Extraction of material parameters for the simulation of heat treatments of cast aluminium alloys through unified models

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The objective of this work, which is part of the IDEAL (Integrated Development Routes for Optimized Cast Aluminium Components) project, financed by the EU in frame work 6 and born in collaboration with the automobile and foundry industries, is to simulate creep behavior of aluminum cast samples subjected to high temperature. In this paper a two-state variables unified model is applied in order to simulate creep behavior and time-dependent metallurgical changes. The fundamental assumption of the unified theory is that creep and viscoplasticity, which are both irreversible strains developed because of dislocations motion in the material structure, can be modelled through the implementation of a similar plastic strain velocity law, generally called flow rule. The paper shows how to obtain the material data needed for the simulation of the stress-strain behavior of aluminum at high temperature. As an example, the analysis of several tests performed at various temperatures and strain rates on a particular aluminum alloy, is presented as well. Furthermore, the one dimensional code developed during this project is illustrated and a simulation is run using the material data obtained through the mentioned experimental study. The results obtained for the simulation of tensile tests and of creep tests are compared with experimental curves, showing a good agreement.

Keywords: heat treatments, aluminium and alloys, modeling

INTRODUCTION

In order to satisfy the growing need in high quality aluminum cast parts of the automobile industries, in the last decades the foundries have been showing an increasing interest in the implementation of numerical simulations as part of their process design. As a consequence, it is possible to affirm that simulations for calculation of the mold filling and solidification have reached a very high level of development. However, the results obtained in this first stages are only applied for the optimization of the casting process itself and they are then ignored during the analysis of the next phases, such as heat treatment and life prediction of the cast parts.
These cast components are consequently shape optimized by structural analysis with FE-Codes without considering inhomogeneous material properties and without any residual stresses. For the purpose of taking into account eventually neglected thermal stresses, it is common practise in the automobile industry to over-design the automobile parts.
In order to reduce the uncertainties of the residual stresses and the inhomogeneous properties, the process simulation must include a thorough thermomechanical analysis, from mold filling to the manufacture of the end product. It is the aim of the IDEAL project to develop such analysis by incorporating the thermomechanical results obtained after solidification and predicting their evolutions during the subsequent thermal treatments. This paper presents the efforts done so far for the development of a simulation tool capable of capturing stress relaxation effects through an adequate description of the creep behavior of the aluminum alloys at high temperature.
More precisely, it shows:

- the implementation, in a computational form, of a unified constitutive model with twostate variables;
- the methodology to follow in order to determine the material parameters required for the specific model;
- the comparison of the computed results with the experimental values obtained during the simulation of tensile and creep tests.

The importance of this one-dimensional model can be seen not only in prospective of the development of a more complex three-dimensional program to implement in a general simulation system, but also as a tool for the extraction of the material parameters from creep and tensile tests. Since these experimental tests are basically one-dimensional, a simpler one-dimensional program is of great use for parametric studies.

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UNIFIED CONSTITUTIVE MODELS

At high temperature metallic materials show in general a decrease in strength with an increase in temperature. At the same time the ductility increases and rate effects become more pronounced.
For the modelling of rate dependent phenomena, such as viscoplasticity, as well as temperature dependent behaviors, e.g. creep and stress relaxation, unified constitutive models are often applied.
A unified constitutive model was introduced in 1976 by Miller [14] for modelling the behavior of metals at high temperature. It was then used and further developed by Anand [1] and successively many others, such as Lu et al. [12], Sehitoglu et al. [16], [17] and Smith et al. [19].

NOMENCLATURE

a	constant of proportionality
A	constant of proportionality
B	material parameter
C	material parameter
D	damage
$\dot{D}$	evolution rate of the damage
E	elastic modulus [Pa]
K	drag stress [Pa]
K^*	drag stress at steady state [Pa]
$\dot{K}$	evolution rate of drag stress [Pa/s]
K_0	initial drag stress [Pa]
L	length of a specimen [m]
n	exponent of the power law
Q	activation energy [j/mol K]
R	universal gas constant [j/mol K]
S_{ij}	ij-component of the deviatoric stress tensor [Pa]
t	time [s]
t_n	time at the n^{th} step [s]
T	absolute temperature [K]
T_m	absolute melting temperature [K]
α	constant
α_1	constant
ε	total strain in 1-D
ε_n	total strain at the n^{th} step in 1-D
$\dot{\varepsilon}^{in}$	inelastic strain rate in 1-D [1/s]
ε^{in}_e	effective inelastic strain
$\dot{\varepsilon}^{in}_e$	effective inelastic strain rate [1/s]
$\dot{\varepsilon}^{in}_{ij}$	ij-component of the inelastic strain rate tensor [1/s]
σ	stress level in 1-D [Pa]
σ^*	steady state stress level in 1-D [Pa]
σ^{trial}	trial stress in 1-D [Pa]
σ_e	equivalent deviatoric stress [Pa]
σ_{ij}	ij-component of the stress tensor [Pa]
Δt	time step [s]
$\Delta\varepsilon_n$	increment in the total strain at the n^{th} step in 1-D
$\Delta\sigma_n$	increment in the stress at the n^{th} step in 1-D

