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DAMAGE EFFECT ON THERMO-MECHANICAL FIELDS IN A MID-THICK PLATE

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The paper aims at the analysis of damage mechanisms and its influence on heat flow in a mid-thick Reissner's plate. Adapting the concept of thermo-damage coupling in continuum damage mechanics, the following two 2D coupled problems are formulated: heat transfer through nonhomogeneous partly damaged material and Reissner's membrane-plate made of aluminium alloy, subjected to brittle damage resulting from mechanical and thermal loadings.

Key words: continuum damage, heat transref, Reissner's plate

1. Introduction

The classical theory of thin plate subjected to mechanical and 1D thermal field loads is applied even to the complicated case of piston analysis (cf Lipka, 1970). Such a simplified theory may be treated only as a first approximation of the problem. In a real structure neither the plate thickness is thin nor the temperature field is 1D, so the theory to be more accurate and closer to reality bases on the mid-thick plate (cf Reissner, 1945) and the continuum damage mechanics method including thermo-damage coupling effects.

2. Basic equations

2.1. Heat transfer in a damaged material

In the present paper, an extension of the Tanigawa concept (cf Tanigawa, 1995) originally derived for a time-independent nonhomogeneous but isotropic material, to cover time-dependent partly damaged materials is presented. Namely, the coefficient of thermal conductivity λ of heat flux through partially damaged material in the homogeneous quasi-stationary Fourier equation (slow temperature field change following damage) is assumed to be the linear function of scalar damage parameter D (cf Ganczarski and Skrzypek, 1995, 1997; Skrzypek and Ganczarski, 1998)

$$\operatorname{div}(\lambda \operatorname{grad} T) = 0 \qquad\qquad \lambda = \lambda_0(1-D) \tag{2.1}$$

However, the effect of damage on the thermal expansion coefficient α is not taken into account. This problem was discussed by Ganczarski (1999). Additionally the heat transfer between fluid (gas) and solid (plate), governed by the Newton law of free convection, is assumed to be affected by damage.

2.2. Reissner's membrane-plate

Assuming that total strains are decomposed into elastic, creep and thermal components: $\boldsymbol{\varepsilon} = \boldsymbol{\varepsilon}^e + \boldsymbol{\varepsilon}^c + \boldsymbol{\varepsilon}^{th}$, we formulate an axisymmetric problem of the Reissner-Mindlin moderate thickness plate theory where the straight and normal segment to the mid-plane before deformation remains straight, but not necessarily normal. Additionally, the stress component σ_z is assumed in the form

$$\sigma_z = -\frac{3q}{4}\Big[\frac{2}{3} - \frac{2z}{h} + \frac{1}{3}\Big(\frac{2z}{h}\Big)^3\Big] \tag{2.2}$$

satisfying the following conditions

$$\sigma_z = \begin{cases} -q & \text{for} \quad z = -h/2 \\ 0 & \text{for} \quad z = h/2 \end{cases} \tag{2.3}$$

at the upper and lower surfaces of the plate, respectively. The equations of equilibrium of the stress resultants, when geometrical changes are taken into account, together with the average values of displacements (cf Love, 1944)

$$\begin{gathered} w = \frac{3}{2h}\int\limits_{-h/2}^{h/2} w^0\Big[1 - \Big(\frac{2z}{h}\Big)^3\Big]\,dz \\ \varphi = \frac{12}{h^2}\int\limits_{-h/2}^{h/2} \frac{u^0 z}{h}\,dz \qquad\qquad u = \int\limits_{-h/2}^{h/2} \frac{u^0}{h}\,dz \end{gathered} \tag{2.4}$$

and the following definitions of the membrane and bending stiffnesses

$$\mathcal{B} = \frac{Eh}{1-\nu^2} \qquad\qquad \mathcal{D} = \frac{Eh^3}{12(1-\nu^2)} \tag{2.5}$$

and the generalized thermal and creep forces

$$\left[\begin{array}{c} N^{th} \\ M^{th} \end{array}\right] = \frac{E}{1-\nu} \int\limits_{-h/2}^{h/2} \theta \left[\begin{array}{c} 1 \\ z \end{array}\right] \, dz$$

