ISSN 0021-8944, Journal of Applied Mechanics and Technical Physics, 2015, Vol. 56, No. 2, pp. 313–320. © Pleiades Publishing, Ltd., 2015.
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EXPERIMENTAL AND THEORETICAL STUDIES OF THE INFLUENCE OF A TENSILE LOAD ON THE RELAXATION OF RESIDUAL STRESSES IN A HARDENED CYLINDRICAL SPECIMEN UNDER CREEP CONDITIONS

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UDC 539.376:539.4.014.13

Abstract: This paper presents an experimental and theoretical study of the influence of a tensile load on the relaxation of residual stresses in a hardened cylindrical specimen of ZhS6KP alloy under creep conditions at 800°C. An experimental study was conducted to investigate the distribution of the axial residual stress tensor component across the thickness of the hardened layer after hardening by air shot blasting using microbeads and after creep loading for 50 and 200 h under a tensile load of 150 and 250 MPa. A detailed theoretical analysis of the problem was performed. In all loading regimes, the calculated and experimental values of the residual stresses were found to be in good agreement. It was shown that at low tensile load, the relaxation rate decreased in comparison with the case of thermal exposure in the absence of a tensile load and, with increasing load intensity, it increased.

Keywords: cylindrical specimen surface plastic strain, residual stress, tensile load, creep, relaxation.

DOI: 10.1134/S0021894415020170

INTRODUCTION

It is known that at normal and medium temperatures, surface plastic deformation has a positive effect on the durability of machine parts [1–6]. Furthermore, an increase in the fatigue resistance is due mainly to the presence of compressive residual stresses in the surface layer. A more complicated situation arises in ascertaining the feasibility of using different methods of surface plastic deformation of parts operating at high temperatures (e.g., components of gas turbine engines). Operating conditions have a significant effect on the state of the hardened layer: due to creep under the action of loads and temperatures, there is a change in the residual stresses in time during rheological deformation of the structure itself. Since the effectiveness of surface plastic deformation methods is determined by the resistance of residual stresses to temperature-power loading, the study of residual stress relaxation is an important theoretical and applied problem. Furthermore, in practice, the rate (time) of complete relaxation can be used to diagnose the expiration of the service life of, e.g., gas turbine engine blades [7].

In [6, 8], an approximate method for solving the boundary-value problem of the relaxation of residual stresses in a stretched hardened cylindrical specimen is proposed based on decomposition of the structure into a thin hardened layer and its body. It was assumed that the hardened layer is glued onto the surface of the cylinder and is deformed together with it in a hard loading regime for given deformation values on the cylinder surface.

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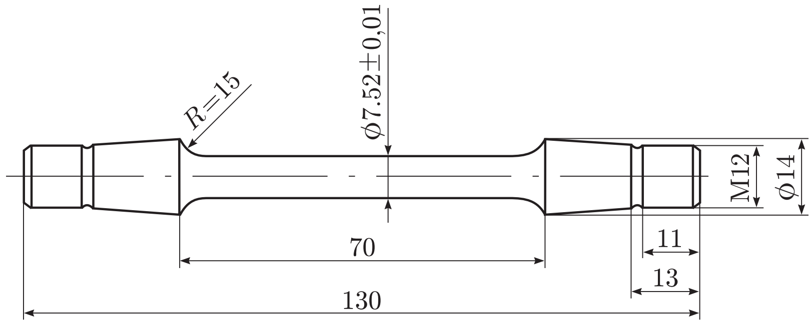

Fig. 1. Experimental specimen.

In [9], a direct numerical method of solving this problem was proposed and verified experimentally for cylindrical specimens of EI691 alloy at 400°C under thermal exposure without load. Experimental studies have been performed under thermal exposure conditions [2, 9], and the effect of tensile loads on the relaxation of residual stresses under creep conditions practically has not been carried out.

This paper presents an experimental and theoretical study of the influence of tensile stresses on the relaxation of residual stresses in cylindrical specimens of ZhS6KP alloy under conditions of high temperature creep at a temperature of 800°C.

