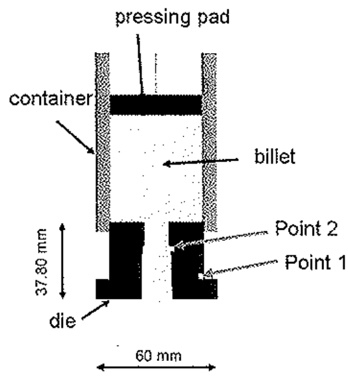

Fig. 1: Axisymmetric simulation model of the extrusion process of a simple solid shape (Deform-2D)

A tubular extrusion section. For this example, the thermo-mechanical load of the die during extrusion of a tubular extrusion geometry was analysed by means of the finite element method, where the die was assigned a rigid behaviour. The output of these simulations was the time-

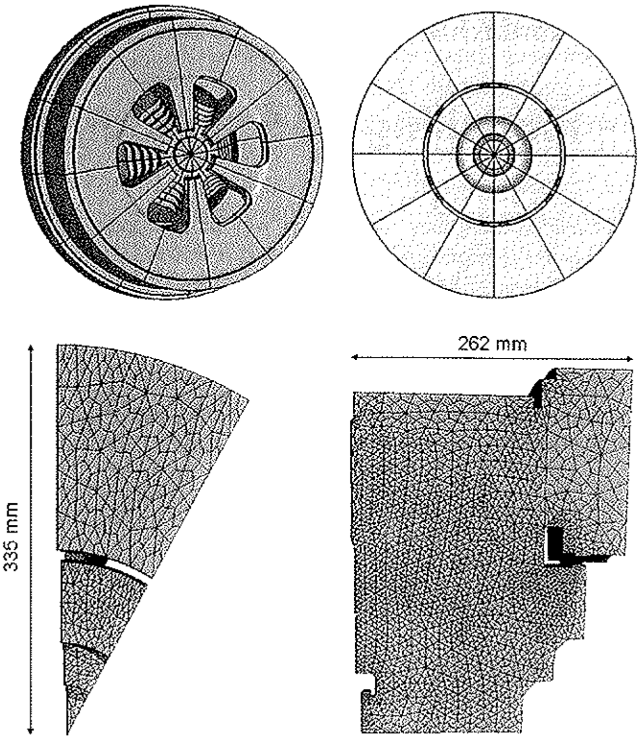

Fig. 2: Aluminium extrusion die for tubes (top) and modelled part with a symmetry of 30° (bottom)

dependent temperature boundary conditions at the contact surfaces billet-die during one cycle in the steady state regime. The reason for this procedure is the much shorter calculation time for the elastic die model with specified boundary conditions in comparison to the plastic extrusion process, especially for several extrusion cycles. Due to missing stress boundary conditions, the time-dependent pressure conditions normal to the contact surfaces billet-die were assumed as 100 MPa.

For the calculation of the cyclic temperature and stress evolution in the die, Abaqus Standard v.6.5-1 FEM calculations were conducted with an elastic die, using 50000 continuum elements of type C3D8. The extrusion die was modelled with a symmetry of 30°, **fig. 2**. The necessary contact conditions, such as friction, interface heat transfer between the extrusion tools as well as the boundary conditions convection, emissivity, were described by using existing data. The time increment chosen had to be relatively small in order to handle large stress and thus damage gradients with time; here a value of 0.25 s was fixed. Within each extrusion cycle, which consists of a pressure time of 300 s and a loading time of 300 s, both the heat of the billet and the pressure were applied by the above defined boundary conditions at the contact surface billet-die. **Fig. 3** displays both the stress (a) and the temperature distribution (b) at the end of the third extrusion cycle. The maximum temperature of approx. 538 °C appears near the contact area billet-die, and the maximum Mises equivalent stress also has its maximum at 723 MPa in this area of relatively high temperature.