$$\left[\begin{array}{c} N^c_{r/\vartheta} \\ M^c_{r/\vartheta} \end{array}\right] = \frac{\mathcal{B}}{h} \int\limits_{-h/2}^{h/2} (\varepsilon^c_{r/\vartheta} + \nu\varepsilon^c_{\vartheta/r}) \left[\begin{array}{c} 1 \\ z \end{array}\right] \, dz \tag{2.6}$$

$$Q^c = \int\limits_{-h/2}^{h/2} \frac{3\gamma^c}{2h}\Big[1 - \Big(\frac{2z}{h}\Big)^2\Big] \, dz$$

where $\theta = T - T_{ref}$ is a difference between the actual and reference temperatures, lead to the simplified Kármán system extended to cover the case of visco-elastic plate of moderate thickness (Ganczarski et al., 1997)

— for $t = 0$

$$\begin{aligned} &\mathcal{B}\Big(\nabla^2 u - \frac{u}{r^2}\Big) = \underline{\frac{\nu}{(1-\nu)}\frac{h}{2}\frac{dq}{dr}} + \frac{dN^{th}}{dr} \\ &\mathcal{D}\nabla^4 w - N_r\frac{d^2w}{dr^2} - N_\vartheta\frac{1}{r}\frac{dw}{dr} = q - \underline{\frac{h^2}{10}\frac{2-\nu}{1-\nu}\nabla^2 q} - \nabla^2 M^{th} \end{aligned} \tag{2.7}$$

— for $t > 0$

$$\begin{aligned} &\mathcal{B}\Big(\nabla^2\dot{u} - \frac{\dot{u}}{r^2}\Big) = \underline{\frac{\nu}{(1-\nu)}\frac{h}{2}\frac{d\dot{q}}{dr}} + \frac{d\dot{N}^{th}}{dr} + \frac{d\dot{N}^c_r}{dr} + \frac{\dot{N}^c_r - \dot{N}^c_\vartheta}{r} \\ &\mathcal{D}\nabla^4\dot{w} - \Big(N_r\frac{d^2w}{dr^2}\Big)^{\cdot} - \Big(N_\vartheta\frac{1}{r}\frac{dw}{dr}\Big)^{\cdot} = \dot{q} - \underline{\frac{h^2}{10}\frac{2-\nu}{1-\nu}\nabla^2\dot{q}} - \nabla^2\dot{M}^{th} + \\ &+\mathcal{D}\nabla^2\frac{d\dot{Q}^c}{dr} - \frac{d^2\dot{M}^c_r}{dr^2} - \frac{1}{r}\frac{d(2\dot{M}^c_r - \dot{M}^c_\vartheta)}{dr} \end{aligned} \tag{2.8}$$

where the following additional assumptions resulting from cylindrical symmetry are true: $M_{r\vartheta} = 0$, $N_{r\vartheta} = 0$, and only the fundamental mode $k = 0$ is considered. The underlined terms of the right-hand side of Eqs (2.7) and

(2.8) are characteristic for Reissner's plate theory and may be neglected when either $h \to 0$ (classical Love-Kirchhoff plate theory) or $q = \text{const}$. In the general theory of rectangular Reissner's plate the bending state is described by the system of two equations: biharmonic one represents deflection w and the harmonic one describes equilibrium of the shear forces Q_x and Q_y. However, in the case of circular plates revealing axisymmetric deformation when the shear state is statically determined $Q = -qr/2$ and no edge effect is observed for all types of boundary conditions (cf Selman et al., 1990), the aforementioned harmonic equation may not be taken into account. Deriving system of equations (2.7) and (2.8) the simplified assumption has been made that the membrane and bending stiffnesses, as defined by Eqs (2.5)), are not affected by damage. This formulation leads to certain inconsistency. Namely, the completely damaged zone is free from mechanical stress and heat flux, but it must be able to bear the thermal stresses that result from a residual temperature field. A consistent formulation, where damage affects not only the time hardening hypothesis Eq (2.12), and coefficient of thermal conductivity Eq (2.1), but also the Young modulus in definitions of both stiffnesses Eqs (2.5), requires extension of the mechanical state equations (2.7) and (2.8) in a similar way as previously proposed by Skrzypek and Ganczarski (1999) for the Love-Kirchhoff plate of variable thickness. Hereinafter, when the above effect is neglected, both the membrane (necessary in view of temperature inhomogeneity along r) and the bending states contribute to the stress state which fulfils relations