The fundamental assumption of all these models is that creep and viscoplasticity are both irreversible strains developed because of dislocations motion in the material structure; hence they can be modelled using the same constitutive laws, in terms of one or more state variables. All the mentioned models are based on the definition of two state variables: the *back stress*, responsible for the kinematic hardening, and the *drag stress*, responsible for the isotropic hardening.
More specifically, the back stress is usually introduced for modelling the cycling loading; while the drag stress is introduced to take into account the hardening and softening occurred because of microstructural changes in the material, due to the temperature and to the strain rate. For the purposes of the present work, since cyclic loading is not considered, the back stress is not taken into account.
The unified constitutive models shortly presented so far are capable of describing rate-dependent phenomena observed at high temperature as long as *internal damages* can be neglected. However, creep tests show that at temperatures commonly used for the thermal treatment T4 (470°C< T < 520°C), a specimens experiences material degradation already when subjected to a stress of 6MPa. In order to be able to simulate the strong acceleration in strain rate due to material degradation, the theory of Continuum Creep Damage Mechanics (CDM) developed by Dyson and McLean [15], [13] was applied in the model presented in this paper.
The physically-based CDM described by Dyson and McLean, is a multi-state variable formulation that can take into account several creep damage categories, such as strain-induced, thermally-induced and environmentally-induced damages. Because of the purposes of this project, only the strain-induced damages have been incorporated in the developed model. It is indeed considered that thermal treatments are not long enough to experience "particle - coarsening" or "depletion of solid-solution elements". Moreover "fracture of surface corrosion product" and "internal oxidation" can be neglected because thermal treatments normally take place in a controlled-environment.
Because of the shape of the tertiary phase observed during creep tests, the multiplication of mobile dislocations was concluded to be the most important damage. For this reason, according to the CDM theory, an extra state variable, the damage was introduced in the model.

DEVELOPED MODEL

In order to simulate the heat treatment processes of aluminum cast parts, a simple unified constitutive model with two state variables, the drag stress and the damage, was developed according to the following procedure.
Viscous material behavior of metals at high temperature is often modelled in literature by assuming a power law for the effective inelastic strain rate:

$$\dot{\varepsilon}_e^{in} = a * \sigma_e^n \tag{1}$$

where $\dot{\varepsilon}^{in}_e$ is the equivalent inelastic strain rate, while σ_e is the equivalent deviatoric stress:

$$\dot{\varepsilon}_e^{in} = \left(\frac{2}{3}\dot{\varepsilon}_{ij}^{in}\dot{\varepsilon}_{ij}^{in}\right)^{\frac{1}{2}}, \quad \sigma_e = \left(\frac{3}{2}S_{ij}S_{ij}\right)^{\frac{1}{2}} \tag{2}$$

In order to take into account strain hardening, which leads to a decreasing creep rate with increasing plastic strain, equation (1) is modified to (see Lu [12]):

$$\dot{\varepsilon}_e^{in} = a * \left(\frac{\sigma_e}{K}\right)^n \tag{3}$$

In equation (3) the parameter a can be thought as a reference strain rate. The hardening exponent n is a constant, which is normally in the range of 32 to 200 for most metals at room temperature.
Consequently the drag stress K would be equal to the effective Von Mises stress for the material, if the Mises stress were measured in a tensile test with the tensile strain rate prescribed such that $\dot{\varepsilon}^{pl}_{ij} = a$, Tvergaard [22].
For the drag stress K the following simple evolution law (see also Anand [1]) is considered:

$$\dot{K} = B * (K^* - K)\dot{\varepsilon}_e^{in} \tag{4}$$

where B and K^* are material constants which can be temperature dependent. Since $\dot{K} = 0$ when $K = K^*$, K^* can be seen as the value of K when the steady-state is reached (i.e. during secondary creep).
The constant B controls the amount of isotropic hardening produced by a given amount of strain.
As such, it plays an important role in transient situations, such as:

- when a tensile test is simulated. Higher values of B cause the stress to rise rapidly as nonelastic strain is imposed;
- when a creep test is simulated. Higher values of B lead to a more important primary creep, while lower values can result in a negligible primary creep.

By integrating equation (4), the drag stress can be expressed as:

$$K = K^* - (K^* - K_0)\, e^{-Bc_e^{in}} \tag{5}$$

where K_0 is a temperature dependent constant, which represents the initial value, i.e. the value of K for $\varepsilon^{in}_e = 0$.
With the purpose of taking into account the temperature dependence, a common Arrhenius law is introduced, and equation (3) is modified to:

$$\dot{\varepsilon}_e^{in} = A * e^{\left(-\frac{Q}{RT}\right)} * \left(\frac{\sigma_e}{K}\right)^n \tag{6}$$

where Q represents the activation energy, R the universal gas constant and T the absolute temperature.
However, it has to be considered that above 0.6 of the melting temperature T_m the activation energy is almost constant (and equal to Q), but below $0.6T_m$ the activation energy decreases linearly to zero at $0K$. Following the approach described by Miller in [14] the temperature dependence can consequently be introduced into the model as follows:

$$\dot{\varepsilon}_e^{in} = \begin{cases} A * \exp\left[\left(-\frac{Q}{0.6RT_m}\right)\left(ln\left(\frac{0.6T_m}{T}\right)+1\right)\right] * \left(\frac{\sigma_e}{K}\right)^n & \text{for } T \le 0.6T_m \\ A * \exp\left(-\frac{Q}{RT}\right) * \left(\frac{\sigma_e}{K}\right)^n & \text{for } T > 0.6T_m \end{cases} \tag{7}$$

