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An Anisotropic Extension of Bodner's Model of Viscoplasticity: Model Development

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AN ANISOTROPIC EXTENSION OF BODNER'S MODEL OF VISCOPLASTICITY: MODEL DEVELOPMENT

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ABSTRACT

An anisotropic viscoplasticity model is developed as an extension of the well known Bodner model. The extension is made by replacing the effective stress of the isotropic Bodner model by one involving invariants for transverse isotropy. The resulting model retains the simplicity of Bodner's in the ease with which the material constants are determined experimentally. It allows a representation of strong initial anisotropy yet is based on a scalar state variable under the assertion that induced anisotropy is negligible relative to the strong initial anisotropy. Temperature dependence is taken as in the original Bodner theory. Account is made of fiber volume fraction through nonlinear rules of mixture applied to the stress history and anisotropy parameters. Focus is on the theoretical development of the model, however, application to a W/Cu composite is in progress and will be reported as a sequel to this report.

INTRODUCTION

Bodner's model of viscoplasticity [1,2] is one of several unified models that has received attention over the past decade. It was one of two models that was focused on under the extensive HOST program sponsored by NASA. In its earliest form, Bodner's model applies to initially isotropic materials and incorporates a single scalar state variable. In the terminology of continuum mechanics, such a model is labeled isotropically hardening. A major strength of the early Bodner model is its simplicity and the relative ease with which the material parameters can be determined through experiment.

The objective here is to develop a simple and tractable transversely isotropic model, applicable to metallic composites, by extending the well established Bodner model. This is accomplished, in part, by replacing the effective stress $\sqrt{3J_2}$ upon which the isotropic Bodner model is based with another effective stress defined in terms of invariants reflecting local transverse isotropy. Identification of an appropriate set of invariants for transverse isotropy was made by Robinson and Duffy [3]. The resulting model, capable of representing strong initial anisotropy, retains its dependence on a single scalar state variable. It is argued that induced anisotropy (e.g., through directional or kinematic hardening) is negligible relative to the strong initial anisotropy.

The anisotropic, viscoplasticity model developed here, like the isotropic Bodner model, is effective in representing rate-sensitive, non isothermal plastic responses typical of the histograms of rocket engines, the space-shuttle main engine (SSME) being a prominent example. Temperature dependence is taken as in the original Bodner model. Influence of the fiber/matrix volume fraction ρ is included by considering the internal variable (or stress history parameter) Z and the anisotropy parameter ζ to functionally depend on ρ.

Here, emphasis is on the theoretical development of the anisotropic model. Several plots analogous to those in [2] are shown illustrating general features of the anisotropic theory. An application of the model is in progress to a W/Cu composite for which tensile (and creep) data are available over a range of temperatures and strain-rates and two volume fractions. The resulting material constants and parameters for the W/Cu composite will be reported in a sequel to this report.

ISOTHERMAL STATEMENT OF THE MODEL

We first state the multiaxial form of the transversely isotropic model under isothermal conditions at a reference temperature T_O and for a reference fiber volume fraction ρ_o. The flow law is

$$\frac{\dot{\varepsilon}_{ij}}{\dot{\varepsilon}_o} = \frac{3}{2\overline{\sigma}} \exp\left[-\frac{1}{2}\left(\frac{Z}{\overline{\sigma}}\right)^{2n}\right]\Gamma_{ij} \tag{1}$$

and the evolution law is

$$\dot{Z} = m(Z_s - Z)\dot{W} \qquad \dot{W} = \Gamma_{ij}\dot{\varepsilon}_{ji} \tag{2}$$

in which $\dot{\varepsilon}_{ij}$ are the components of inelastic strain rate, $\overline{\sigma}$ is the effective stress

$$\overline{\sigma} = \sqrt{3(I_1 + (1-\xi)I_2 + \frac{3}{4}(1-\zeta)I_3)} \tag{3}$$

and Z is the internal state variable (stress history parameter).