— for $t = 0$

$$
\begin{aligned}
&\varphi = -\frac{dw}{dr} - \frac{6}{5}\frac{1+\nu}{Eh}qr && q = \text{const} \\
&\kappa_r = -\frac{d^2w}{dr^2} && \kappa_\vartheta = -\frac{1}{r}\frac{dw}{dr} \\
&\lambda_r = \frac{du}{dr} && \lambda_\vartheta = \frac{u}{r} \\
&M_{r/\vartheta} = \mathcal{D}(\kappa_{r/\vartheta} + \nu\kappa_{r/\vartheta}) - \frac{h^2}{10}\frac{q}{1-\nu} - M^{th} \\
&N_{r/\vartheta} = \mathcal{B}(\lambda_{r/\vartheta} + \nu\lambda_{r/\vartheta}) - \frac{\nu}{1-\nu}\frac{h}{2}q - N^{th} \\
&\sigma_{r/\theta} = \frac{12}{h^3}(M_{r/\vartheta} + M^{th})z + \frac{1}{h}(N_{r/\vartheta} + N^{th}) - \frac{E\alpha}{1-\nu}\theta \\
&\sigma_z = -\frac{3q}{4}\Big[\frac{2}{3} - \frac{2z}{h} + \frac{1}{3}\Big(\frac{2z}{h}\Big)^3\Big] && \tau_{rz} = -\frac{3q}{4h}\Big[1 - \Big(\frac{2z}{h}\Big)^2\Big]
\end{aligned}
\tag{2.9}
$$

— for $t > 0$

$$
\begin{aligned}
&\dot{\varphi} = -\frac{d\dot{w}}{dr} + \dot{Q}^c \\
&\dot{M}_{r/\vartheta} = \mathcal{D}(\dot{\kappa}_{r/\vartheta} + \nu\dot{\kappa}_{r/\vartheta}) + \mathcal{D}\Big(\frac{d\dot{Q}^c}{dr} + \nu\frac{\dot{Q}^c}{r}\Big) - \dot{M}^c_{r/\vartheta} - \dot{M}^{th} \\
&\dot{N}_{r/\vartheta} = \mathcal{B}(\dot{\lambda}_{r/\vartheta} + \nu\dot{\lambda}_{r/\vartheta}) - \dot{N}^c_{r/\vartheta} - \dot{N}^{th} \\
&\dot{\sigma}_{r/\theta} = \frac{12}{h^3}(\dot{M}_{r/\vartheta} + \dot{M}^c_{r/\vartheta} + \dot{M}^{th})z + \frac{1}{h}(\dot{N}_{r/\vartheta} + \dot{N}^c_{r/\vartheta} + \dot{N}^{th}) + \\
&\qquad -\frac{E}{1-\nu^2}(\dot{\varepsilon}^c_{r/\vartheta} + \nu\dot{\varepsilon}^c_{\vartheta/r}) - \frac{E\alpha}{1-\nu}\dot{\theta} \\
&\dot{\sigma}_z = 0 \qquad\qquad \dot{\tau}_{rz} = 0
\end{aligned}
\tag{2.10}
$$

2.3. Constitutive equations

The essence of thermo-damage coupling results from the reciprocal coupling between the processes of creep, microcrack growth and change of thermal field Eq (2.1), which are described by the similarity of deviators based on the flow theory

$$
\dot{\varepsilon}^c_{kl} = \frac{3}{2}\frac{\dot{\varepsilon}^c_{eff}}{\sigma_{eff}}s_{kl} \tag{2.11}
$$

the time hardening hypothesis

$$
\dot{\varepsilon}^c_{eff} = \frac{\sigma^{m-1}_{eff}}{(1-D)^m}\dot{f}(t) \tag{2.12}
$$

and Kachanov's type isotropic brittle rupture law (cf Skrzypek and Ganczarski, 1999)

$$
\frac{dD}{dt} = C\Big(\frac{\chi(\boldsymbol{\sigma})}{1-D}\Big)^n \tag{2.13}
$$

respectively. In Eq (2.13) the state of damage is measured by the scalar internal variable D, whereas damage growth is controlled by the damage equivalent stress $\chi(\boldsymbol{\sigma})$ according to Hayhurst. For the aluminium alloy $\chi(\boldsymbol{\sigma}) = \sigma_{eff}$ is applicable (Hayhurst, 1999). In the above formulae the following definitions hold

$$
\sigma_{eff} = \sqrt{\frac{3}{2}s_{kl}s_{kl}} \qquad\qquad \dot{\varepsilon}^c_{eff} = \sqrt{\frac{2}{3}\dot{e}^c_{kl}\dot{e}^c_{kl}} \tag{2.14}
$$