If equations (7) are considered more thoroughly, it is noticed that the model described here does not have any yield surface: a non-zero plastic rate corresponds indeed to any stress different from zero [22]. Instead of one particular yield surface the viscoplastic material has a whole family of neighboring plastic potential surfaces, which mark a gradual transition from mainly elastic to mainly plastic behavior (see figure 1).

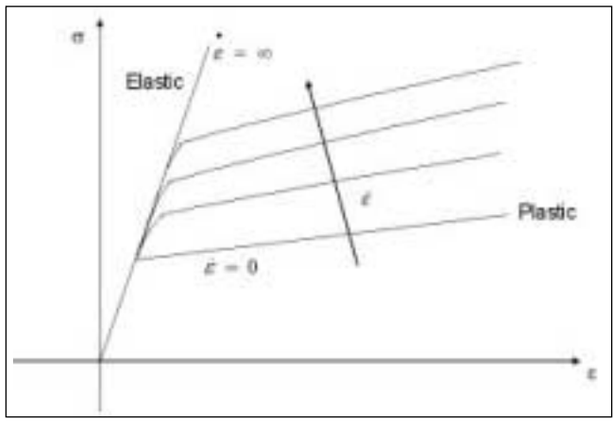

Fig. 1 – Typical stress-deformation curves for viscoplastic materials.

Fig. 1 – Tipiche curve di sforzo-deformazione per materiali viscoplastici.

As mentioned in the previous section, equations (7) describe the creep behavior well as long as no damages occur in the structures. However, since damages cannot be neglected when simulating stress evolution during heat treatment of aluminum parts, an extra state variable D needs to be introduced, so that equations (7) become:

$$\dot{\varepsilon}_e^{in} = \begin{cases} A * (1+D) * \exp\left[\left(-\frac{Q}{0.6RT_m}\right)\left(ln\left(\frac{0.6T_m}{T}\right)+1\right)\right] * \left(\frac{\sigma_e}{K}\right)^n & \text{for } T \le 0.6T_m \\ A * (1+D) * \exp\left(-\frac{Q}{RT}\right) * \left(\frac{\sigma_e}{K}\right)^n & \text{for } T > 0.6T_m \end{cases} \tag{8}$$

Following the suggestion of Dyson and McLean [13] the following simple evolution law is considered:

$$\dot{D} = C * \dot{\varepsilon}_e^{in} \tag{9}$$

where C is a material parameter.

Moreover, since experimental tests showed that deformations of 10-20% could be reached in the axial direction, a "constant load" model was implemented, instead of a more common "constant stress", in order to better reproduce the experimental conditions.

Constitutive Equations for 3D

Equation (8) represents the flow rule implemented in the model in term of the *equivalent* inelastic strain rate. However, when simulating the stress-strain evolution of a real cast part, a three-dimensional relation is needed as well. For this purpose, the following tensor relation between the strain rate tensor and the deviatoric stress tensor has to be introduced:

$$\dot{\varepsilon}_{ij}^{in} = \frac{3}{2} * \dot{\varepsilon}_e^{in} * \frac{S_{ij}}{\sigma_e} \tag{10}$$

where $\dot{\varepsilon}^{in}_{ij}$ is the $i\,j$ component of the inelastic strain rate tensor and S_{ij} is the deviatoric stress :

$$S_{ij} = \sigma_{ij} - \frac{1}{3}\sigma_{kk}\delta_{ij} \tag{11}$$

Substituting equations (8) into equation (10) leads to:

$$\dot{\varepsilon}_{ij}^{in} = \begin{cases} \frac{3}{2} * A * (1+D) * e^{\left[\left(-\frac{Q}{0.6RT_m}\right)\left(ln\left(\frac{0.6T_m}{T}\right)+1\right)\right]} * \left(\frac{\sigma_e}{K}\right)^n * \frac{S_{ij}}{\sigma_e} & \text{for } T \le 0.6T_m \\ \frac{3}{2} * A * (1+D) * e^{\left(-\frac{Q}{RT}\right)} * \left(\frac{\sigma_e}{K}\right)^n * \frac{S_{ij}}{\sigma_e} & \text{for } T > 0.6T_m \end{cases}$$

(12)

which is the constitutive equation, that together with the evolution laws (4) and (9) were implemented in a 1D numerical program.