The invariants I_1, I_2 and I_3 in (3) are defined as

$$I_1 = J_2 - \tilde{I} + \frac{1}{4}I^2 \qquad J_2 = \frac{1}{2}s_{ij}s_{ji}$$

$$I_2 = \tilde{I} - I^2 \qquad \tilde{I} = D_{ij}s_{jk}s_{ki}$$

$$I_3 = I^2 \qquad I = D_{ij}s_{ji}$$

also, $$\Gamma_{ij} = s_{ij} - \xi(D_{ki}s_{jk} + D_{jk}s_{ki} - 2ID_{ij}) - \frac{1}{2}\mathcal{I}(3D_{ij} - \delta_{ij}) \tag{4}$$

and $$0 \le \xi < 1 \qquad\qquad 0 \le \zeta < 1$$

s_{ij} are the components of deviatoric stress, D_{ij} are components of an orientation tensor, cf. [3], $\dot{\varepsilon}_o, n, m, Z_s$ and Z_o (the initial value of Z) are the material constants of viscoplasticity; ξ and ζ are constants relating to the degree of anisotropy.

The model expressed in (1),(2),(3) and (4) must be supplemented by special provisions allowing for stress reversals under cyclic loading conditions. It is expected that in metallic composite materials, as with monolithic alloys, a reversal of the sign of stress after unloading from an inelastic state is generally accompanied by a marked decrease in hardening. In the absence of detailed information on the inelastic cyclic response of metallic composites, we shall adopt the same provisions for stress reversals as are outlined in [1,2] for monolithic alloys.

EQUAL LONGITUDINAL AND TRANSVERSE SHEAR $\xi = 0$

A simpler form of the model results when the inelastic response in longitudinal and transverse shear can be idealized as being equal. This corresponds to taking the material parameter $\xi = 0$. In this case (1) and (2) remain the same, however, (3) and (4) become

$$\overline{\sigma} = \sqrt{3(J_2 - \frac{3}{4}\mathcal{I}^2)} \tag{5}$$

and $$\Gamma_{ij} = s_{ij} - \frac{1}{2}\mathcal{I}(3D_{ij} - \delta_{ij}) \tag{6}$$

respectively.

We shall use this simplified form in the application to W/Cu. It is interesting to note that the effective stress (5) relates directly to the anisotropic yield function used by Robinson and Pastor [4] in the limit analysis of a metallic composite ring.

FULL ISOTROPY $\xi = 0$ AND $\zeta = 0$

Additionally taking $\zeta = 0$ the model reduces to the fully isotropic form of Bodner [1,2]; (5) and (6) reduce to

$$\overline{\sigma} = \sqrt{3J_2} \tag{7}$$

and $$\Gamma_{ij} = s_{ij} \tag{8}$$

As the characterization of the Bodner model is based on uniaxial testing, we consider the uniaxial form of the isotropic model represented by (1),(2), (7) and (8). Thus,

$$\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o} = \exp\left[-\frac{1}{2}\left(\frac{Z}{\sigma}\right)^{2n}\right] \tag{9}$$

$$\dot{Z} = m(Z_s - Z)\sigma\dot{\varepsilon} \tag{10}$$

The constants

$$\dot{\varepsilon}_o, n, Z_s, m \text{ and } Z_o, \tag{11}$$

are found by correlating calculated responses based on (9) and (10) with tensile data obtained at strain-rates and temperatures of interest. Details of the established characterization procedure for the isotropic Bodner model are discussed in [2].

UNIAXIAL: LONGITUDINAL (0^0) STRESS

We return to the simplified ($\xi = 0$) anisotropic form (1),(2),(5) and (6). Reduction to uniaxial stress along the longitudinal (0^0) fiber direction gives

$$\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o^*} = \exp\left[-\frac{1}{2}\left(\frac{Z^*}{\sigma}\right)^{2n}\right] \tag{12}$$

$$\dot{Z}^* = m^*(Z_s^* - Z^*)\sigma\dot{\varepsilon} \tag{13}$$

with constants $$\dot{\varepsilon}_o^*, n, Z_s^*, m^* \text{ and } Z_o^* \tag{14}$$

We note that (12) and (13) with the constants (14) are identical in form to (9) and (10) with the constants (11). Thus, we can use the same characterization procedure on the anisotropic material under 0^0 testing to get the constants (14) as is used on the isotropic Bodner model to get the constants (11). As indicated earlier, this procedure is well established [2].