1. METHOD OF EXPERIMENTAL STUDY

This section provides the characteristics of the test specimens and the experimental procedure.

1.1. Test Specimens

The specimens of ZhS6KP alloy had the shape and dimensions shown in Fig. 1 (M12 is the typical size of the thread for attaching the specimen in the test machine). The selected shape allowed determining the residual stresses after hardening and exposure at elevated temperatures and performing tensile creep tests. Blanks for the specimens were cut from a rod and heated at a temperature of 1220°C in for 4 h with subsequent cooling in air, aging at a temperature of 950°C for 2 h, and cooling in air. After that, the initial and final turning of the working part of the cylindrical portion of the specimen was conducted. The grinding allowance was 0.4 mm, and the surface roughness was $R_a = 0.32$–0.63 μm. The allowance for polishing was 0.06 mm. In polishing, 0.03 mm was left for final finishing. After polishing, the specimens were heated at a temperature of 950°C for 4 h and then cooled in air. The last operation was final polishing, in which the roughness was maintained at a level $R_a = 0.32$ μm. This technology rules out the occurrence of residual stresses.

1.2. Hardening of Specimens

Residual stresses were induced in the surface layer of the specimens using the air shot blasting of the surface with 160–200 μm diameter microbeads made of SH-15 material at an air pressure of 0.3 MPa, a flight speed of microbeads of 76 m/s, and a hardening time of 45 s.

1.3. Determining Residual Stresses after Treatment with Microbeads

Residual stresses were determined by the method of rings and strips proposed in [1, 10, 11]. This method was further developed in [2, 12]. In the method, the hardened specimen is bored out to make bushings, which are then cut along the generatrix to produce rings and strips. In the cut strips, the deflection is measured, and in the cut rings, the change in the diameter. Next, the outer layers of the rings and strips are removed (etched) by electrochemical polishing and the changes in the diameter of the rings and the deflections of the strips are simultaneously measured. The measured values are used to evaluate the circumferential and axial residual stresses using the procedure described in [2, 10, 12].

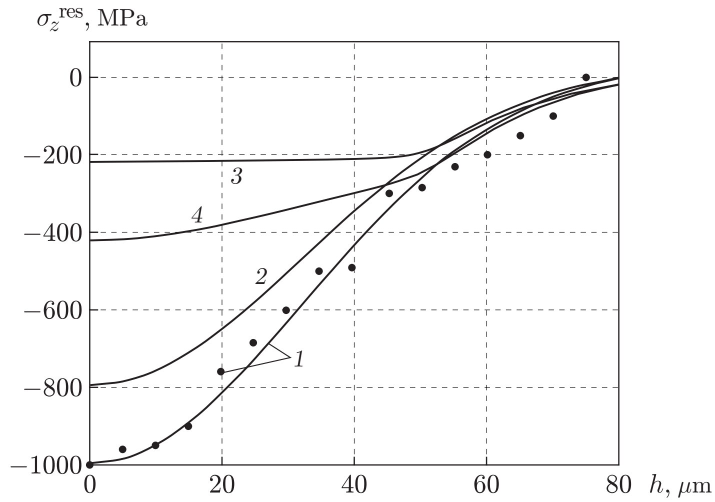

Fig. 2. Calculated (curves) and experimental (points) diagrams of residual stresses $\sigma_z^{\rm res}(h)$ in a cylindrical specimen of radius $a = 3.76$ mm under thermal exposure ($T = 800°$Cand $N = 0$): (*1*) after hardening at time $t = 0 - 0$; (*2*) after thermal loading at time $t = 0 + 0$; (*3*) after thermal loading at time $t = 50 - 0$ h; (*4*) after thermal unloading at the time $t = 50 + 0$ h.

The measured residual stresses after the treatment with microbeads averaged over three specimens are presented in Fig. 2 ($h = a - r$ is the thickness of the hardened layer, a is the radius of the specimen, and r is the radial coordinate).