Model for the deformation behaviour. A viscoplastic model was used according to Chaboche [6], where the total strain $\boldsymbol{\varepsilon}$ is taken to be composed of elastic ε_e, thermal ε_{th} as well as inelastic ε_{in} parts

$$\boldsymbol{\varepsilon} = \boldsymbol{\varepsilon}_e(\boldsymbol{\sigma}) + \boldsymbol{\varepsilon}_{in} + \boldsymbol{\varepsilon}_{th}(T), \quad \boldsymbol{\varepsilon}_{th}(T) = \varepsilon_{th}(T)\cdot \mathbf{1} \quad (1)$$

and Hook's law is given by

$$\boldsymbol{\sigma} = 2G\,\boldsymbol{\varepsilon}_e' + \frac{E}{3(1-2\nu)}\,\mathrm{tr}\,\boldsymbol{\varepsilon}_e \cdot \mathbf{1} \quad (2)$$

with G denoting the shear modulus, E the Young's modulus, ν the Poisson's ratio and the deviator of the elastic strain tensor $\boldsymbol{\varepsilon}_e'$.

For the lifetime prediction of highly stressed extrusion tools during service, taking into account the inelastic strain

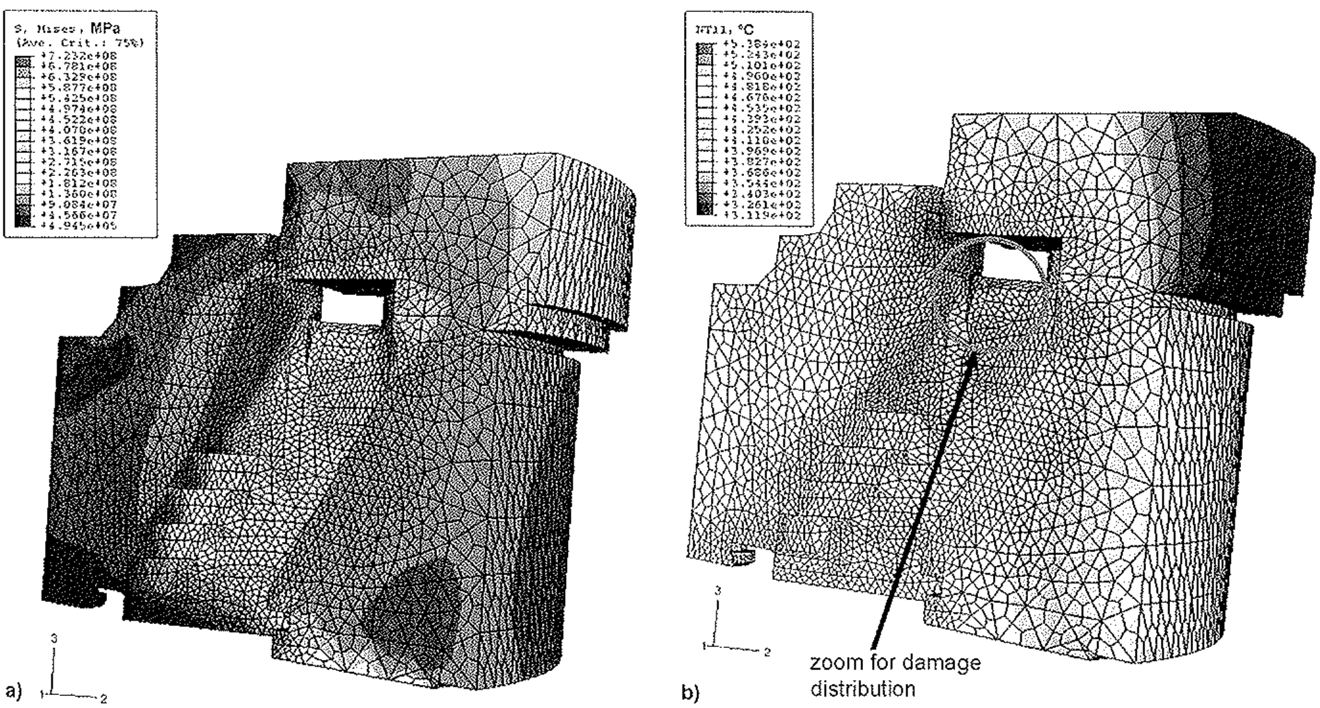

Fig. 3: Equivalent Mises stress (a) as well as temperature distribution (b) at the end of the 3rd press cycle

rate during a cycle, it is necessary to be able to assess the inelastic stress-strain response of the material [7; 9]. The influence of the thermomechanical history on the current stress-strain behaviour can be described with internal, i.e. non-measurable variables, besides the measurable external variables of deformation, time, temperature and stress [5]. The evolution equations for the internal variables are given by flow and hardening rules. In viscoplastic, i. e. unified inelastic, models, creep and plasticity are covered within a single inelastic strain variable in order to describe creep-plasticity interaction. The flow rule, i. e. the evolution equation for the inelastic strain is [6]