The constants (14) for 0^0 stress relate to those of the multiaxial model (1),(2),(5) and (6) as follows:

$$\dot{\varepsilon}_o^* = \dot{\varepsilon}_o\sqrt{1-\zeta} \qquad Z_s^* = \frac{Z_s}{\sqrt{1-\zeta}} \qquad m^* = m(1-\zeta) \qquad Z_o^* = \frac{Z_o}{\sqrt{1-\zeta}} \tag{15}$$

Fig.1 shows the dependence of the uniaxial flow stress parameter $\frac{\sigma}{Z^*} = \frac{\sigma\sqrt{1-\zeta}}{Z}$ under 0^0 stress on the strain rate parameter $\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o^*} = \frac{\dot{\varepsilon}}{\dot{\varepsilon}_o\sqrt{1-\zeta}}$ for different values of the constant n. With $\zeta = 0$ (isotropy) Fig. 1 reduces to that given in [2].

Thus, through (15), the multiaxial model is totally specified once the constants (14) have been determined by 0^0 testing and the value of the anisotropy parameter ζ is known. Determination of ζ will be discussed in a later section.

UNIAXIAL: TRANSVERSE (90^0) STRESS

Next, we specialize the ($\xi = 0$) model (1),(2),(5) and (6) for transverse (90^0) stress.There results

$$\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o^{**}} = \exp\left[-\frac{1}{2}\left(\frac{Z^{**}}{\sigma}\right)^{2n}\right] \tag{16}$$

$$\dot{Z}^{**} = m^{**}(Z_s^{**} - Z^{**})\sigma\dot{\varepsilon} \tag{17}$$

with constants $\dot{\varepsilon}_o^{**}, n, Z_s^{**}, m^{**}$ and Z_o^{**} (18)

Again, the form of (16),(17) with (18) is identical to that of the uniaxial Bodner model (9),(10) with (11), so the same characterization procedure also can be applied to 90^0 testing. The resulting constants (18) for 90^0 relate to the constants in the multiaxial model (1),(2),(5) and (6) according to:

$$\dot{\varepsilon}_o^{**} = \dot{\varepsilon}_o\sqrt{1-\frac{\zeta}{4}} \qquad Z_s^{**} = \frac{Z_s}{\sqrt{1-\frac{\zeta}{4}}} \qquad m^{**} = m\left(\frac{1-\frac{\zeta}{2}+\frac{\zeta^2}{4}}{1-\frac{\zeta}{4}}\right) \qquad Z_o^{**} = \frac{Z_o}{\sqrt{1-\frac{\zeta}{4}}} \tag{19}$$

A plot identical to Fig. 1 but for 90^0 stress is obtained by replacing $\frac{\sigma}{Z^*}$ by $\frac{\sigma}{Z^{**}}$ and $\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o^*}$ by $\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o^{**}}$. Again, the multiaxial model is totally specified once the constants (18) are determined from 90^0 testing and the value of ζ is known.

DETERMINATION OF ζ

Comparing (15) and (19) that relate the constants for 0^0 and 90^0 testing to those of the multiaxial model, it is apparent that ζ is specified once the sets of constants (14) and (18) are known. However, both sets need not be complete. If, for example, we know all the constants (14) from 0^0 testing and have only, say Z_o^{**} from 90^0 testing, ζ can be determined from

$$\frac{Z_o^{**}}{Z_o^*} = \sqrt{\frac{1-\zeta}{1-\frac{\zeta}{4}}} \tag{20}$$

We select Z_o^{**} (the initial value of the internal variable Z^{**}) in this context because it is one of the most easily obtained constants in the characterization procedure. The same could be done using the constant $\dot{\varepsilon}_o^{**}$, in which case ζ would be obtained from

$$\frac{\dot{\varepsilon}_o^{**}}{\dot{\varepsilon}_o^*} = \sqrt{\frac{1-\frac{\zeta}{4}}{1-\zeta}} \tag{21}$$