1.4. Determining Residual Stresses after Creep Loading at Elevated Temperatures

For the experimental study of the relaxation of residual stresses resulting from creep in specimens hardened with microbeads at a temperature of 800°C these specimens were allowed to stay without load ($N = 0$) and under tensile axial distributed loads $N = 150$ and 250 MPa for 50 and 200 h in a high temperature furnace. After 50 h, we determined the distribution of residual axial stresses across the thickness of the hardened layer and, at time $t = 200$ h, the maximum absolute values of compressive stresses on the specimen surface. It should be noted that the determination of residual stresses after keeping the specimens at a temperature of 800°C was carried out after thermal unloading (cooling to room temperature). In Fig. 3, points show the experimental distribution of the axial component of the stress tensor σ_z across the thickness of the hardened layer after creep for 50 h under tensile loads $N = 150$ and 250 MPa. The following are the maximum absolute values of the experimental residual stress σ_z on the surface of a cylindrical specimen of radius $a = 3.76$ mm:

$\sigma_{z\,\max} = -370$ MPa at an exposure time $t_{\rm e} = 50$ h ($N = 150$ MPa);
$\sigma_{z\,\max} = -470$ MPa at $t_{\rm e} = 50$ h ($N = 250$ MPa);
$\sigma_{z\,\max} = -280$ MPa at $t_{\rm e} = 200$ h ($N = 150$ MPa);
$\sigma_{z\,\max} = -300$ MPa at $t_{\rm e} = 200$ h ($N = 250$ MPa).

From the analysis of the data shown in Figs. 2 and 3, it follows that under a constant load, the stress relaxation of residual stresses is slowing. This result appears to be due to the fact that after hardening, the self-equilibrated field of residual stresses is superimposed by the stress caused by the applied load. However, unambiguous conclusions can be drawn only after a comprehensive theoretical analysis. The aim of this study is to investigate the relaxation process using the method developed in [9] to solve this boundary-value problem.