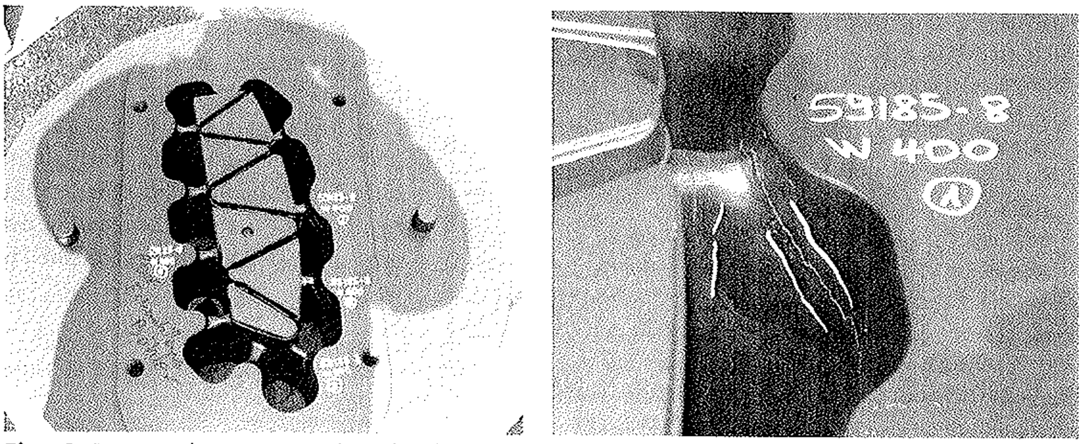

Fig. 4: Damaged extrusion tool made of hot work tool steel W400VMR (left) and macro-cracks in the bridges (right)

$$\dot{\varepsilon}_{in} = \frac{3}{2}\left\langle \frac{J_2(S-X)-(k+R)}{K} \right\rangle^n \frac{S-X}{J_2(\mathbf{S}-\mathbf{X})},$$
$$\langle y \rangle := \begin{cases} y, \text{if } y > 0 \\ 0, \text{otherwise} \end{cases} \qquad (3)$$

with the applied stress deviator $\mathbf{S}$ and specifying k as the initial elastic limit, R as the increase of the elastic limit due to hardening, $\mathbf{X}$ as the internal back stress tensor, describing kinematic hardening and K as a material parameter. Olschewski et al. [8] have proposed a certain type of a thermo-mechanical evolution equation

$$\dot{R} = Q\dot{r} + \frac{R}{Q}\frac{dQ}{dT}\dot{T} \qquad (4)$$

for the isotropic hardening variable $R = Q(T)\cdot r$ in order to describe non-isothermal material behaviour, T denotes the temperature with Q as the saturation parameter of R at isothermal loading and r as the related isotropic hardening variable with the evolution equation

$$\dot{r} = b\left(1-\frac{R}{Q}\right)\dot{p} - \frac{f}{Q}\left(\frac{R}{Q}\right)^s$$
$$r(t=0)=0, \qquad \dot{p} := \sqrt{\frac{2}{3}}\,\|\dot{\varepsilon}_{in}\| \qquad (5)$$

where b, f and s are material parameters adapting the isotropic hardening and static recovery, respectively, and $\dot{p}$ is the inelastic Mises equivalent strain-rate.