3. Formulation of boundary problems

3.1. Thermal boundary problem

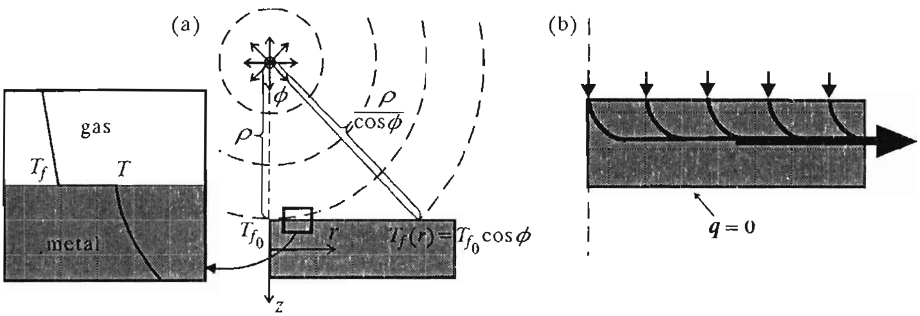

Fig. 1. Temperature front propagation (a) and schematic heat flow through plate (b)

The temperature distribution in a medium over the upper plate surface is not uniform. Assuming spherical symmetry of the temperature front propagation (Fig.1a) we find that the temperature distribution of the gas neighbouring the upper plate surface reveals cylindrical symmetry and is given by the cosine function $T_{f_0} \cos \phi$, where T_{f_0} stands for the gas temperature neighbouring the central upper point of the plate, whereas ϕ denotes the angle between normal and radial directions starting form center of the temperature front (heat source). On the other hand, typical heat flux through the plate is presented in Fig.1b. The whole heat flux which enters the upper plate surface goes towards the sidewall, whereas the lower surface is subject to the adiabatic conditions. The above comments allow one to formulate the mixed and Neumann integral type boundary conditions for Eq (2.1) in the following form ($\phi_{\max} = \pi/3$) (Moon and Spencer, 1961)

$$
\begin{aligned}
&\beta(1-D)(T_{f_0}-T)\Big|_{(r,-h/2)} = -\lambda_0(1-D)\frac{\partial T}{\partial z}\Big|_{(r,-h/2)} && \text{upper surface}\\
&\frac{\partial T}{\partial z}\Big|_{(r,h/2)} = 0 && \text{lower surface}\\
&\frac{\partial T}{\partial r}\Big|_{(0,z)} = 0 && \text{axis of symmetry}\\
&\int\limits_{-h/2}^{+h/2} \lambda_0(1-D)\frac{\partial T}{\partial r}\Big|_{(R,z)}\, dz = C && \text{sidewall}
\end{aligned}
\tag{3.1}
$$

where β denotes the coefficient of free convection between the gas and upper surface of the plate. The heat flux, that enters the top surface of the plate and is transfered through the sidewall is kept constant throughout the process ($C = \text{const}$).

3.2. Mechanical boundary value problem

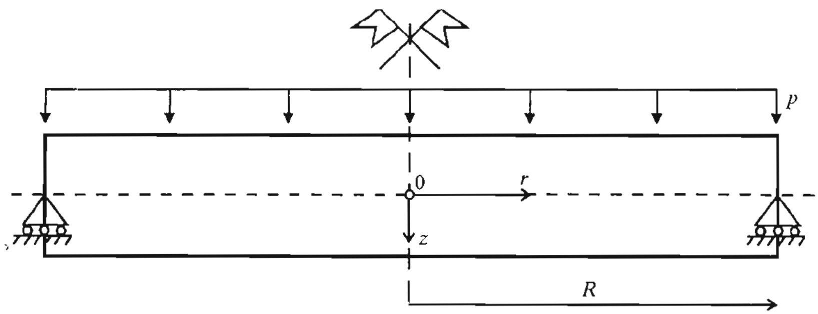

Fig. 2. Simply supported Reissner's plate

The initial and boundary conditions (Fig.2) for the simply supported plate subject to the uniform constant pressure p are as follows

$$\left.\begin{array}{l} n_r(R) = 0 \\ m_r(R) = 0 \\ u(0) = 0 \\ w(R) = 0 \end{array}\right\} \text{for} \quad t = 0 \qquad \left.\begin{array}{l} \dot{n}_r(R) = 0 \\ \dot{m}_r(R) = 0 \\ \dot{u}(0) = 0 \\ \dot{w}(R) = 0 \end{array}\right\} \text{for} \quad t > 0 \tag{3.2}$$

