ISSN 0021-8944, *Journal of Applied Mechanics and Technical Physics, 2016, Vol. 57, No. 6, pp. 1133–1140.* © *Pleiades Publishing, Ltd., 2016.*
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SPRINGBACK ANALYSIS IN SHEET METAL FORMING BY USING THE RAMBERG–OSGOOD STRESS–STRAIN RELATION

R. K. Lal[a], M. K. Bhagat[b], J. P. Dwivedi[b], V. P. Singh[b], and S. K. Patel[b]

UDC 539.3

Abstract: The Ramberg–Osgood stress–strain relation is used to perform a theoretical springback analysis in the problem of bending of a narrow rectangular strip made of a strain-hardening material. The maximum strip thickness is 5 mm, and its length is significantly greater than the thickness. Based on the elasticity and plasticity deformation theory and also on the Tresca and von Mises yield criteria, an expression for the springback ratio is derived. The springback ratio depends on the ratio of the yield stress to Young's modulus, Poisson's ratio, strain hardening coefficient, and sheet thickness.

Keywords: strain hardening, sheet metal, Ramberg–Osgood stress–strain relation.

DOI: 10.1134/S0021894416060225

INTRODUCTION

In sheet metal working, sheets are deformed to cylindrical and helical shapes by means of plastic bending with the use of punches and dies. If such bending is properly done, the inner surface of the sheet matches the surface contour of the die at the end of the forming process. However, when the applied load is released, the contour acquires a different shape than that of the die because of the release of elastic stresses in the metal. This elastic distortion is commonly called the springback. To take the springback effect into account, it is necessary to use methods capable of predicting the springback magnitude as a function of the die geometry and material properties.

In the case of bending, the springback is a measure of elastic recovery of the sheet curvature radius after removal of the applied bending moment.

The springback of sheets after their bending was studied by Sachs [1], Schroeder [2], Gardiner [3], and Singh and Johnson [4], who derived equations for the springback ratio as a function of the sheet width, length, and thickness. Those studies were limited to V- and U-shaped dies. Huth [5], Nadai [6], and Upadhyay [7] considered elastoplastic torsion of bars with rectangular cross sections under the action of monotonically increasing loads. Dwivedi et al. [8, 9] analytically predicted the residual angle of twisting for bars made of elastic strain-hardening materials with rectangular cross sections. However, only thin rectangular strips were considered in those studies. Dwivedi et al. also investigated the torsional springback of bars with square cross sections made of linear [10] and nonlinear [11] strain-hardening materials. Dwivedi et al. [12] also studied the torsional springback of bars with L-shaped cross sections made of nonlinear strain-hardening materials. Experimental results on studying the springback of sheet bending and tube bending were reported by Zhang and Hu [13], Kuwabara [14], Yi et al. [15],

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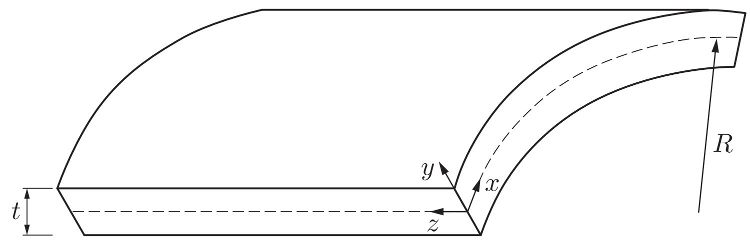

Fig. 1. Cylindrical shell element.

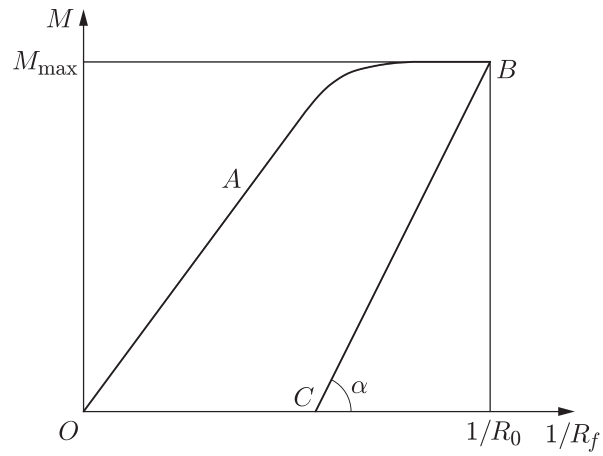

Fig. 2. Bending moment versus the sheet curvature.