The rate equations for the kinematic hardening variables obey a unique format. The back stress $\mathbf{X}$ is decomposed into independent variables $\mathbf{X}_i$, each of them obeying the same rule. As shown in previous studies, e. g. Watanabe and Atluri [10], two or three of such variables are sufficient to describe, very correctly, the real materials. In this work two independent variables were chosen:

$$\mathbf{X} = \mathbf{X}_1 + \mathbf{X}_2, \quad \mathbf{X}_i = \frac{2}{3}a_i(T)\boldsymbol{\alpha}_i, \quad i=1,2 \quad (6)$$

where $a_i\,(T)$ are saturation parameters of the internal back-stresses $\mathbf{X}_i$, and $\boldsymbol{\alpha}_i$ are related kinematic hardening variables:

$$\dot{\boldsymbol{\alpha}}_i = c_i\,\dot{\varepsilon}_{in} - \frac{3}{2}c_i\frac{\mathbf{X}_i}{a_i}\dot{p}$$
$$-\frac{3}{2}\frac{d_i}{a_i}\left(\frac{J_2(\mathbf{X}_i)}{a_i}\right)^{m_i}\frac{\mathbf{X}_i}{J_2(\mathbf{X}_i)}, \quad \boldsymbol{\alpha}_i(t=0)=0 \quad (7)$$

with c_i, d_i and m_i as material parameters defining the kinematic hardening and the static recovering, respectively.

The related hardening variables r and $\boldsymbol{\alpha}_i$ describe the degree of hardening that corresponds in the material structure to the accumulation of immobile dislocations, compare e. g. Ilschner [11], and that causes certain internal stresses $k + R$ and $\mathbf{X}_i$, respectively, at a certain temperature. For example, the isotropic variable r tends, at negligible static recovery, according to eq. (5) for any temperature to its saturation value 1. Similar is the case for the kinematic variables $\boldsymbol{\alpha}_i$ according to eq. (7) under proportional loading. Thus, the internal stresses $k + R$ and $\mathbf{X}_i$ vary under temperature changes according to eqs. (4) and (6) at most to their saturation levels (Q and $2/3\ a_i$) at the current temperature, compare Olschewski et al. [8] and Frenz et al. [9].

All thermo-physical and material parameters are temperature-dependent and were determined for temperatures in the range of 470 °C to 590 °C with 30 °C temperature steps. For a reduction of the number of parameters, the material parameters of the thermal, so-called static recovery of the internal back-stresses $\mathbf{X}_i$ are set equal: $d_1 = d_2 =: d$, $m_1 = m_2 =: m$. With respect to the number of material parameters to be determined it can be said, that as many material parameters can be identified as material phenomena can be observed in the experiments. Such phenomena of the hardening and recovery behaviour together with the corresponding material parameters to be identified especially from these phenomena are listed in detail in [7].

Strain-controlled tensile tests with subsequent strain holding-time, i. e. stress relaxation, periods of several hours (up to 40 h) were performed at different temperatures [7] in order to determine the parameters of the primary rate-dependence as well as of the static recovery. During relaxa-

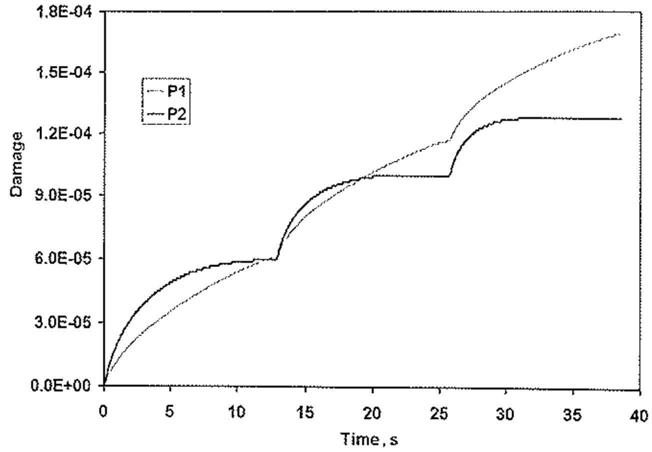

Fig. 5: Evolution of the lifetime consumption of two points P1 and P2 (see fig. 1) with comparatively high equivalent stresses as well as temperatures in the die

tion several magnitudes of the inelastic strain-rate (creep rates) occur, which decrease down to 10^{-8} s^{-1} with the decreasing magnitude of the stress-rate. For the determination of the hardening parameters, hysteresis loops of strain-controlled cyclic tests without hold-times were evaluated [7].