Constitutive Equations for 1D

In the case of a one dimensional tensile test or creep test, since $\sigma_e = |\sigma 11|$ and $S11 = {}^2/_3\,\sigma 11$, equation (10) becomes:

$$\dot{\varepsilon}_{11}^{in} = \frac{3}{2} * \dot{\varepsilon}_e^{in} * \frac{2}{3}\frac{\sigma_{11}}{|\sigma_{11}|} = \dot{\varepsilon}_e^{in} * sign(\sigma_{11}) \tag{13}$$

Substituting equation (8) into equation (13) it is possible to write:

$$\dot{\varepsilon}^{in} = \begin{cases} A * (1+D) * \exp\left[\left(-\frac{Q}{0.6RT_m}\right)\left(ln\left(\frac{0.6T_m}{T}\right)+1\right)\right] * \left(\frac{\sigma}{K}\right)^n & \text{for } T \le 0.6T_m \\ A * (1+D) * \exp\left(-\frac{Q}{RT}\right) * \left(\frac{\sigma}{K}\right)^n & \text{for } T > 0.6T_m \end{cases}$$

(14)

where the subscript 11 has been neglected for simplicity and it was assumed that the applied stress is positive (as in a common tensile and tensile creep test).

NUMERICAL IMPLEMENTATION IN 1D

In order to determine all the material parameters needed for the model, a one dimensional program was developed at first. It is indeed from tensile and creep tests, i.e. typical one dimensional tests, that those parameters can be evaluated.

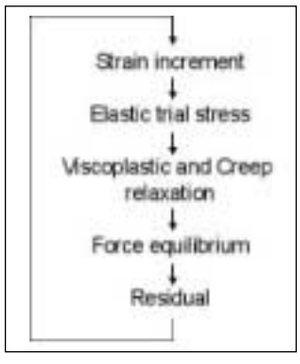

Fig. 2 – Numerical implementation of the viscoplasticity/creep algorithm.

Fig. 2 – Implementazione numerica dell'algoritmo di viscoplasticita'/scorrimento viscoso.

For the purpose of enhancing stability, the algorithm for the program was based on a *backward Euler* implementation in the frame work of a strain driven problem. In the algorithm (see also figure 2) a strain increment is calculated and an elastic trial state is then evaluated, similarly to the procedure described by Simo [18] for the *return mapping algorithm*; consequently an *inelastic relaxation* is calculated. The force equilibrium is then imposed and a residual is evaluated.
If the residual is found to be different from zero, a new strain increment is calculated and the procedure is repeated, applying a Newton-Rapson equilibrium algorithm.

ELASTIC TRIAL STATE

In order to implement a model capable of simulating inelastic relaxation, it was considered that at the current time t_n the state of a point $x \in [0,L]$ is known:

$$\left\{\varepsilon_n(x), \varepsilon_n^{in}(x)\right\} \quad \rightarrow \quad \sigma_n(x) = E_n\left[\varepsilon_n(x) - \varepsilon_n^{in}(x)\right] \tag{15}$$

and an increment $\Delta\varepsilon_n(x)$ is *given* at the time t_n, so to drive the state to the time $t_{n+1} = t_n + \Delta_t$.
In order to determine the new state at time t_{n+1}, an auxiliary state, that generally does not correspond to an actual state, is defined at first by freezing the inelastic flow. Hence a purely elastic step is calculated:

$$\begin{aligned} \sigma_{n+1}^{trial} &:= E_{n+1}\left(\varepsilon_n + \Delta\varepsilon_n - \varepsilon_n^{in}\right) \\ \varepsilon_{n+1}^{trial} &:= \varepsilon_n^{in} \end{aligned} \tag{16}$$

INELASTIC RELAXATION

Once the trial elastic state is calculated, an inelastic relaxation, due to viscoplasticity and creep, must be evaluated, Bellini [5].
Applying Hooke's law, and remembering the definition of the trial stress (see equation (16)) it is possible to write:

$$\sigma_{n+1}(x) = E_{n+1}\left(\varepsilon_n^{el}(x) + \Delta\varepsilon_n - \Delta\varepsilon_n^{in}(x)\right) = \sigma_{n+1}^{trial} - E_{n+1}\Delta\varepsilon_n^{in}(x) \tag{17}$$

For the determination of $\sigma_{n+1}(x)$ it is thus necessary to calculate the inelastic strain increment during the interval Δ_t. Remembering equation (13) and (8), the inelastic strain increment can be expressed as:

$$\Delta\varepsilon_n^{in}(x) = A * (1+D) * e^{\left(-\frac{Q}{RT}\right)} * \left(\frac{|\sigma|}{K}\right)^n * \Delta t * sign(\sigma) \tag{18}$$

where only the case of $T > 0.6T_m$ was considered for simplicity. Substituting equation (18) into (17) and rearranging the different terms, it is possible to conclude that the new stress level must satisfy the following equation:

$$f(\sigma_{n+1}) = \sigma_{n+1}(x) - \sigma_{n+1}^{trial} + E * A * (1+D) * e^{\left(-\frac{Q}{RT}\right)} * \left(\frac{|\sigma|}{K}\right)^n * \Delta t * sign(\sigma) = 0 \tag{19}$$

From equation (19) it is evident that the stress level at the new time t_{n+1} can not be found in closed form, thus a Newton-Raphson algorithm or a bisection technique, as suggested by Bathe in [4], nust be locally implemented.