We note from (20) and (21) that

$$\dot{\varepsilon}_o^{**} Z_o^{**} = \dot{\varepsilon}_o^{*} Z_o^{*} \tag{22}$$

Evidently, ζ is over specified in that four equations like (20) and (21) can be written from (15) and (19). It is possible that one value of ζ cannot be found satisfying all four equations to a desired degree of accuracy. Our recourse then is to reexamine some of the idealizations made in the simplified ($\xi = 0$) model (1),(2), (5) and (6). An obvious consideration is to remove the condition $\xi = 0$ leaving the degree of anisotropy to depend on both ξ and ζ; the model is then (1),(2),(3) and (4), as originally stated. The simple characterization procedure just outlined, based directly on that established for the Bodner model, is not generally applicable to the more comprehensive model. An analogous but more involved procedure has been identified for this model; unavoidably it requires transverse and longitudinal shear testing to determine ξ. As this kind of test data on metallic composites, in particular W/Cu, are not readily available at present, we shall continue to assume $\xi = 0$ and to develop and apply the simplified model (1),(2),(5) and (6). Development and characterization of the more general anisotropic model including both ξ and ζ will be addressed as a continuation of this research.

In the context of the simplified ($\xi = 0$) model, Figs. 2-4 show the influence of the anisotropy parameter ζ on the transverse (90^0) stress response. Although these figures relate to transverse behavior, they are shown in terms of the uniaxial flow stress parameter $\frac{\sigma}{Z^*}$ and the strain rate parameter $\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o^*}$ for comparison with the 0^0 response. Figs. 2,3 and 4 correspond to $\zeta = 0.3$, 0.6 and 0.9, respectively. We see that the 90^0 flow stress parameter decreases as ζ increases for fixed values of strain rate and n. In particular, for $\zeta = 0.9$ and $n = 2$ (Fig. 4), $\frac{\sigma}{Z^*} \approx 0.2$ when $\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o^*} = 10^{-2}$. Comparing this with longitudinal (0^0) stressing for $n = 2$ (Fig. 1), $\frac{\sigma}{Z^*} \approx 0.6$ when $\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o^*} = 10^{-2}$. Thus, for the degree of anisotropy $\zeta = 0.9$ and with $n = 2$, the ratio of the flow stress for 0^0 to that for 90^0 is ≈ 3 for an inelastic strain rate of $\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o^*} = 10^{-2}$.

TEMPERATURE DEPENDENCE

As with the isotropic Bodner model, the primary influence of temperature on the flow stress is taken through the parameter $n(T)$. If we take $n(T) = \frac{C}{kT}$ where k is Boltzmann's constant, a thermal activation form results with the anisotropic activation energy function

$$H = kT\left(\frac{Z}{\bar{\sigma}}\right)^{\frac{2C}{kT}} \tag{23}$$

With full isotropy, $\xi = \zeta = 0$, (3) or (5) reduce to (7), and (23) becomes the activation energy function specified in [2].

Fig. 5 shows the dependence of the flow stress parameter for 0^0 uniaxial stress on $\frac{1}{n} = \frac{T}{C/k}$ for different values of the strain rate parameter. Again, for $\zeta = 0$ (isotropy) this plot reduces to its counterpart given in [2]. .

It may prove necessary to include temperature dependence in other material parameters, e.g., Z_o (the initial value of the internal variable). However, in the application to W/Cu we shall attempt to limit temperature dependence to the parameter n.

FIBER VOLUME FRACTION ρ

The characterization procedure described above is defined as being applicable to a composite material having the reference fiber volume fraction ρ_o. A composite having the same constituents but a different fiber volume fraction ρ can be viewed as a distinct material and the same characterization procedure applied to it, yielding another set of constants and parameters. In principle, this can be repeated for a range of fiber volume fractions of interest with the resulting *constants* fit in some optimal sense as functions of ρ. This may be necessary in practice because the precise fiber volume fraction is often not known *a priori* for a structure fabricated from a composite material. Indeed, ρ may vary from point to point in a given body or structure. A constitutive model to be used in the analysis of such structures must allow for variations in ρ.

To avoid a large amount of tedious and costly characterization testing, we can appeal either to micromechanical studies to provide guidance of how the overall composite response changes with fiber volume fraction ρ, or we can attempt to limit the amount of phenomenological testing by judiciously identifying the key parameters in which the ρ dependence should reside. Here, we pursue the latter course within the context of the simplified $\xi = 0$ model (1),(2),(5) and (6).