A lifetime rule for complex processes. Cyclically loaded structures suffer a fatigue failure. Fatigue lifetime means in a macroscopic model the initiation of a macro-crack (typically a fraction of a millimetre). **Fig. 4** shows a corresponding damaged extrusion tool with cracks appearing in the bridges of the die. Fatigue lifetime rules are usually formulated on the basis of mean quantities of a cycle, like stress or strain ranges, see e. g. Chaboche and Gallerneau [13]. In contrast, time incremental lifetime rules as given by Danzer [14], Sermage et al. [15], Yeh and Krempl [16], Levkovitch et al. [17], evaluate the total damage in each time increment and, thus, can be applied also to complex multiaxial loading paths, for which the definition of a single loading parameter describing the entire cycle could be difficult. Furthermore, a time incremental lifetime rule can easily be implemented into a material sub-routine for finite element analysis of structures just as an evolution equation for an additional internal variable, the lifetime consumption D, $0 \le D \le 1$. The following lifetime rule was used:

$$\frac{\mathrm{d}D}{\mathrm{d}t} = \left(\frac{\sigma_{\mathrm{eq}}}{A}\right)^{m_l} \left(\frac{\dot{p}}{\dot{p}_0}\right)^{n_l} \dot{p}_0, \quad \sigma_{\mathrm{eq}} := \sqrt{\frac{3}{2}}\,\|\mathbf{S}\| \quad (8)$$

where σ_{eq} is the Mises equivalent stress, $\dot{p}$ the inelastic Mises equivalent strain-rate as defined in eq. (5) and $\dot{p}_0$ is a normalisation constant. The material parameters A and m_l describe the stress-dependence of the lifetime behaviour. Influence of the mean stress of a cycle is taken into account automatically by the fact that a stress process, which is non-symmetric to zero-point in the stress space during a cycle, nevertheless moves for the same stress range as in a symmetric process at higher stress magnitudes. The parameter n_l describes the time-dependence of the lifetime: For rate-independent behaviour n_l is equal to 1, n_l equal to zero means that a fully time-dependent lifetime behaviour is present. n_l was found to be positive but significantly lower than 1 for the investigated high temperature loading.

The parameters A and m_l were determined from LCF tests with strain-rates of 10^{-3} s^{-1} without hold-times and on the basis of the third cycle because at the investigated high temperatures no saturation of the hysteresis appeared. The parameter n_l was identified by the influence of hold-times in LCF tests on the lifetime behaviour. The cycles-to-failure N_f of the LCF experiments were determined at 2 % stress drop in tension and were calculated by the formula

$$N_f \approx 1/(\Delta D)_3 \quad (9)$$

where $(\Delta D)_3$ is the lifetime consumption within the third cycle.

Simulation results

Circular solid shape. In this example, a simple die geometry was modelled in order to test the constitutive as well as damage model that were coupled to the FEM software Deform-2D. Maximum damage occurred in regions with relatively high values of both temperature and equivalent stress amplitudes, typically in 90° angles and next to thickness changes. Therefore the evolution of the lifetime consumption of two corresponding points are shown in **fig. 5**. The calculated cycles-to-failure of the die are approx. 20000 for point 1 and about 33000 for point 2. These results seem to be reasonable in comparison to real lifetime of more complex aluminium extrusion dies. However, for typical initial die temperatures for aluminium extrusion, i. e. approx. 500 °C instead of the 590 °C applied here, the calculated lifetime for the simple die geometry would increase by a factor of circa 10.

Tubular extrusion section. Here, the simulations led to maximum lifetime consumption in the region of relatively high level of temperature and equivalent stresses, **fig. 6**. During extrusion, the equivalent stress and temperature maxima are not located at exactly the same place in the die. However, the largest accumulated damage occurs in regions that exhibit maximum overlapping temperature and equivalent stress loading. **Fig. 7** depicts the lifetime consumption evolution with time for 3 extrusion cycles. The calculated cycles-to-failure of the die are approx. 10000. These results seem to be reasonable in comparison to real lifetime of aluminium extrusion dies. This must also be judged regarding the fact that no measured stress data were available to be used as boundary conditions as mentioned above.