Megharbel et al. [16], Da-xin et al. [17], and Da-xin and Liu [18]. The torsional springback in thin tubes made of nonlinear strain-hardening materials was studied by Choubey et al. [19–21].

In the present work, we derive an approximate relation in an attempt to develop a quantitative method for predicting the springback behavior in bent sheets as a function of the die radius, sheet thickness, and stress–strain characteristics of the sheet material.

1. FORMULATION OF THE PROBLEM OF THE SPRINGBACK AFTER SHEET METAL FORMING

The following assumptions are applied:

(1) the sheet transforms to a cylindrical surface due to pure bending;

(2) the sheet width is much greater than its thickness;

(3) the reference surface is the mid-surface of the sheet, and the cross sections remain plane during the bending process;

(4) the cross-sectional dimensions of the sheet do not change significantly in the course of bending; the radius of bending is much greater than the sheet thickness; therefore, radial stresses can be neglected;

(5) the circumferential strains are sufficiently small; therefore, the conventional strain is approximately equivalent to the true strain;

(6) the transverse strains are equal to zero at all points of the sheet;

(7) the circumferential strains are constant over the sheet cross section.

An element of the cylindrical shell and the coordinate system used are shown in Fig. 1.

Figure 2 shows the qualitative dependence of the bending moment on the sheet curvature during the process of forming of a wide sheet around a portion of a cylindrical die. The stage of elastic straining (segment OA) is followed by plastic straining of the material (segment AB). The point B corresponds to the time instant when the inner surface of the sheet completely matches the cylindrical die surface. After that, the applied moment is released, and the elastic springback of the sheet occurs.

The change in the sheet curvature due to the elastic springback is $1/R_0 - 1/R_f$ (see Fig. 2):

$$\frac{1}{R_0} - \frac{1}{R_f} = \frac{M_{\max}}{\partial M_E/\partial\,(1/R)} \tag{1}$$

$[\partial M_E/\partial\,(1/R) = \tan\,\alpha]$. The equality of the applied bending moment and the internal stress moment is expressed as

$$M_{\max} = \int\limits_{-t/2}^{t/2} \sigma_x y\,dy = 2\int\limits_{0}^{t/2} \sigma_x y\,dy. \tag{2}$$

2. STRESS–STRAIN RELATION UNDER UNIAXIAL LOADING

The springback after sheet metal forming is modeled with the use of a modified stress–strain Ramberg–Osgood relation.

In the case of a uniaxial stress–strain state, the nonlinear Ramberg–Osgood stress–strain relation has the form

$$\varepsilon = \frac{\sigma}{E}\Big[1 + \alpha\Big(\frac{\sigma}{\sigma_0}\Big)^{n-1}\Big],$$

where the first term σ/E is the elastic strain and the second term $(\sigma/E)\alpha(\sigma/\sigma_0)^{n-1}$ is the plastic strain. The plastic strain in the Ramberg–Osgood relation is never equal to zero, regardless of the fact that the stress can take infinitesimal values. The second term, however, makes a significant contribution to the total strain only at $\sigma > \sigma_0$.

In this work, we use a modified Ramberg–Osgood stress–strain relation. In the case of a uniaxial stress–strain state, we have

$$\varepsilon = \sigma/E \quad \text{at} \quad \sigma < \sigma_* \qquad \text{or} \qquad \varepsilon = (\sigma/E)\alpha(\sigma/\sigma_0)^{n-1} \quad \text{at} \quad \sigma > \sigma_*.$$

It follows from the condition of stress and strain continuity at $\sigma < \sigma_*$ that

$$\sigma_* = K\varepsilon_*^{\lambda} = K\Big(\frac{K}{E}\Big)^{\lambda/(1-\lambda)}, \qquad \varepsilon_* = \Big(\frac{K}{E}\Big)^{1/(1-\lambda)}, \tag{3}$$

where $K = \sigma_0(E/(\alpha\sigma_0)^{\lambda})$ and $\lambda = 1/n$.