DETERMINATION OF MATERIAL CONSTANTS

Once the model has been defined and implemented in the numerical code, the determination of the material constants is the next important step in order to achieve an adequate simulation. For the purpose of determining the value of the exponent n of the power law, the experimental creep curves at T = 180°C and several imposed stresses were considered .
Since a correct evaluation of n requires the achievement of steady state, the secondary creep rates of the mentioned curves were determined as the minimum creep rates reached during the tests. However, in order to determine the values of n and A of the equation (14), a value for Q was needed as well. As a first guess, a value of Q = 210000 *J/mol*, which, corresponds to the theoretical activation energy for aluminum, Frost [6], was imposed.
By plotting $\dot{\varepsilon}_{in} / e^{(-Q/RT)}$ versus the applied stress σ^* (here the imposed stress σ is called σ^* in order to remember that those values correspond to the steady-state, i.e. secondary creep) two regions of different creep mechanisms could be recognized (see figure 3). More specifically it could be noticed that for $\sigma^* \le 110$ *MPa* the experimental points were aligned on an exponential with smaller n (see equation (14)) with respect to the points for $\sigma^* > 110$. For this reason it was considered that K^* = 110 *MPa* for T = 180°C.
In order to evaluate K^* at other temperatures, it was imposed that $K^*/E(T) = \alpha$, where α is a constant, Sehitoglu [17], that was calculated to be $K^*/E(180) = 110/71289 = 1.543e\text{-}3$.
Once determined the temperature dependence of K^*, also creep tests at other temperatures were taken into account and the normalized values of σ^*/K^* were calculated.
If equation (14) is rearranged to express the normalized $\dot{\varepsilon}^{in}$ with respect to the normalized σ^*/K^*, the following equation can be derived:

$$\begin{cases} \log_{10}\left(\dfrac{\dot{\varepsilon}^{in}}{\exp\left(-\frac{Q}{RT_m}\left[\ln\left(\frac{0.6T_m}{T}\right)+1\right]\right)}\right) = n * \log_{10}\left(\frac{\sigma}{K}\right) + \log_{10}(A) & \text{for } T \le 0.6T_m \\ \log_{10}\left(\dfrac{\dot{\varepsilon}^{in}}{\exp\left(-\frac{Q}{RT}\right)}\right) = n * \log_{10}\left(\frac{\sigma}{K}\right) + \log_{10}(A) & \text{for } T > 0.6T_m \end{cases} \tag{20}$$

where $D = 0$ because only tests with no damage (i.e. increase of strain rate after the secondary creep phase) were taken into account. By plotting the left hand sides of equation (20) versus $\log_{10}(\sigma^*/K^*)$ it was consequently possible to make a linear fitting; thus to determine the value of n, which represents the slope, and the value of A, i.e. the value of the intercept. It should be mentioned that the data points that have been circled in figure 5 were not considered in the fitting because they represent tests which actually experienced damage. In the same figure it is possible to observe that three linear curves could be individuated in three different stress regions. This result can be explained considering that for different stress levels, different creep mechanisms take place:

- for $\log_{10}(\sigma^*/K^*) \le -0.5$ it can be considered creep controlled in viscous glide;
- for $-0.5 < \log_{10}(\sigma^*/K^*) \le 0$ it can be considered climbed-controlled creep;
- for $\log_{10}(\sigma^*/K^*) > 0$ it can be considered that power law breakdown regime has been reached.

At this point, it should not be forgotten that the value of Q was only a guess. Consequently this value needed to be corrected, with a trial and error approach, to fit also the experimental data at different temperatures as well. Tensile test curves at T = 275°C were considered in order to evaluate Q. The program was run for Q = 210000 *J/mol* and the steady state part of the curve was compared with the experimental data (see figure 4). According to the result of the comparison, the value of Q was modified, and consequently also n and A were adjusted. This process was repeated until when a good agreement between the numerical and the experimental curve was found for Q = 150 *KJ/mol* (see figure 4).
For the new value of Q, three creep laws were determined as functions of σ/K (see figure 5):

$$\dot{\varepsilon}^{in} = \begin{cases} 1.0e7 * e^{\left(-\frac{Q}{RT}\right)}\left(\frac{\sigma}{K}\right)^{2.3} & \text{for } \left(\frac{\sigma}{K}\right) \le 0.32 \\ 2.00e10 * e^{\left(-\frac{Q}{RT}\right)}\left(\frac{\sigma}{K}\right)^{4.8} & \text{for } 0.32 < \left(\frac{\sigma}{K}\right) \le 1 \\ 2.00e10 * e^{\left(-\frac{Q}{RT}\right)}\left(\frac{\sigma}{K}\right)^{23.4} & \text{for } \left(\frac{\sigma}{K}\right) > 1. \end{cases} \tag{21}$$

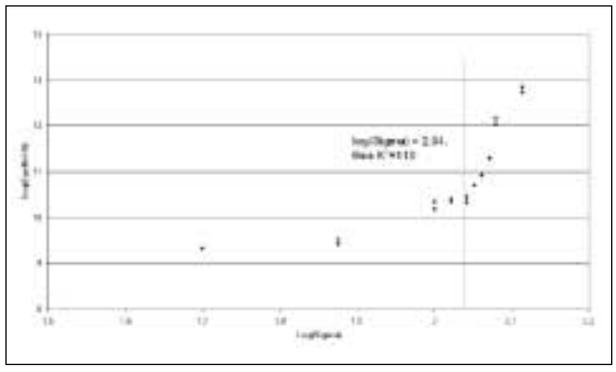

Fig. 3 – Diagram of $\dot{\varepsilon}^{in}$ as referred to σ^* for T= 180°C .