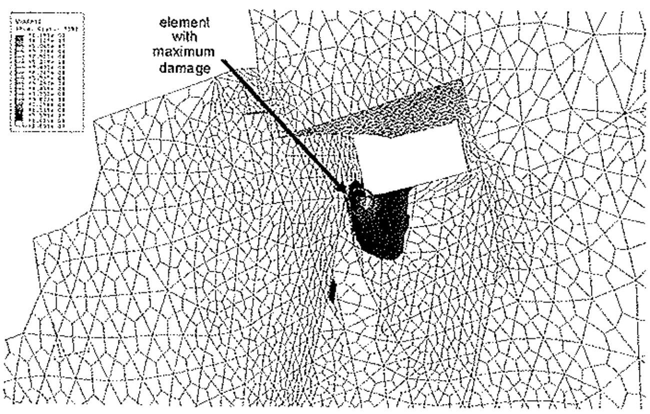

Fig. 6: Damage distribution at the end of the 3rd press cycle (zoomed area in fig. 3b)

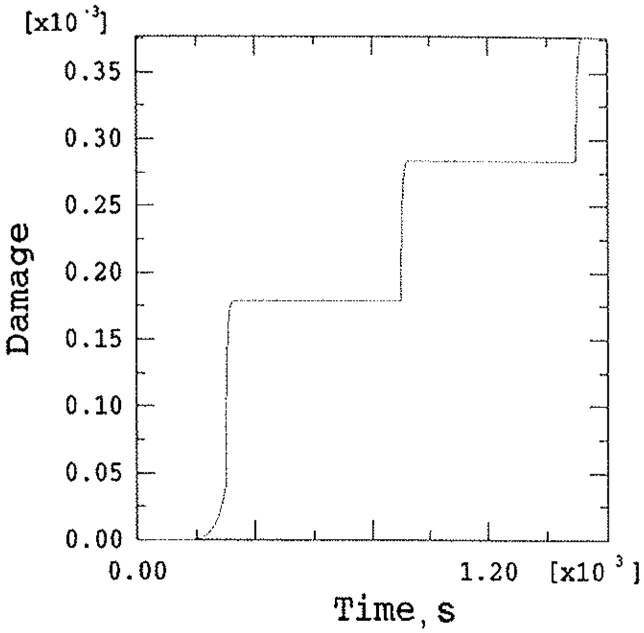

Fig. 7: Lifetime consumption evolution of the corresponding element with maximum damage (see fig. 6)

Conclusions and outlook

A thermo-viscoplastic constitutive model for the calculation of inelastic strains due to creep-fatigue loads in extrusion dies made of hot work steels was presented. A creep-fatigue lifetime rule for complex multiaxial loading was used that is independent of single loading parameters to describe an entire cycle. Instead, this lifetime rule evaluates the total damage in each time increment and, thus, can easily be implemented in a material sub-routine for finite element analysis of structures just as an evolution equation for an additional internal variable, the lifetime consumption.

Two models were investigated. To test both the constitutive and damage model, coupled with the FEM calculations, an axisymmetric circular solid shape extrusion was simulated. As a more complex example, the lifetime of an extrusion die for a tubular extrusion section was calculated. Therefore the extrusion process was simulated in 3D in order to get thermo-mechanical boundary conditions for a subsequent cyclic simulation of the temperature and stress evolution in the die. For both examples, the largest accumulated damage occurred in regions with disadvantageous overlapping of creep and fatigue, i. e. maximum overlapping temperature and equivalent stress loading. Hence cracking was computed to appear in 90° angles and next to thickness changes (circular solid shape) as well as at the bridges of the die (tubular extrusion section) at cycles-to-failure that seemed reasonable in comparison to real lifetime of aluminium extrusion dies.

In future, the calculated temperature and stress distribution will be compared to measured values with the help of an extrusion test device. Additionally, both the impact of temperature and press speed changes on tools lifetime shall be analysed and different hot work tool steels will be compared related to lifetime.

Acknowledgements

The authors would like to thank the group of Dr.-Ing. Klingelhöffer of the division V.2 *Mechanical Behaviour of Materials* of the Federal Institute for Materials Research and Testing (BAM) very much for performing the tests. Additionally the plastic FEM simulation of the extrusion process to get the temperature boundary conditions on the contact surface billet-die, done by Dr. Gaudin of Alcan Aluminium Valais SA in Sierre is greatfully acknowledged.

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