3. SHEET DEFORMATION IN THE ELASTIC REGION

If there are no shear stresses, Hooke's law has the form

$$\varepsilon_x = (\sigma_x - \nu(\sigma_y + \sigma_z))/E; \tag{4}$$

$$\varepsilon_y = (\sigma_y - \nu(\sigma_x + \sigma_z))/E, \qquad \varepsilon_z = (\sigma_z - \nu(\sigma_y + \sigma_x))/E. \tag{5}$$

As it is assumed that the radius of bending is much greater than the sheet thickness, the radial stresses can be neglected, and the transverse strains can be assumed to have zero values at all points of the sheet:

$$\sigma_y = \varepsilon_z = \delta_z = 0. \tag{6}$$

In view of Eq. (6), it follows from Eq. (5) that

$$\sigma_z = \nu\sigma_x,$$

and Eq. (4) yields

$$\varepsilon_x = \frac{\sigma_x}{E}\,(1-\nu^2), \qquad \sigma_x = \frac{E}{1-\nu^2}\,\varepsilon_x.$$

In the case of pure bending, we have

$$\varepsilon_x = y/R_G.$$

Therefore, in the case of elastic deformation, we have

$$\sigma_x = \frac{E}{1-\nu^2}\,\frac{y}{R_0}. \tag{7}$$

4. DETERMINATION OF STRESSES AND STRAINS IN THE CASE OF PLASTIC DEFORMATION OF THE SHEET WITH THE TRESCA YIELD CRITERION

In accordance with the Tresca yield criterion, the material transforms to the plastic state under the condition

$$\sigma_x - \sigma_z = 2\sigma_*. \tag{8}$$

Taking into account Eqs. (3) and (8) for the stress and strain at which the material transforms to the plastic state, we obtain

$$\sigma_{x0} = \frac{K(K/E)^{\lambda/(1-\lambda)}}{1-\gamma}, \qquad \varepsilon_{x0} = \Big(\frac{K}{E}\Big)^{1/(1-\lambda)}(1+\nu).$$

As $\varepsilon_x = y/R_0$ in the case of pure bending, we obtain the following expression for $0 \leqslant y/R_0 \leqslant (K/E)^{1/(1-\lambda)}(1+\nu)$:

$$\sigma_x = \frac{E}{1-\nu^2}\,\frac{y}{R_0}.$$

Using the deformation theory of plasticity [16], we calculate the stresses and strains in the plastic region

$$\delta_x = \frac{1}{K^{1/\lambda}}(\sigma_x^2 + \sigma_y^2 + \sigma_z^2 - \sigma_x\sigma_y - \sigma_y\sigma_z - \sigma_z\sigma_x)^{(1-\lambda)/(2\lambda)}\Big(\sigma_x - \frac{\sigma_y}{2} - \frac{\sigma_z}{2}\Big); \tag{9}$$

$$\delta_y = \frac{1}{K^{1/\lambda}}(\sigma_x^2 + \sigma_y^2 + \sigma_z^2 - \sigma_x\sigma_y - \sigma_y\sigma_z - \sigma_z\sigma_x)^{(1-\lambda)/(2\lambda)}\Big(\sigma_y - \frac{\sigma_x}{2} - \frac{\sigma_z}{2}\Big);$$

$$\delta_z = \frac{1}{K^{1/\lambda}}(\sigma_x^2 + \sigma_y^2 + \sigma_z^2 - \sigma_x\sigma_y - \sigma_y\sigma_z - \sigma_z\sigma_x)^{(1-\lambda)/(2\lambda)}\Big(\sigma_z - \frac{\sigma_x}{2} - \frac{\sigma_y}{2}\Big), \tag{10}$$

where δ_x, δ_y, and δ_z are the plastic strains.