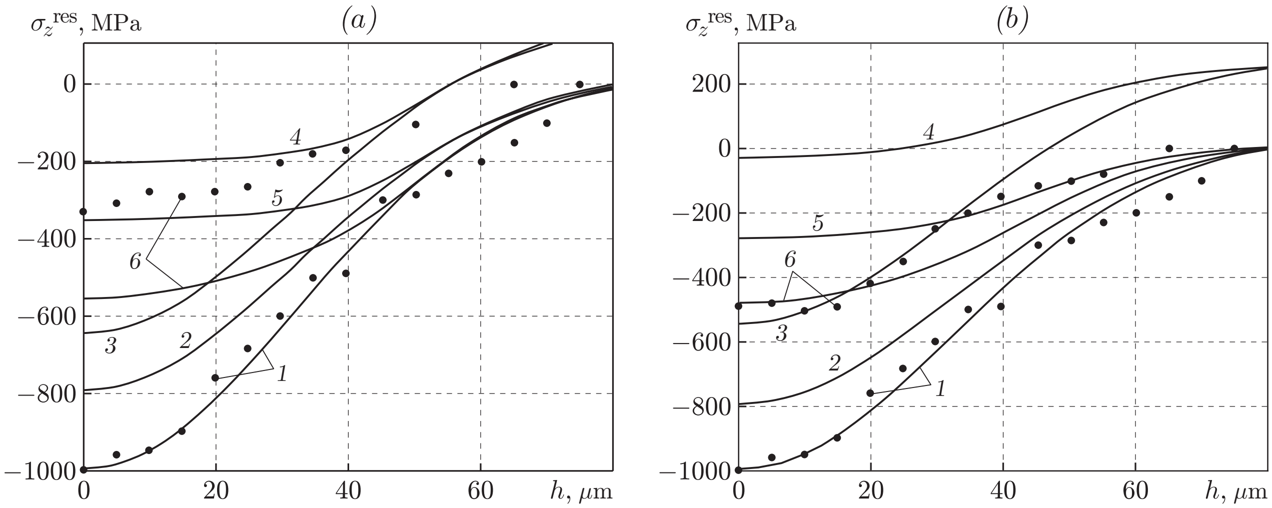

Fig. 3. Calculated (curves) and experimental (points) diagrams of residual stresses $\sigma_z^{\rm res}(h)$ at a temperature ($T = 800°$C) and power loads: (a) $N = 150$ MPa, (b) $N = 250$ MPa; (*1*) after hardening at time $t = 0 - 0$; (*2*) after thermal loading at time $t = 0 + 0$; (*3*) after thermal-power loading at time $t = 0+0$; (*4*) after creep under thermal-power loading at time $t = 50-0$ h; (*5*) after power unloading at time $t = 50 + 0$ h; (*6*) after thermal-power unloading at time $t = 50 + 0$ h.

2. CALCULATION OF THE INITIAL STRESS–STRAIN STATE AFTER SURFACE PLASTIC DEFORMATION AND TEMPERATURE HEATING

We consider a solid cylindrical specimen of radius a, in which residual stress and plastic strain fields were induced in the surface layer by air shot blasting at normal temperature T_0. Then the specimen was heated to temperature T_1 (steady-state temperature field in the cylindrical specimen is considered). The problem is solved in cylindrical coordinates (r, θ, z). We denote the radial, circumferential, and axial residual stresses by $\sigma_r^{\rm res}$, $\sigma_\theta^{\rm res}$, and $\sigma_z^{\rm res}$, and the corresponding tensor components of the residual plastic strain after hardening by q_r, q_θ, and q_z. The off-diagonal components of the residual stress and plastic strain tensors will be neglected because of their smallness compared to the diagonal components. Under the assumption of no secondary plastic strain in the compression region of the surface layer and based on experimental data, the component $\sigma_\theta^{\rm res}(r)$ was determined, and in [6, 8, 12, 13] for the remaining components of the residual stresses and plastic strain tensors, the following dependences were obtained:

$$\sigma_r^{\rm res}(r) = -\frac{1}{r}\int_r^a \sigma_\theta^{\rm res}(\xi)\,d\xi; \tag{1}$$

$$q_\theta(r) = \frac{(1+\nu)(1-2\nu)}{E(1+a\nu)^2}\,r^{-\beta}\int_0^r \xi^{\beta-1}\Big[\sigma_r^{\rm res}(\xi) + (1+\alpha)\sigma_\theta^{\rm res}(\xi)\Big]\,d\xi$$

$$-\frac{1+\nu}{E(1+\alpha\nu)}\Big[(1-\nu)\sigma_\theta^{\rm res}(r) - \nu\sigma_r^{\rm res}(r)\Big], \qquad \beta = \frac{2+\alpha}{1+\alpha\nu}; \tag{2}$$

$$q_z(r) = \alpha q_\theta(r), \qquad q_r(r) = -(1+\alpha)q_\theta(r); \tag{3}$$

$$\varepsilon_z^0 = \frac{2}{a^2}\int_0^a r\Big\{q_z(r) - \frac{\nu}{E}\Big[\sigma_r^{\rm res}(r) + \sigma_\theta^{\rm res}(r)\Big]\Big\}dr; \tag{4}$$

$$\sigma_z^{\rm res}(r) = E\Big(\varepsilon_z^0 - q_z(r)\Big) + \nu\Big(\sigma_r^{\rm res}(r) + \sigma_\theta^{\rm res}(r)\Big), \tag{5}$$

Here E is Young's modulus, ν Poisson's ratio, α is the phenomenological hardening anisotropy parameter, whose determination is described in [12, 13] (in the case of air shot blasting with microbeads, $\alpha = 1$); the distributions of the stresses $\sigma_\theta^{\rm res}(r)$ and $\sigma_z^{\rm res}(r)$ almost coincide [6].