Observations based on data for two fiber volume fractions of a W/Cu composite suggest that the primary dependence on ρ should be taken in the state variable (or stress history parameter) $Z^*$and in the anisotropy parameter ζ. These dependencies are taken as:

$$Z^*(\rho) = Z^*(\rho_o)\Psi(\rho) \tag{24}$$

$$\zeta(\rho) = \zeta(\rho_o)\Phi(\rho) \tag{25}$$

where ρ_o is the reference fiber volume fraction and Ψ and Φ can be considered *nonlinear rules of mixture*. Of course, $Z^*(\rho_o)$ in (24) is identical to Z^* in (12)-(14).

Consistent with the basic features of the model, exponential forms are chosen for Ψ and Φ as follows:

$$\Psi(\rho) = \left(1 - \frac{Z^*(0)}{Z^*(\rho_o)}\right)\left(1 - \exp(-\frac{\alpha\rho}{\rho_o - \rho})\right) + \frac{Z^*(0)}{Z^*(\rho_o)} \tag{26}$$

$$\Phi(\rho) = 1 - \exp(-\frac{\beta\rho}{\rho_o - \rho}) \tag{27}$$

in which α and β are constants.

From (24)-(27), we see that as $\rho \to \rho_o$, $Z^* \to Z^*(\rho_o)$ and $\zeta \to \zeta(\rho_o)$, relating as earlier, to the reference volume fraction. As $\rho \to 0$ (no fibers), $Z^* \to Z^*(0)$ corresponding to the matrix material, and $\zeta \to 0$ corresponding to isotropy.

Figs. 6 and 7 are plots of $\Psi(\rho) vs. \frac{\rho}{\rho_o}$ and $\Phi(\rho) vs. \frac{\rho}{\rho_o}$, respectively, where for illustration we have taken $Z^*(0)/Z^*(\rho_o) = 1/3$, $\alpha = 1/2$ and $\beta = 3$. Fig. 8 shows the dependence of the flow stress parameter $\frac{\sigma}{Z^*}$ on the strain rate parameter $\frac{\dot{\varepsilon}}{\dot{\varepsilon}_o}$ for $n = 2$ and for three values of the fiber volume fraction $\rho = \rho_o$, $\frac{2}{3}\rho_o$ and $\frac{1}{3}\rho_o$. Values of the other relevant parameters in Fig. 8 are taken as in Figs. 6 and 7. The three upper curves in Fig. 8 are for longitudinal (0^0) stress; the three lower curves are for transverse (90^0) stress. As expected intuitively, both the 0^0 and 90^0 flow stress parameters decrease with decreasing fiber volume fraction. Also as expected, the change in the flow stress with ρ is considerably greater for 0^0 than for 90^0 ($\approx$4 times greater in this example).

The additional constants introduced in (24)-(27), viz., $Z^*(0)$ (or equivalently, $Z_o^*(0)$ and $Z_s^*(0)$ - the initial and saturation values of the state variable for the matrix), α and β, must be determined using experimental results from tests on, at least, one fiber volume fraction other than the reference ρ_o .

SUMMARY AND CONCLUSIONS

A simple and tractable, transversely isotropic viscoplasticity model is developed by extending the well known isotropic Bodner model. The model retains the simplicity of the Bodner theory regarding the relative ease with which the material constants and parameters can be determined. It is capable of representing strong initial anisotropy yet is based on a single scalar state variable under the assertion that induced anisotropy through directional or kinematic hardening can be ignored relative to the strong initial anisotropy. Like the original Bodner model, the anisotropic model is particularly effective in representing rate-sensitive, non isothermal viscoplastic responses typical of the histograms of rocket engines, e.g., the SSME.

A procedure for determining the constants of the model is discussed and is shown to be essentially identical to the well established characterization procedure for the Bodner model. Temperature dependence is taken as in the original Bodner theory. Account is made of varying fiber volume fraction by including its dependence specifically in the state variable Z (stress history parameter) and the anisotropy parameter ζ.

Application of the model to a W/Cu composite is in progress. The results specifying the material constants and parameters will be reported as a sequel to this report.