In view of Eq. (6), relation (10) yields

$$\sigma_z = \sigma_x/2.$$

As $\delta_x = y/R_0$, we use Eq. (9) and take into account Eq. (6) to find

$$\sigma_x = \frac{K}{(3/4)^{(1+\lambda)/2}}\Big(\frac{y}{R_0}\Big)^{\lambda}. \tag{11}$$

Equation (11) is valid under the condition

$$\Big(\frac{K}{E}\Big)^{2/(1-\lambda)}(1+\nu) \leqslant \frac{y}{R_0} \leqslant \frac{t}{2R_0}.$$

Using Eq. (2), we find the maximum value of the bending moment $M_{\max} = 2\int\limits_0^{t/2} \sigma_x y\,dy$. The integral from 0 to $t/2$ can be presented as a sum of two parts: the integral from 0 to R_* (elastic region) and the integral from R_* to $t/2$ (plastic region):

$$M_{\max} = 2\Big(\int\limits_0^{R_*} \sigma_{x,el}\,y\,dy + \int\limits_{R_*}^{t/2} \sigma_{x,pl}\,y\,dy\Big).$$

Here $R_* = R_0(K/E)^{1/(1-\lambda)}(1+\nu)$.

Using Eqs. (7) and (11) and calculating the integrals, we finally obtain

$$M_{\max} = 2\,\frac{ER_0^2(K/E)^{3/(1-\lambda)}(1+\nu)}{3(1-\nu^2)}$$

$$+\,2\Big(\frac{K(t/2)^{\lambda+2}}{(3/4)^{(1+\lambda)/2}R_0^{\lambda}(\lambda+2)} - \frac{KR_0^2(K/E)^{(\lambda+2)/(1-\lambda)}(1+\nu)^{\lambda+2}}{(3/4)^{(1+\lambda)/2}(\lambda+2)}\Big). \tag{12}$$

5. DETERMINATION OF STRESSES AND STRAINS IN THE CASE OF PLASTIC DEFORMATION OF THE SHEET WITH THE VON MISES YIELD CRITERION

In accordance with the Mises yield criterion, the material transforms to the plastic state under the condition

$$\frac{1}{2}\left[(\sigma_x-\sigma_y)^2+(\sigma_y-\sigma_z)^2+(\sigma_z-\sigma_x)^2\right]=\sigma_*^2. \tag{13}$$

In view of Eq. (6), Eq. (13) yields

$$\sigma_*=\sigma_x(1+\nu^2-\nu)^{1/2}. \tag{14}$$

Taking into account Eqs. (3) and (4) for the stress and strain at which the material transforms to the yield state, we obtain

$$\sigma_{x0}=\frac{K(K/E)^{\lambda/(1-\lambda)}}{(1-\nu+\nu^2)^{1/2}},\qquad \varepsilon_{x0}=\Big(\frac{K}{E}\Big)^{1/(1-\lambda)}\frac{1-\nu^2}{(1-\nu+\nu^2)^{1/2}}.$$

For the elastic region, at $0\leqslant\dfrac{y}{R_0}\leqslant\Big(\dfrac{K}{E}\Big)^{1/(1-\lambda)}\dfrac{1-\nu^2}{(1-\nu+\nu^2)^{1/2}}$, we have

$$\sigma_x=\frac{E}{1-\nu^2}\,\frac{y}{R_0}. \tag{15}$$

Taking into account Eq. (6), we use Eq. (9) to obtain the stress in the plastic region, at $\Big(\dfrac{K}{E}\Big)^{1/(1-\lambda)}\dfrac{1-\nu^2}{1-\nu+\nu^2}\leqslant\dfrac{y}{R_0}\leqslant\dfrac{t}{2}$:

$$\sigma_x=\frac{K}{(3/4)^{(1+\lambda)/2}}\Big(\frac{y}{R_0}\Big)^{\lambda}. \tag{16}$$

Using Eq. (2), we find

$$M_{\max}=2\int_0^{t/2}\sigma_x y\,dy.$$

The integral from 0 to $t/2$ can be presented as a sum of two parts: the integral from 0 to R_* (elastic region) and the integral from R_* to $t/2$ (plastic region):

$$M_{\max}=2\Big(\int_0^{R_*}\sigma_{x,el}y\,dy+\int_{R_*}^{t/2}\sigma_{x,pl}y\,dy\Big).$$

Here

$$R_*=R_0\Big(\frac{K}{E}\Big)^{1/(1-\lambda)}\frac{1-\nu^2}{(1-\nu+\nu^2)^{1/2}}.$$