Thus, the calculation of the residual stress and plastic strain fields in a solid cylinder after hardening of its surface (at time $t = 0 - 0$) can be schematically represented as follows:

$$\sigma_\theta^{\rm res}(r) \xrightarrow{(1)} \sigma_r^{\rm res}(r) \xrightarrow{(2)} q_\theta(r) \xrightarrow{(3)} q_z(r), q_r(r) \xrightarrow{(4)} \varepsilon_z^0 \xrightarrow{(5)} \sigma_z^{\rm res}(r). \tag{6}$$

Here the arrows show the sequence of determining the quantities; numbers above the arrow denote the number of formulas by which these values are determined. From scheme (6) it follows that the components $\sigma_r^{\rm res}$, $\sigma_z^{\rm res}$, q_θ, q_r, and q_z are ultimately determined in terms of the quantity $\sigma_\theta^{\rm res}$ and the parameter α.

Let the temperature T of the cylindrical specimen increase from value T_0 to value T_1, and $T_1 \gg T_0$. We denote by E_0 and E_1 the Young's moduli at temperatures T_0 and T_1, respectively; it is obvious that $E_1 < E_0$. We assume that increasing the temperature does not lead to additional plastic deformation in the surface hardened layer of the cylindrical specimen due to thermal softening of the plastic material. Under this assumption, the component $\sigma_\theta^{\rm res}$ at $T = T_1$ can be determined by solving the integral equation (2) using (1) for $E = E_1$ since the quantity $q_\theta(r)$ is known and does not depend on temperature. Since the solution of this problem is complex, we use the following method which reduces the problem considered to the problem of fictitious creep. Assume that during heating, the Young's modulus varies as

$$E(\tau) = E_0 + (1 - {\rm e}^{-\tau})(E_1 - E_0), \tag{7}$$

where τ is a fictitious time (loading parameter). At $\tau > 10$, the value of ${\rm e}^{-\tau} \approx 0$ and the value of $E(\tau) = E_1$ corresponds to temperature T_1, and at $\tau = 0$, the value of $E(0) = E_0$ corresponds to temperature T_0. Then, with the notation

$$\sigma_0^{\rm res}(r,\tau) = \sigma_\theta^{\rm res}(r,\tau) + \sigma_z^{\rm res}(r,\tau) + \sigma_r^{\rm res}(r,\tau),$$

$$e_i^0(r,\tau) = \Big((1+\nu)\sigma_i^{\rm res}(r,\tau) - \nu\sigma_0^{\rm res}(r,\tau)\Big)/E_0, \qquad i = r, \theta, z,$$

$$E^* = (E_0 - E_1)/E_0$$

and using relation (7) we have

$$e_i(r,\tau) = \frac{(1+\nu)\sigma_i^{\rm res}(r,\tau) - \nu\sigma_0^{\rm res}(r,\tau)}{E(\tau)} = \frac{e_i^0(r,\tau)}{1 - (1 - {\rm e}^{-\tau})E^*}, \quad i = r, z, \theta.$$

Expanding the second factor in the last equality in a Taylor series, and retaining terms of the first order of smallness with respect to E^*, we obtain

$$e_i(r,\tau) = e_i^0(r,\tau) + e_i^0(r,\tau)(1 - {\rm e}^{-\tau})E^*, \qquad i = r, \theta, z. \tag{8}$$

The second term on the right side of (8) will be called the fictitious creep strain (pseudo-creep) and denoted by $h_i(r,\tau)$:

$$h_i(r,\tau) = \frac{E^*}{E_0}\Big((1+\nu)\sigma_i^{\rm res}(r,\tau) - \nu\sigma_0^{\rm res}(r,\tau)\Big)(1 - {\rm e}^{-\tau}), \qquad i = r, \theta, z.$$

Differentiating this equation with respect to time, we get

$$\dot h_i(r,\tau) = \frac{E^*}{E_0}\Big((1+\nu)\sigma_i^{\rm res}(r,\tau) - \nu\sigma_0^{\rm res}(r,\tau)\Big) - h_i(r,\tau), \qquad i = r, \theta, z, \tag{9}$$

where the dot denotes the derivative with respect to the loading parameter τ.

Relations (9) are similar in form to the relations of the hereditary theory of viscoelasticity with an exponential creep kernel and initial data $h_i(r, 0) = 0$, where $i = r, \theta, z$ [14].