Fig. 3 – Diagramma di $\dot{\varepsilon}^{in}$ rispetto a σ^* per T= 180°C.

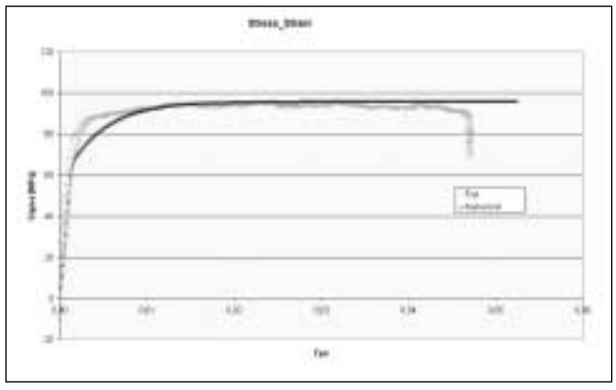

Fig. 4 – Determining Q: comparison between experimental and numerical curves for a traction test at T = 275°C.

Fig. 4 – Determinazione di Q: confronto tra curve sperimentali e numeriche per una prova di trazione a T = 275°C.

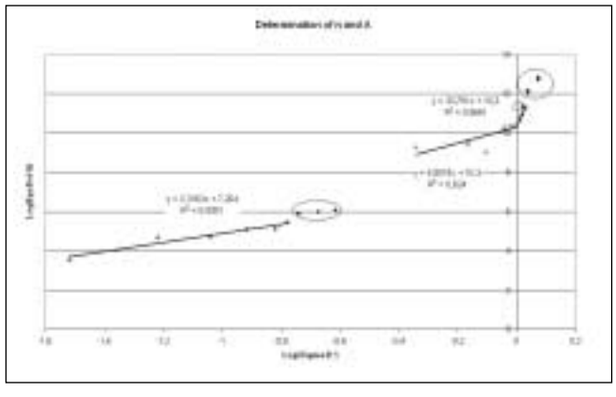

Fig. 5 – Determining the parameters n and A.

Fig. 5 – Determinazione dei parametri n e A.

Once the material parameters Q, n and A were determined, a value of K_0 had to be found in order to best simulate the experimental data. For this reason the experimental tensile test curves at different temperatures and different strain rates were considered. The value of the yield stress (σ_0) and the corresponding ($\dot{\varepsilon}^{in}$) were extrapolated for each curve and a value of K_0 was determined, for each curve, as:

$$K_0 = \left(\frac{A * e^{\left(-\frac{Q}{RT}\right)}}{\dot{\varepsilon}^{in}} \right)^{\frac{1}{n}} * \sigma_0 \qquad (22)$$

where it was assumed that $n = 33.4$, because strain rates $\dot{\varepsilon}^{in} > 3e\text{-}7$ (see figure 5) are realistic in tensile tests. A value of $K_0 = 79$ *MPa* was calculated for $T = 180$°C. In order to evaluate K_0 at other temperatures, it was imposed that $K^*/E(T) = \alpha_1$, where α_1 is a constant, that was calculated to be $K_0/E(180) = 110/71289 = 1.12e\text{-}3$.

Another important value to be determined was then the hardening parameter B, that, as said before, plays an important role during the transient situations, both in the stress-strain as well as in the creep curves. A best-fit value of B was found by simulating and then comparing both tensile tests and creep tests at different temperatures and different strain rates. It was concluded that the hardening parameter B was a constant $B = 400$.

The last value still to be evaluated was the value of C for the evolution law of the damage parameter (see equation (9)). Using the data obtained for creep tests that showed damage initialization:

- at $T = 180$°C and $\sigma > 115$ *MPa*;
- at $T = 275$°C and $\sigma = 100$ *MPa*;
- at $T = 450$°C and $\sigma > 6$ *MPa*;

a best-fit value of $C = 35$ was found by simulating and then comparing the results with the experiments. Using this set of data it was possible to reproduce the experimental curves.

RESULTS

This section is dedicated to the comparison of the numerically obtained curves with the experimental data obtained both for tensile and creep tests. About the experimental data it should be emphasized that the experimental curves show large scattering, probably because of the difficulties related to measurements at high temperatures. Consequently it is hard to compare experimental and numerical curves. For simplicity, in figure 6 for example only one experimental curve for each temperature was shown.

The numerical simulations and the experimental curves obtained at several temperatures during *tensile tests* are shown in figure 6. It can be noticed that good agreement is reached, showing that the model well simulate the tensile tests even for a quite wide range of temperatures. The computed results and the experimental values obtained during the simulation of *creep tests* at $T = 180$°C and different stress levels are shown in figures 7 - 8. From the comparison it is possible to conclude that the developed program well predicts the creep strains during high temperature exposure of the aluminum parts for almost all the stress levels. However, for an imposed stress of $\sigma = 75$ *MPa* the simulated curves overestimate the strain rate during secondary creep. This result is due to the linear fitting, shown in figure 5, that was used for the determination of the exponent n. From the same figure it can be seen indeed that for

$\sigma = 75$ *MPa*, hence $log_{10}(\sigma^*/K^*) = -0.16$

the curve fitting amply over-estimates the strain rate.