Using Eqs. (15) and (16) and calculating the integrals, we finally obtain

$$M_{\max}=2\,\frac{ER_0^2(K/E)^{3/(1-\lambda)}}{3(1-\nu^2)}\,\frac{(1-\nu^2)^3}{(1-\nu+\nu^2)^{3/2}}+2\Big(\frac{K(t/2)^{\lambda+2}}{(3/4)^{(1+\lambda)/2}R_0^{\lambda}(\lambda+2)}-\frac{KR_0^2(K/E)^{(\lambda+2)/(1-\lambda)}(1-\nu^2)^{\lambda+2}}{(3/4)^{(1+\lambda)/2}(\lambda+2)(1-\nu+\nu^2)^{(\lambda+2)/2}}\Big). \tag{17}$$

6. CALCULATION OF THE SPRINGBACK RATIO

In the elastic region, the bending moment per unit width of the sheet [17] is

$$M_E=\frac{2E(t/2)^3}{3(1-\nu^2)R}; \tag{18}$$

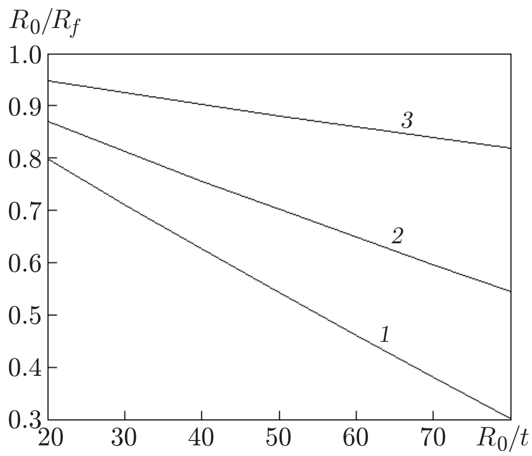

Fig. 3.

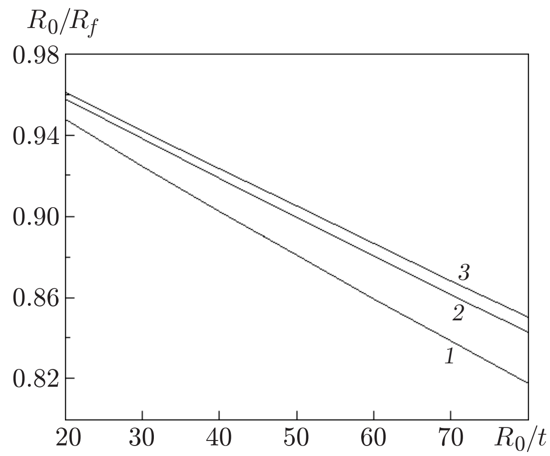

Fig. 4.

Fig. 3. Springback ratio R_0/R_f versus R_0/t for $\nu = 0.33$, $n = 10$, and $\sigma_0/E = 2.45 \cdot 10^{-3}$ (*1*), $1.52 \cdot 10^{-3}$ (*2*), and $5.5 \cdot 10^{-4}$ (*3*).

Fig. 4. Springback ratio R_0/R_f versus R_0/t for $\sigma_0/E = 5.5 \cdot 10^{-4}$, $\nu = 0.33$, and $n = 10$ (*1*), 20 (*2*), and 30 (*3*).

therefore, we have

$$\frac{\partial M_E}{\partial\,(1/R)} = \frac{2E(t/2)^3}{3(1-\nu^2)}. \tag{19}$$

Taking into account Eqs. (1), (18), (19), (17), and (12), we obtain the following expressions for the springback ratio. If the Tresca yield criterion is used, we have

$$\frac{R_0}{R_f} = 1 - \frac{3(1-\nu^2)}{(3/4)^{(n+1)/(2n)}(2n+1)/n}\Big(\frac{1}{\alpha}\Big)^{1/n}\Big(\frac{\sigma_0}{E}\Big)^{(n-1)/n}\Big(\frac{2R_0}{t}\Big)^{(n-1)/n}$$
$$+\Big[\frac{2R_0}{t}\Big(\frac{K}{E}\Big)^{n/(n-1)}(1+\nu)\Big]^3\Big(\frac{3(1+\nu)^{1/n}(1-\nu)}{(3/4)^{(n+1)/(2n)}(2n+1)/n} - 1\Big). \tag{20}$$