Representing the total strain $\varepsilon_i(r,\tau)$ as

$$\varepsilon_i(r,\tau) = e_i^0(r,\tau) + q_i(r) + h_i(r,\tau), \qquad i = r, \theta, z, \tag{10}$$

we can calculate the stress–strain state in the process of heating of a cylindrical product to temperature T_1 using the forward method developed in [9] to solve the boundary-value creep problem for a hardened cylindrical specimen, and as the final solution we use the asymptotic solution for $\tau \to +\infty$ (in practice, this solution corresponds to values $\tau > 10$, because, in this case, $e^{-\tau} \approx 0$). It should be noted that the temperature deformations in (10) are neglected as they do not influence on the stress state because of the homogeneity of the temperature field distribution over the volume of the cylindrical specimen.

Implementation of the numerical method [9] in solving the fictitious creep problem allows finding the residual stress tensor components σ_i^{res}, $i = r, z, \theta$ at time $t = 0 + 0$ at temperature T_1 (we assume that the heating of the cylindrical specimen occurred instantaneously).

3. NUMERICAL SOLUTION OF THE PROBLEM AND ANALYSIS OF THE RESULTS

This section provides a numerical solution of the problem of the relaxation of residual stresses in the hardened layer of a solid cylindrical specimen of ZHS6KP alloy at 800°C in axial tension under creep conditions. In the theoretical analysis of the results of these experimental studies (see Section 1), we performed a numerical calculation of the kinetics of residual stresses in the hardened cylindrical specimen of ZHS6KP alloy at a temperature of 800°C in creep under tensile distributed loads $N = 150$ and 250 MPa and under thermal exposure conditions ($N = 0$).The method of solving this problem is described in [9]. Steady creep was modeled by the following rheological relations:

$$\dot{p}_{ij} = \frac{3}{2}\, cS^{m-1}\Big(\sigma_{ij} - \frac{1}{3}\,\delta_{ij}\sigma_{kk}\Big). \tag{11}$$

Here, p_{ij} and σ_{ij} are the stress tensor and creep strain components, respectively; S is the stress intensity; c and m are parameters. To determine the parameters of model (11) and implement the numerical procedure of solving the creep problem for the hardened specimen, it is necessary to have experimental data on the creep of ZHS6KP alloy at temperatures of 800°C, but such information is available only for temperatures of 900, 950, and 1000°C [15, 16]. Based on these data [17], a stochastic model of non-isothermal creep was constructed for said material. Neglecting the first stage of creep (because of its short duration), given that the third stage in the time interval from 0 to 50 h is absent, and by extrapolating the corresponding temperature dependences for the mathematical expectations of the parameters c and m model (11), we determined their values at $T = 800$°C: $c = 5.454 \cdot 10^{-29}$ MPa^{-m}, and $m = 9.815$.

Note that according to scheme (6), the initial information for the calculation of the residual stress and plastic strain fields in a cylindrical specimen of radius a is the circumferential component $\sigma_\theta^{\mathrm{res}}(r)$, which, according to [6, 8], can be approximated as

$$\sigma_\theta^{\mathrm{res}}(r) = \sigma_0 + \sigma_1 \exp\Big(-(a-r)^2/b^2\Big),$$

where σ_0, and σ_1, b are approximation parameters.

Since, in this case, experimental data for the component $\sigma_z^{\mathrm{res}}(r)$ are available, and for the component $\sigma_\theta^{\mathrm{res}}(r)$ they are absent, the parameters σ_0, σ_1, and b were varied, and for each set of their values, a numerical calculation was carried out by scheme (6) until the minimum was reached for the functional of the standard deviation of the calculated values of σ_z^{res} from the experimental values (points in Fig. 2). As a result, the following values of the parameters were obtained: $\sigma_0 = 19.3$ MPa, $\sigma_1 = 1019.3$ MPa, and $b = 0.08$ mm.