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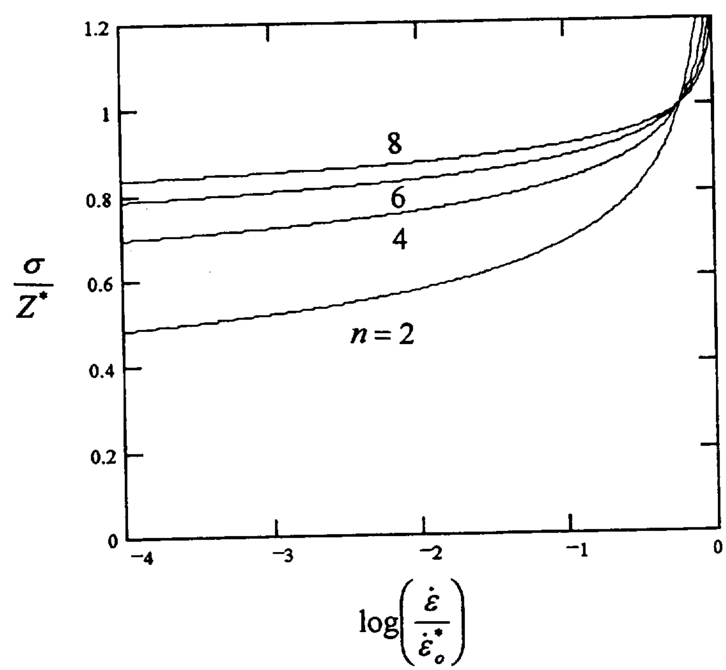

Fig. 1. Dependence of the uniaxial flow stress parameter on the strain rate parameter under 0° stress for different values of n.

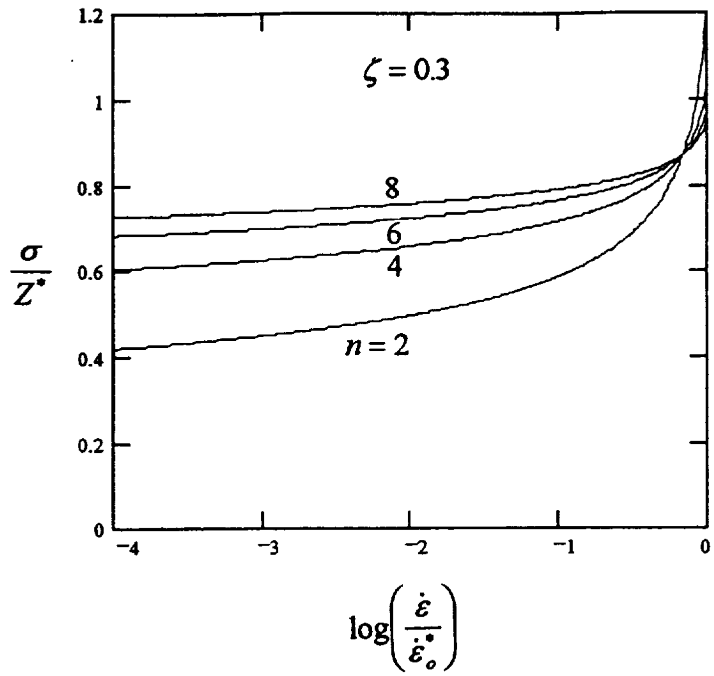

Fig. 2. Dependence of the uniaxial flow stress parameter on the strain rate parameter under 90^0 stress for different values of n and $\zeta = 0.3$.