A comment should also be spent for the comparison with the creep curve at $\sigma = 130$ *MPa*.

Figure 7 shows that the numerical curve has the same shape of the experimental one, but the simulation underestimates the time to failure. However it should be said that this simple onedimensional program works well for a wide range of temperatures, stresses and deformation rates, so obviously some deviations could be expected for the single experiments.

The comparison between computed results and experimental values for simulations of *creep tests* at $T = 230$°C and imposed stresses of 80 and 110 *MPa* is presented in figure 8. Also in this case it is possible to conclude that the developed program well predicts the creep strains even if a mismatch can be observed for the low imposed stress of 80 *MPa*.

At last, also for the simulated and observed curves obtained at $T = 450$°C it can be observed that the introduction of the

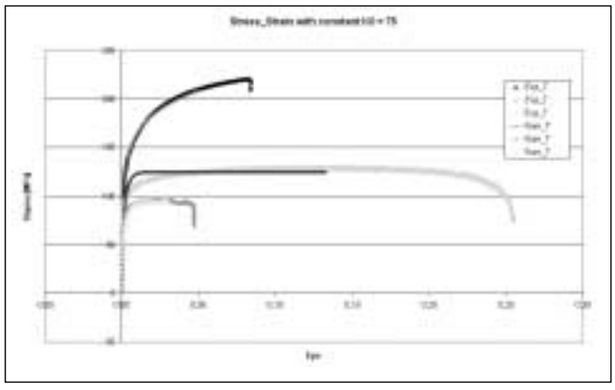

Fig. 6 – Experimental and numerical curves for a traction test at different temperatures.

Fig. 6 – Curve sperimentali e numeriche per prove di trazione a diverse temperature.

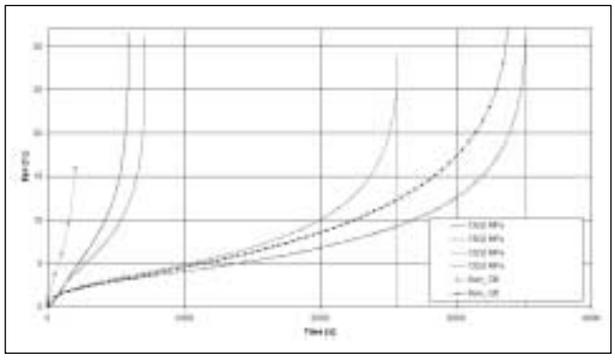

Fig. 7 – Comparison with the creep tests performed at T = 180°C.

Fig. 7 – Confronto con prove di scorrimento viscoso svolte a T = 180°C.

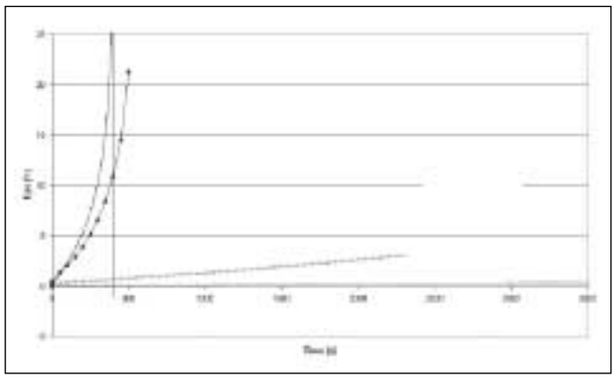

Fig. 8 - Comparison with the creep tests performed at T = 230°C.

Fig. 8 - Confronto con prove di scorrimento viscoso svolte a T = 230°C.

damage parameter allows to well predict the growth in the strain rate after having reach a minimum value during the secondary creep phase.

CONCLUSIONS

The paper illustrates the work done with the scope of predicting stress relaxation of aluminum at high temperature. In particular, it presents the unified constitutive model used to describe the creep behavior of aluminum. The model is based on two state variables, the drag stress, which represents the isotropic hardening of the material and the damage. It is shown that by applying simple evolution laws, such as equation (4) for the drag stress and (9) for the damage, not only the steady state, but also the transient situations can be properly modelled (see the previous section). For the simulation of all the phases of the heat-treatment of aluminum, which can last between 2 and 8 hours, not only the secondary, but also the primary and the tertiary creep, need indeed to be quite accurately predicted.
The paper showed that:

- the constitutive law expressed in term of a ratio of the imposed stress with respect to the drag stress allows to well predicts the primary as well as the secondary phase the creep tests;
- three creep mechanisms could be identified for different stress levels: creep controlled in viscous glide, climbed-controlled creep and break down creep. For each mechanism a different power law exponent was identified;
- the introduction of a damage parameter, which takes into account the multiplication of dislocations induced by the strains, allows to predict the growth of the strain rate, thus the failure of the part.