4. Numerical algorithm for the creep-damage problem

To solve the initial-boundary problem by FDM, we discretize time by inserting N time intervals Δt_k, where $t_0 = 0$, $\Delta t_k = t_k - t_{k-1}$ and $t_N = t_I$ (macrocrack initiation) (cf Skrzypek and Ganczarski, 1999). Hence, the initial-boundary problem is reduced to a sequence of quasistatic boundary value problems, the solution of which determines unknown functions at a given time t_k, e.g., $T(r, z, t_k) = T^k(r, z)$, $u(r, t_k) = u^k(r)$, $w(r, t_k) = w^k(r)$, etc. At each time step the Runge-Kutta II method is applied to yield updated functions T^k, u^k, w^k, etc. To account for primary and tertiary creep regimes, a dynamically controlled time step Δt_k is required, the length of which is defined by the bounded maximum damage increment

$$\Delta D^{lower} \leqslant \max_{(r,z)}\Big\{[\dot{D}^k(r,z) - \dot{D}^{k-1}(r,z)]\Delta t_k\Big\} \leqslant \Delta D^{upper} \tag{4.1}$$

Discretizing also the radial and axial coordinates r_j, z_i, by inserting the equal mesh $\Delta r = r_j - r_{j-1}$, $\Delta z = z_i - z_{i-1}$, we rewrite the equations of thermal state (2.1) and mechanical state Eqs (2.7) and (2.8) for a time step t_k in terms of FDM in respect of r_j and z_i, respectively. Applying a stage algorithm, first to the damage $[D]_j \equiv 0$, the equation of thermal problem (2.1) with the boundary conditions Eqs (3.1) is solved by using the SOR method (cf Press et al., 1993), and the initial "elastic" temperature $[T^e]_{j,i}$ is found. Then, the system of equations of mechanical state Eqs (2.7) and (2.8) with the boundary conditions (3.2) is numerically solved, and elastic displacements are determined $[u^e, w^e]_j$. Next, the program enters the creep damage loop which requires the effective stress intensity, the components of the damage and strain rates $[\sigma_{eff}, \dot{D}, \dot{\varepsilon}^c]_j$, Eqs (2.11) $\div$ (2.13), and the generalized inelastic forces $[N^{th}, M^{th}, N^c_{r/\vartheta}, M^c_{r/\vartheta}, Q^c]_j$, Eqs (2.6), are found. Repeating again the stage algorithm a solution of discretized thermo-creep-damage problem, rates of temperature $[\dot{T}]_{j,i}$ and displacements $[\dot{u}, \dot{w}]_j$ are computed. In the next time step the "new" temperature $[T]_{j,i}$ and displacements $[u, w]_j$ are found, and the process is continued until the maximum value of damage parameter reaches the critical level $\max[D]_j \cong 1$.

5. Results

5.1. Material data

The numerical results presented in this paper concern a plate made of Al-Mg-Si alloy of the following thermo-mechanical properties at the temperature of 483 K (cf Litewka (1989): $T_{f_0} = 300°\mathrm{C}$, $T_{ref} = 0°\mathrm{C}$, $E = 60.06\,\mathrm{GPa}$, $\sigma_0 = 149.5\,\mathrm{MPa}$, $\nu = 0.3$, $\alpha = 2.5\cdot 10^{-5}$, $\lambda_0 = 203\,\mathrm{W/m°C}$, $\beta = 15\,\mathrm{W/m^2°C}$, $m = 7.0$, $n = 3.7$, $C = 8.44\cdot 10^{-17}\,\mathrm{MPa}^{-n}\mathrm{h}^{-1}$. The characteristic parameters of the plate (thickness compared to diameter) and magnitude of the pressure are: $h/R = 0.2$, $p = 6.7\cdot 10^{-3}\sigma_0$, respectively.