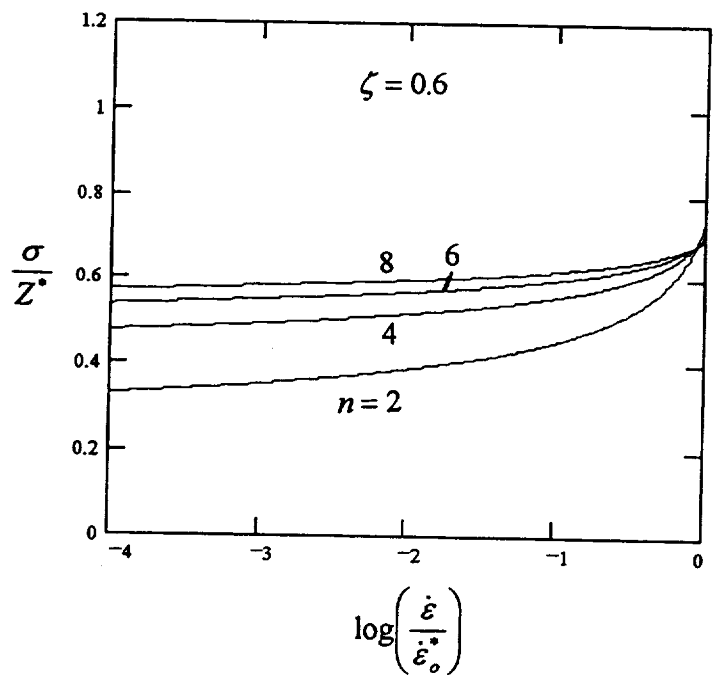

Fig. 3. Dependence of the uniaxial flow stress parameter on the strain rate parameter under 90^0 stress for different values of n and $\zeta = 0.6$.

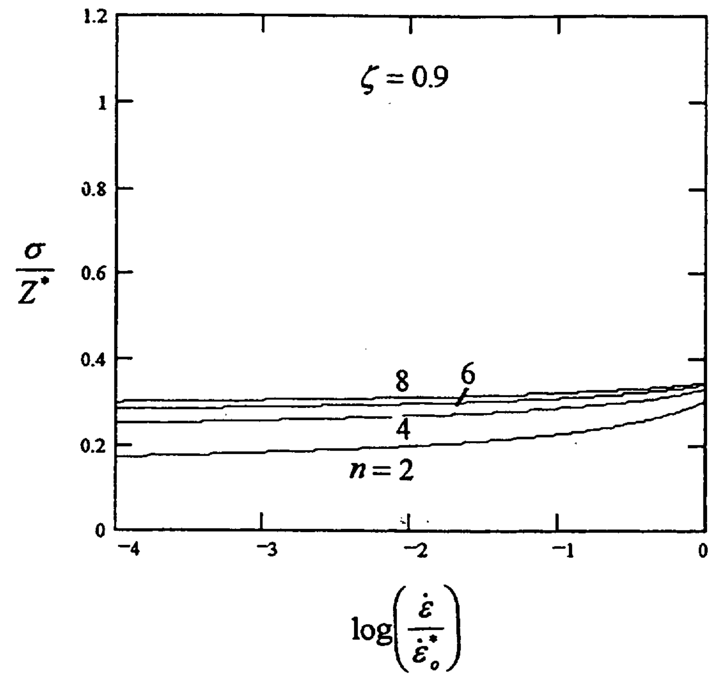

Fig. 4. Dependence of the uniaxial flow stress parameter on the strain rate parameter under 90^0 stress for different values of n and $\zeta = 0.9$.

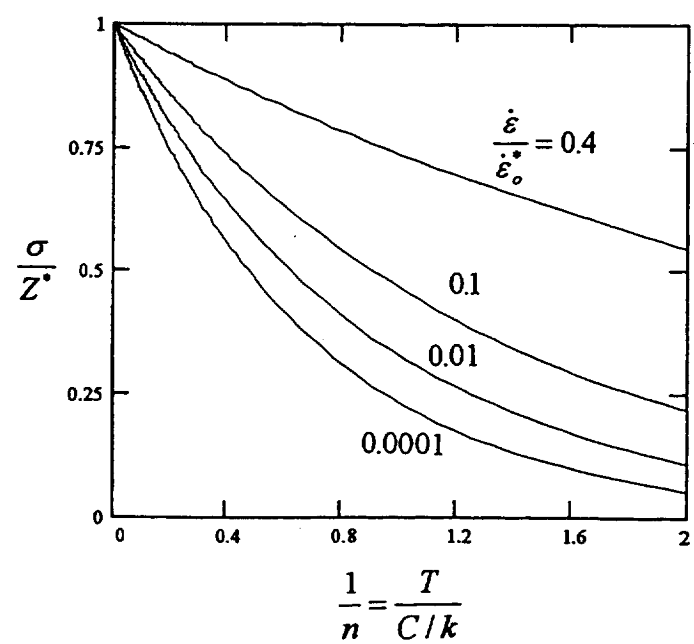

Fig. 5. Dependence of the uniaxial flow stress parameter on $\frac{1}{n} = \frac{T}{C/k}$ under 0^0 stress for different values of the strain rate parameter.