If the von Mises yield criterion is used, we obtain

$$\frac{R_0}{R_f} = 1 - \frac{3(1-\nu^2)}{(3/4)^{(n+1)/(2n)}(2n+1)/n}\Big(\frac{1}{\alpha}\Big)^{1/n}\Big(\frac{\sigma_0}{E}\Big)^{(n-1)/n}\Big(\frac{2R_0}{t}\Big)^{(n-1)/n}$$
$$+\Big[\frac{2R_0}{t}\Big(\frac{K}{E}\Big)^{n/(n-1)}(1-\nu^2)\Big]^3\Big(\frac{3(1-\nu^2)^{1/n}}{(3/4)^{(n+1)/(2n)}(1-\nu+\nu^2)^{(2n+1)/(2n)}(2n+1)n} - \frac{1}{(1-\nu+\nu^2)^{3/2}}\Big). \tag{21}$$

To estimate the springback ratio in engineering processes where $R_0/t \leqslant 30$, Eqs. (20) and (21) can be simplified by omitting the second terms.

7. RESULTS AND DISCUSSION

The resultant relations depend on the parameters R_0/t and σ_0/E, strain hardening coefficient n, and Poisson's ratio ν.

Figure 3 shows the springback ratio R_0/R_f as a function of R_0/t for different values of σ_0/E.

Figure 4 shows the springback ratio R_0/R_f as a function of R_0/t for $\sigma_0/E = 5.5 \cdot 10^{-4}$, $\nu = 0.33$, and different values of the strain hardening coefficient. As the value of n increases, it is seen that the material behavior approaches the behavior of an ideal elastoplastic material.

Figure 5 shows the springback ratio R_0/R_f as a function of R_0/t for different values of Poisson's ratio.

Figures 6–8 show the springback ratio R_0/R_f as a function of the sheet thickness t for different values of σ_0/E, n, and ν.

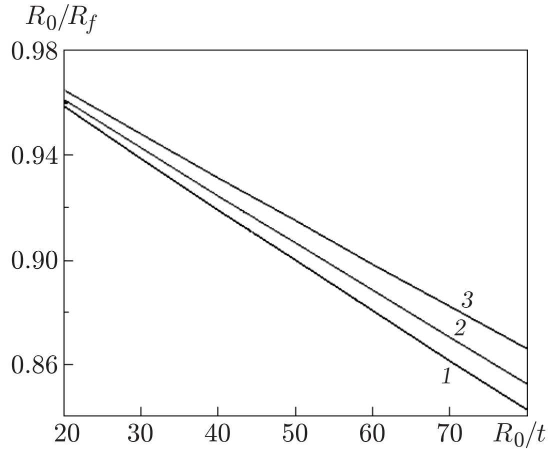

Fig. 5. Springback ratio R_0/R_f versus R_0/t for $\sigma_0/E = 5.5 \cdot 10^{-4}$, $n = 30$, and $\nu = 0.25$ (*1*), 0.35 (*2*), and 0.45 (*3*).

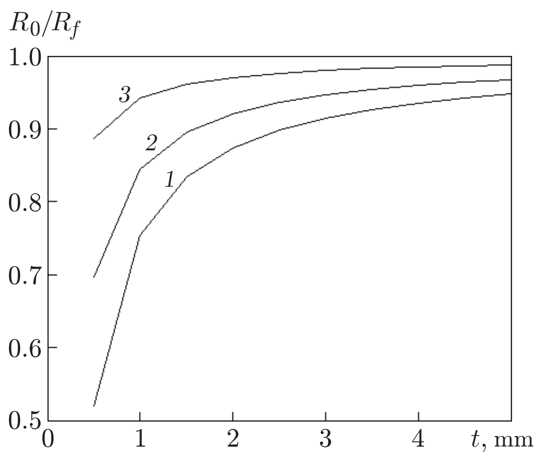

Fig. 6.