Figure 2 shows the results of calculating the value of $\sigma_z^{\mathrm{res}}(h)$ by scheme (6) at time $t = 0 - 0$ after hardening (curve *1*), the results of calculation at time $t = 0 + 0$ after thermal loading (curve *2*), at time $t = 50 - 0$ h after creep at a temperature of 800°C under thermal exposure ($N = 0$) (curve *3*), and at time $t = 50 + 0$ h after unloading to the normal temperature (20°C) (curve *4*). In the calculations, we used the following values of the Young's modulus: $E_0 = 2 \cdot 10^5$ MPa at a temperature of 20°C, and $E_1 = 1.492 \cdot 10^5$ MPa at a temperature of 800°C. The Poisson's ratio was set equal to $\nu = 0.3$.

Similar information for cylindrical specimens under tensile axial distribution loads $N = 150$ and 250 MPa is shown in Fig. 3. Comparing the final calculated distributions of the stresses $\sigma_z^{\mathrm{res}}(h)$ in Fig. 2 (curve *4*) and Fig. 3 (curve *6*), it can be noted that within the framework of the proposed mathematical model, applying an axial tensile load slows the relaxation of residual stresses under creep conditions (at least, in the investigated time intervals and for given tensile stress.) Note also the satisfactory agreement between the calculated and experimental data on the residual stresses after creep for 50 h [taking into account the temperature approximation for the parameters c and m in relations (11)].

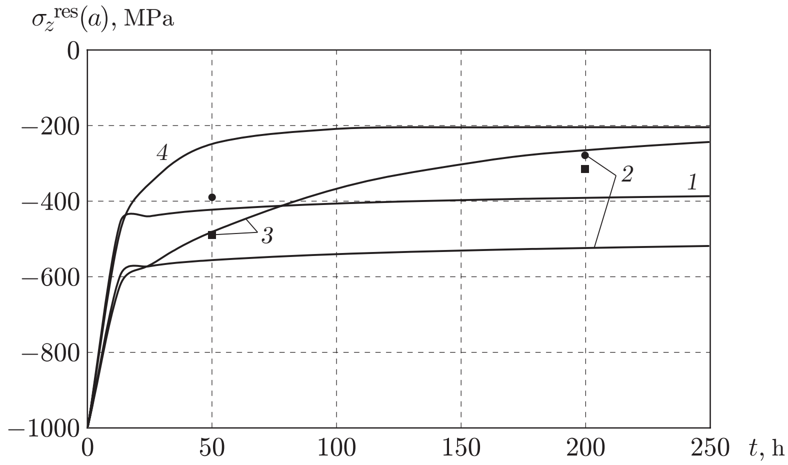

Fig. 4. Relaxation of residual stresses $\sigma_z^{\rm res}$ on the surface of a cylindrical specimen due to creep for different distributed tensile loads N: points are experimental data, and curves are calculation results for $N = 0$ (*1*), 150 (*2*), 250 (*3*), 300 MPa (*4*).

A more complete understanding of the phenomena mentioned above is provided by estimates (see Fig. 4) for the maximum (in absolute value) value of $\sigma_z^{\rm res}$ on the surface of the cylindrical specimen after creep.In the case considered, for relatively small values of the tensile load (100–200 MPa) there is a decrease in the relaxation rate stress compared to the version of thermal exposure in the absence of tensile load ($N = 0$), and at loads of higher intensity (250–300 MPa), the relaxation rate decreases only the initial portions of the time interval. For large time values, the stress relaxation rate becomes higher than that in the case of thermal exposure.

Thus, the relaxation of residual stresses in the surface hardened layer of cylindrical specimens subjected to tensile loading under creep conditions is determined by the initial stress–strain state arising after hardening, and its nature depends on the magnitude and duration of the applied tensile load.

This work was supported by the Ministry of Education and Science of the Russian Federation (Grant No. 1151) and the Russian Foundation for Basic Research (Grant No. 13-01-00699).