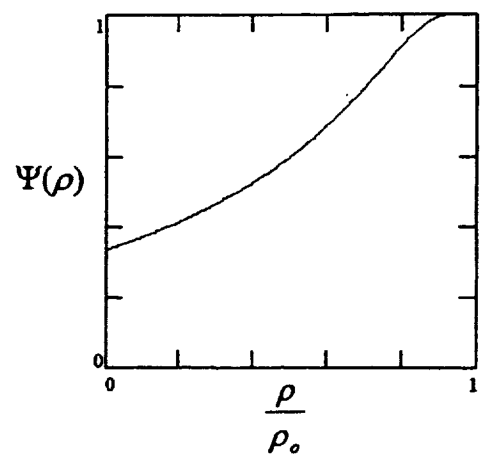

Fig. 6. $\Psi(\rho) vs. \rho / \rho_o$ with $Z^*(0)/Z^*(\rho_o) = 1/3$ and $\alpha = 1/2$ in (26).

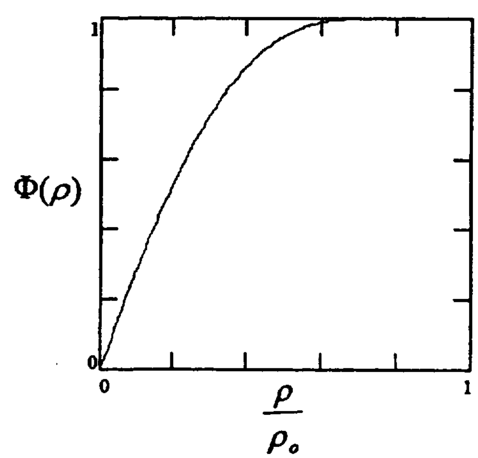

Fig. 7. $\Phi(\rho) vs. \rho / \rho_o$ with $\beta = 3$ in (27).

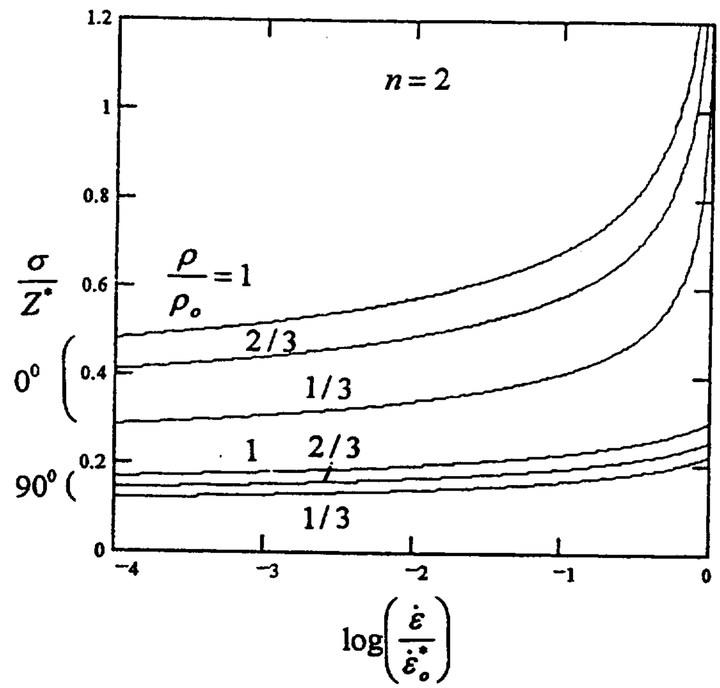

Fig. 8. Dependence of the uniaxial flow stress parameter on the strain rate parameter under 0^0 and 90^0 stress with $n=2$ and $\rho=\rho_o$, $\frac{2}{3}\rho_o$ and $\frac{1}{3}\rho_o$.

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