6. Example

The effective stress type sensitivity to damage of aluminium alloy and combined thermo-mechanical loads cause that the first macrocrack appears at the central upper plate surface which is a region of compressive stress concentration (Fig.3).

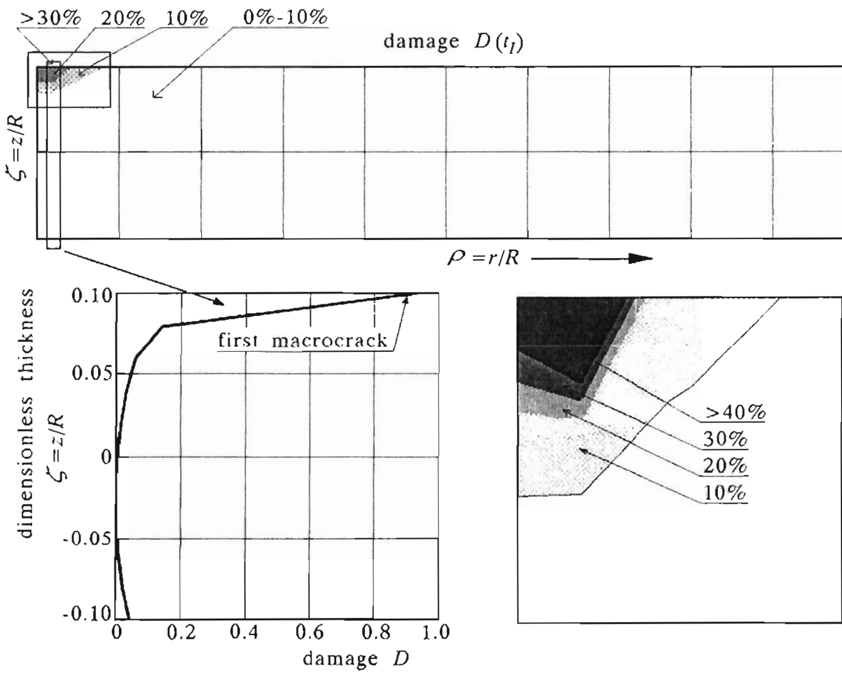

Fig. 3. Damage distribution in Reissner's plate under thermo-mechanical loadings

Damage accumulation causes substantial decrease in the coefficient of thermal conductivity $\lambda = \lambda_0(1 - D)$ and consequently decrease in temperature (Fig.4) and local heat flux (Fig.5). As the total heat flux transferring through the sidewall is constant the local heat flux in an undamaged region at the upper surface increases in order to satisfy the global condition of thermal balance (Fig.6). The region of most advanced damage becomes unable to conduct any heat (see Fig.5 and Fig.6).

However, in the model under consideration, where mechanical moduli are not affected by damage, the total effective stress in the most damaged zone (top) that results from both the bending compression and the thermal com-