REFERENCES

1. I. A. Birger, *Residual Stresses* (Mashgiz, Moscow, 1963) [in Russian].
2. V. F. Pavlov, V. A. Kirpichev, and V. B. Ivanov, *Residual Stresses and Fatigue Resistance of Toughened Parts with Stress Concentrators* (Samara Scientific Center, Russian Acad. of Sci., Samara, 2008) [in Russian] .
3. I. G. Grinchenko, *Hardening of Parts from Heat-Resistant and Titanium Alloys* (Mashinostroenie, Moscow, 1971) [in Russian].
4. B. A. Kravchenko, V. G. Krutzilo, and G. N. Gutman. *Thermoplastic Hardening is a Reserve for Increasing the Strength and Reliability of Machine Parts* (Samara State Technical University, Samara, 2000) [in Russian].
5. A. M. Sulima, V. A. Shuvalov, and Yu. D. Yagodkin, *Surface Layer and Performance of Gas Turbine Engines* (Mashinostroenie, Moscow, 1988) [in Russian].
6. V. P. Radchenko and M. N. Saushkin, *Creep and Relaxation of Residual Stresses in Hardened Structures* (Mashinostroenie, Moscow, 2005) [in Russian]
7. O. V. Kolotnikova, "Effectiveness of Hardening by Surface Plastic Deformation of Parts Operating at Elevated Temperatures," Probl. Prochn., No. 2, 112–114 (1983).
8. V. P. Radchenko and M. N. Saushkin, "Mathematical Models of Recovery and Relaxation of Residual Stresses in the Surface-Hardened Layer of Cylindrical Structural Elements in Creep," Izv. Vyssh. Uchebn. Zaved., Mashinostr., No. 11, 3–17 (2004).
9. V. P. Radchenko and M. N. Saushkin, "Direct Method of Solving the Boundary-Value Problem of Relaxation of Residual Stresses in a Hardened Cylindrical Specimen under Creep Conditions," Prikl. Mekh. Tekh. Fiz. **50** (6), 90–99 (2009) [J. Appl. Mech. Tech. Phys. **50** (6), 989–997 (2009)].

10. S. I. Ivanov, "Determination of Residual Stresses in a Cylinder by the Method of Rings and Stripes," in *Residual Stresses* (Kuibyshev Aviation Institute, Kuibyshev, 1974), Issue 53, pp. 32–42 [in Russian].
11. S. I. Ivanov, "Determination of Residual Stresses in the Surface Layer of a Cylinder," in *Problems of the Strength of Aircraft Structural Elements* (Kuibyshev Aviation Institute, Kuibyshev, 1971), Issue 48, pp. 153–168.
12. V. P. Radchenko, V. F. Pavlov, and M. N. Saushkin, "Determination of the Hardening Anisotropy Parameter and Residual Stresses in a Cylindrical Steel Specimen after Rolling," Probl. Mashinostr. Prochn. Mashin, No. 4, 93–100 (2011).
13. V. P. Radchenko, M. N. Saushkin, and V. F. Pavlov, "Method of Calculating the Fields of Residual Stresses and Plastic Strains in Cylindrical Specimens with Allowance for Surface Hardening Anisotropy," Prikl. Mekh. Tekh. Fiz. **52** (2), 173–182 (2011) [J. Appl. Mech. Tech. Phys. **52** (2), 302–310 (2011)].
14. Yu. P. Samarin, *Equation of State for Materials with Complicated Rheological Properties* (Kuibyshev State University, Kuibyshev, 1979) [in Russian].
15. A. N. Badaev, "On the Determination of the Distribution Function of the Parameters of the Creep State Equation," Probl. Prochn., No. 12, 22–26 (1984).
16. V. I. Kovpak and A. N. Badaev, "Unified Approach to Predicting Creep. Issues of Heat-Resistant Materials in the Statistical Aspects," in *Standardized Methods for the Determination of Creep and Rupture Strength* (Izd. Standartov, Moscow, 1986), pp. 51–62 [in Russian].
17. V. P. Radchenko, M. N. Saushkin, and E. P. Goludin, "Stochastic Model of Nonisothermal Creep and Long-Term Strength of Materials," Prikl. Mekh. Tekh. Fiz. **53** (2), 167–174 (2012) [J. Appl. Mech. Tech. Phys. **53** (2), 292–298 (2012)].