The results showed that this simple model well predicts the creep behavior of the cast aluminum parts in a broad range of temperatures, stresses and strain rates. Obviously more accurate results could be reached if those ranges could be narrowed. However, the comparison between simulations and experimental tests is both quantitative and qualitative satisfactory.

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ABSTRACT

ESTRAZIONE DI PARAMETRI DEL MATERIALE PER LA SIMULAZIONE DI TRATTAMENTI TERMICI DI PARTI IN LEGHE DI ALLUMINIO DI COLATA UTILIZZANDO MODELLI UNIFICATI

Parole chiave:
trattamenti termici, alluminio e leghe, modellazione

Negli ultimi decenni, per soddisfare la crescente richiesta da parte delle aziende automobilistiche, di prodotti di allumninio di colata di alta qualità, le fonderie hanno mostrato un notevole interesse per l'implementazione di simulazioni numeriche come parte integrante del loro processo di progettazione. Di consequenza e' possibile affermare che le simulazioni per il calcolo del riempimento degli stampi e per il calcolo del processo di solidificazione hanno raggiunto un elevato livello di sviluppo. Tuttavia, i risultati ottenuti in queste prime fasi sono usati solamente per l'ottimizzazione del processo stesso di colata e sono totalmente trascurati durante l'analisi delle fasi successive, quali trattamenti termici e previsione del ciclo di vita della parti ottenute in colata. In conclusione, i prodotti di colata sono dimensionati tenendo in considerazione le previsioni ottenute attraverso un'analisi strutturale agli elementi finiti che esclude a priori ogni possibile inomogeneità delle proprietà del materiale ed eventuali sforzi residui. Per ridurre possibili rischi di rottura del pezzo a causa dei trascurati sforzi residui e di inomogeneità, è pratica comune nelle aziende automobilistiche sovradimensionare i componenti meccanici. Le conseguenze di queso modo di procedere possono essere riassunte nelle seguenti voci:

- *pesi più elevati delle autovetture;*
- *maggiori consumi di carburante;*
- *maggiori spese;*
- *maggior inquinamento.*

La crescente preoccupazione per l'inquinamento dell'ambiente e il continuo aumento del costo del carburante hanno spinto le aziende automobilistiche a trovare una soluzione per il sovradimensionamento dei componenti automobilistici attraverso la riduzione delle incertezze dovute agli sforzi residui e alle inomogeneità del materiale. Questo obbiettivo puo' essere raggiunto solo attraverso un processo di simulazione che consideri un'analisi termomeccanica completa, dallo studio del riempimento degli stampi alla produzione del prodotto finale. Lo sviluppo di questa analisi è proprio il principale obbiettivo del progetto IDEAL (Integrated Development Routes for Optimized Cast Aluminium Components), finanziato dalla comunità europea nell'ambito del "framework 6" e nato in collaborazione con le industrie automobilistiche e le fonderie. La procedura con la quale si prevede di svolgere uno studio completo del processo di produzione della parti meccaniche prevede l'incorporazione dei risultati termomeccanici ottenuti dopo solidificazione e la predizione della loro evoluzione durante i successivi trattamenti termici. Questo articolo presenta lo sviluppo di un prototipo di un programma di simulazione per la predizione del rilassamento degli sforzi meccanici che si osserva durante i trattamenti termici, attraverso una adeguata modellizzazione del comportamento di scorrimento viscoso delle leghe di alluminio soggette ad alte temperature. Più precisamente, presenta:

- *l'implementazione, in forma computazionale, di un modello costitutivo unificato, che si sviluppa su due variabili di stato. E' opportuno sottolineare a questo punto che il presupposto fondamentale dei modelli costitutivi unificati è che la viscoplasticità e lo scorrimento viscoso, che sono entrambi deformazioni inelastiche dovute al movimento di dislocazioni nella struttura del materiale, possono essere modellizzati attraverso l'implementazione di simili leggi per la velocita' di deformazione plastica;*
- *la metodologia da seguire per determinare i parametri del materiale che sono necessari per il modello sviluppato;*
- *il confronto tra i dati ottenuti sperimentalmente e i risulati calcolati durante la simulazione di prove di trazione e di prove di scorrimento viscoso. Tale confronto mostrerà che il programma sviluppato è in grado di simulare appropriatamente il comportamento di scorrimento viscoso della lega di alluminio in considerazione. In particolare verrà evidenziato che i risulatati ottenuti, per esempio in termine di rilassamento degli sforzi, concordano con le prove effettuate sia per un'ampia finestra di temperature sia per un'ampia finestra di velocita' di deformazioni.*

L'importanza di questo modello uni-dimensionale può essere visto nono solo in prospettivadello sviluppo di un più complesso programma tri-dimensionale da implementare in un sistema di simulazione generale, ma anche come strumento da usare per l'estrazione dei parametri del materiale da prove di tensione e di scorrimento viscoso. Siccome infatti queste prove sperimentalihanno la caratteristica di essere unidimensionali, un più semplice programma unidemensionale puo' essere di grande aiuto, soprattutto dal punto di vista di risparmio di tempocomputazionale, per studi parametrici.