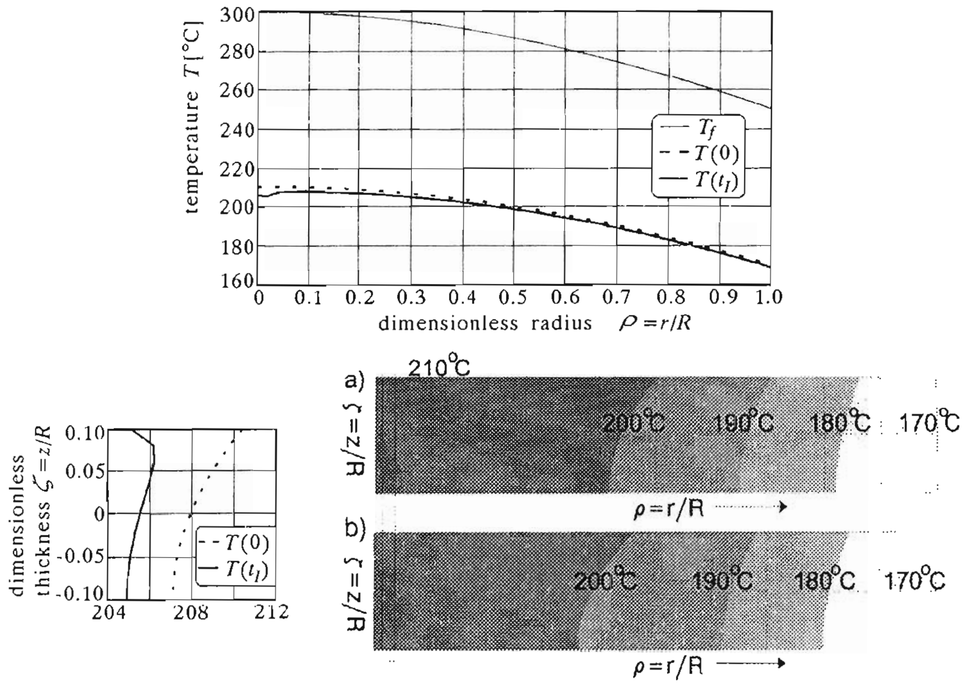

Fig. 4. Redistribution of temperature a) initial, b) at the moment of rupture

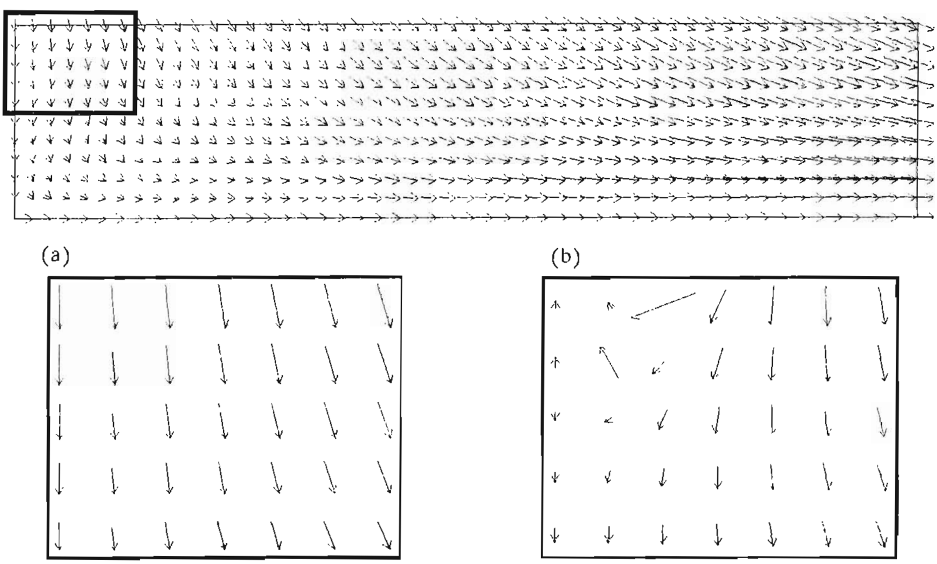

Fig. 5. Heat flux $\boldsymbol{q} = -\lambda_0(1 - D)\,\mathrm{grad}\,T$: (a) in the case of $t = 0$, (b) at the moment of rupture

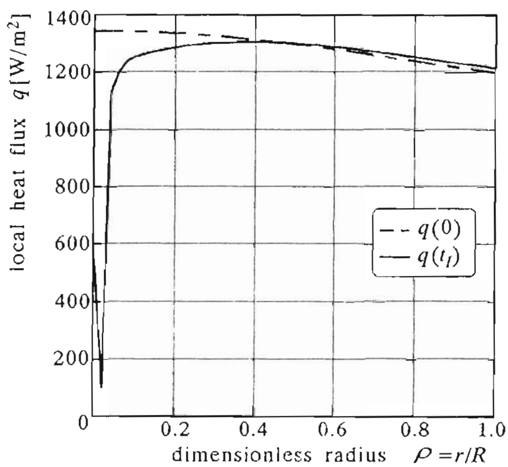

Fig. 6. Evolution of local heat flux entering the upper plate surface $q = \beta(1 - D)(T_\infty - T)$