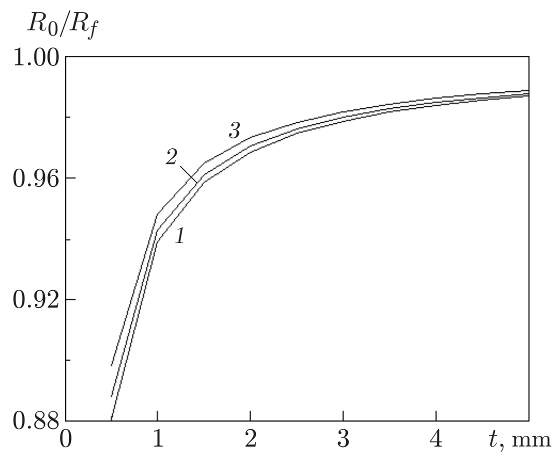

Fig. 7.

Fig. 6. Springback ratio R_0/R_f versus the sheet thickness t for $\nu = 0.33$, $n = 30$, and $\sigma_0/E = 2.45 \cdot 10^{-3}$ (*1*), $1.52 \cdot 10^{-3}$ (*2*), and $5.5 \cdot 10^{-4}$ (*3*).

Fig. 7. Springback ratio R_0/R_f versus the sheet thickness t for $\sigma_0/E = 5.5 \cdot 10^{-4}$, $n = 20$, and $\nu = 0.25$ (*1*), 0.35 (*2*), and 0.45 (*3*).

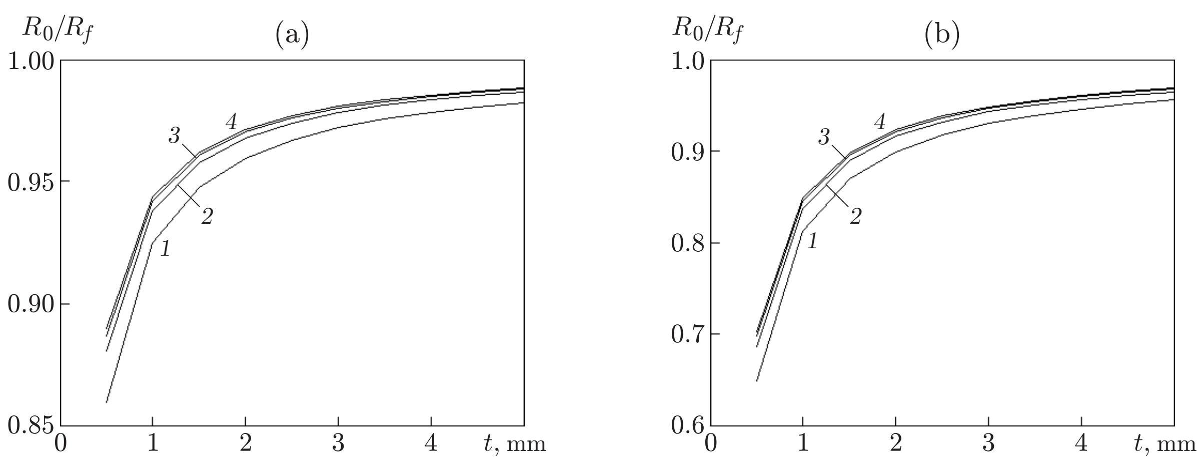

Fig. 8. Springback ratio R_0/R_f versus the sheet thickness t for $\nu = 0.33$, $\sigma_0/E = 5.5 \cdot 10^{-4}$ (a) and $1.52 \cdot 10^{-3}$ (b), and $n = 10$ (*1*), 20 (*2*), 30 (*3*), and 40 (*4*).

CONCLUSIONS

The springback ratio in the case of forming cylindrical elements under pure bending conditions was determined with the use of nonlinear constitutive relations. It was found that the springback ratio increases with an increase in the strain hardening coefficient, Poisson's ratio, and sheet thickness, and also with a decrease in the ratio of the yield stress to Young's modulus. At $R_0/t < 20$, the springback ratios determined with the use of the Tresca and von Mises yield criteria almost coincide with each other.

The authors are thankful to Prof. P. C. Upadhyay, Ex-Head of the Department of Mechanical Engineering of the Indian Institute of Technology (Banaras Hindu University, Varanasi) for his kind help in preparing this paper.

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