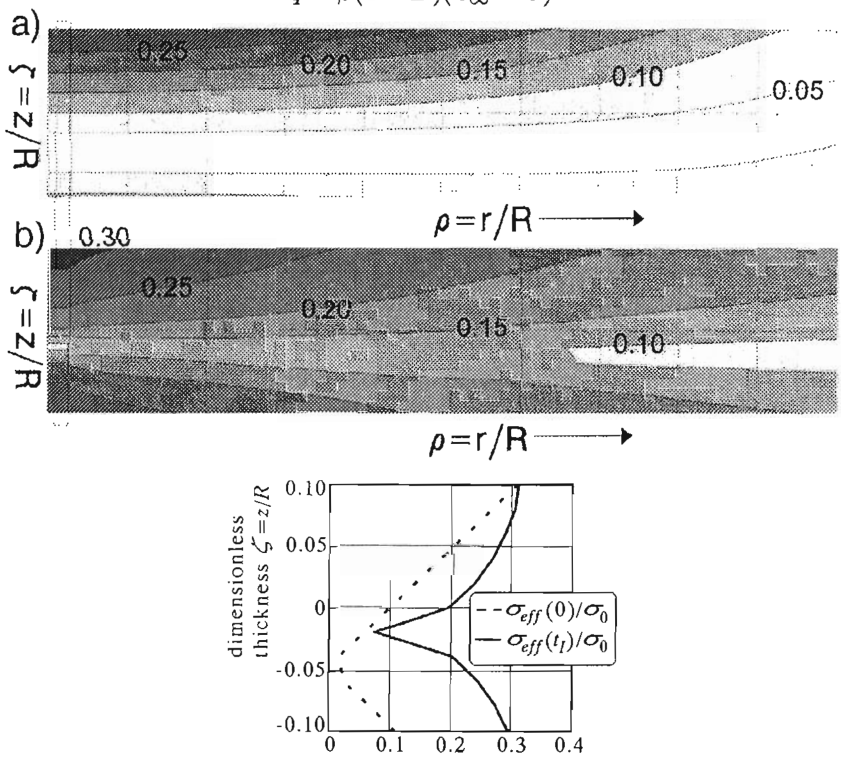

Fig. 7. Redistribution of von Mises effective stress a) initial "elastic", b) at the moment of rupture

pression is not released to zero, but remains almost unchanged, whereas in less damaged tensile zone (bottom) the effective stress at t_I is about three times as high as at the beginning $t = 0$, Fig.7.

7. Conclusions

- Heat flux through the damaged material is sensitive to a current damage state of the material. To describe this effect in the most general case both the tensor of thermal conductivity $\widetilde{\boldsymbol{\lambda}}$ and the tensor of thermal expansion $\widetilde{\boldsymbol{\alpha}}$ in the Fourier heat flux equation and mechanical state equations, respectively, should be functions of the damage tensor $\mathbf{D}$. However, in the case of aluminium alloy, when the damage growth is controlled by the effective stress σ_{eff}, the scalar damage parameter D is applicable and consequently the material remains isotropic during the creep-damage process.

- In the model under consideration, a completely damaged particle is free from the heat flux, but it has to carry non-zero stress resulting from the residual temperature field. To avoid this inconsistency it is necessary to incorporate the effect of damage on the mechanical constitutive moduli $\widetilde{\mathbf{E}}$, and consequently the damage influence on the membrane and bending stiffnesses $\widetilde{\mathcal{B}}$ and $\widetilde{\mathcal{D}}$. In this more consistent formulation some additional terms associated with derivatives of stiffnesses, as functions of damage parameter D, with respect to both radial co-ordinate r and time t must by taken into account. The approach mentioned above will be a subject of separate publication.

- In the example of Reissner's plate considered, significant stress redistribution accompanied by a moderate temperature change is observed.

- In the proposed model, a temperature jump at the fluid/solid interface is used to approximate the real heat exchange conditions. To describe precisely the fluid-solid interaction it is suggested to extend the model by taking into account the boundary layer effect as well as an environmental corrosion.

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Wpływ uszkodzeń na pola termo-mechaniczne w płycie średniej grubości

Streszczenie

Przedmiotem pracy jest analiza mechanizmów rozwoju uszkodzeń i ich wpływ na przepływ ciepła w płycie Reissner'a średniej grubości. Stosując sprzężenie efektów termicznych i uszkodzeń w kontynualnej mechanice zniszczenia rozpatruje się następujące dwuwymiarowe zagadnienia pól sprzężonych: transport ciepła w niejednorodnej, częściowo uszkodzonej, aluminiowej płyto-tarczy typu Reissner'a podlegającej kruchemu uszkodzeniu w warunkach złożonych obciążeń termo-mechanicznych.

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