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Effect of continuum damage mechanics on springback prediction in metal forming processes †

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Abstract

The influence of considering the variations in material properties was investigated through continuum damage mechanics according to the Lemaitre isotropic unified damage law to predict the bending force and springback in V-bending sheet metal forming processes, with emphasis on Finite element (FE) simulation considerations. The material constants of the damage model were calibrated through a uniaxial tensile test with an appropriate and convenient repeating strategy. Holloman's isotropic and Ziegler's linear kinematic hardening laws were employed to describe the behavior of a hardening material. To specify the ideal FE conditions for simulating springback, the effect of the various numerical considerations during FE simulation was investigated and compared with the experimental outcome. Results indicate that considering continuum damage mechanics decreased the predicted bending force and improved the accuracy of springback prediction.

Keywords: Springback; Damage mechanics; Stiffness degradation; FE simulation; Metal forming

1. Introduction

Springback is defined as the change in a deformed part from a desirable configuration after unloading. It is one the common problems in sheet metal-forming processes. Disregarding this undesirable phenomenon causes problems, particularly in the assembly step, and increases tolerances and variability in the subsequent forming operations [1]. Therefore, prediction and compensation of springback are vital in nearly all metal-forming processes. Springback is rapidly gaining considerable research interest. A review by Wagoner et al. [2] in 2005 revealed that the term "springback" does not appear in standard dictionaries, although this term had been used as early as the 1940s. According to the Thomson scientific database, only 334 technical papers discussing springback have been published since 1980. A recent investigation conducted in 2012 noted a dramatic change in previous reports [1]. Springback prediction was initially conducted by using only analytical methods. This approach is still employed by researchers [3, 4]. Analytical methods utilize simplified assumptions (e.g., perfect plastic behavior, constant Young's modulus). Therefore, the accuracy of these methods is low. The introduction of the Finite element method (FEM) caused a significant change in numerical work. Currently, FEM is regarded as an effective method by researchers. Yui et al. [5] investigated the effects of planar anisotropy coefficients and the yield function on springback characteristics through a numerical simulation. Da Sisva Botelho et al. [6] presented a simple and ordinary comparison of FE simulation, analytical, and experimental results on a draw-bending process. Farsi and Arezoo [7] experimentally investigated the value of springback and bending forces for a low-carbon steel material in a V-bending process. The capability of various hardening laws and their combination to predict the springback, wall-thinning, and cross-section deformation of titanium tubes in a rotary-draw tube-bending process was investigated by Shahabi and Nayebi [8]. Initially, researchers did not consider the importance of Bauschinger's effect but recognized its importance in subsequent investigations. Gau and Kinzel [9] reported a difference between the results of isotropic and kinematic hardening models in predicting the springback in sheet metal-forming processes. Their investigations indicated that the strain path changes the effect of the difference on springback prediction using the two models. Srinivasan et al. [10] presented predictive models for the bending force and final bend angle in air bending of an electrogalvanized steel sheet by employing the response surface methodology. Leu and Zhuang [11] developed a simplified approach by considering the thickness ratio, normal anisotropy, and strain-hardening exponent to estimate the springback of V-bending in reference to elementary bending theory. The researchers also experimentally and numerically investi-

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gated the influence of geometrical and numerical process parameters. Lee and Park [12] optimized the structural and process parameters in a sheet metal-forming process. The relation between equivalent plastic strain and springback was investigated by Kadkhodayan [13] in consideration of a benchmark of NUMISHEET'93 2D draw bending and by using the commercial FEM code. The theory of classical plasticity generally assumes that materials present a linear unloading behavior with a slope equal to the elasticity modulus, whereas experimental results show that the elasticity behavior in unloading is nonlinear and exhibits a slight curvature, which depends on plastic strain [14, 15]. Thus, researchers considered the changes in elasticity modulus in their investigations, and this consideration improved springback prediction [16-18]. In the current study, the damage mechanics concept was utilized to overcome the problem and improve the accuracy of modeling results. Continuum damage mechanics (CDM) is a useful approach to describe the material microscopic behavior accurately. Unlike most studies, the current study does not assume that Young's modulus is constant during plastic deformation. Instead, CDM based on the Lemaitre isotropic unified damage law was employed to describe the variations in Young's modulus during the forming process. CDM effectively describes plasticity behavior, such as kinematic hardening. Calibrating the material constants of the Lemaitre damage model requires cyclic (loading-unloading) experimental tests. Therefore, this process may be partly difficult and undesirable in many cases. The damage model was calibrated through simple uniaxial tests instead of complicated cyclic tests. To investigate the changes in material behavior and properties during plastic deformation, the concept of CDM based on the Lemaitre isotropic unified damage law was employed to provide effective springback prediction in metal-forming processes. Moreover, the material constants of the damage model were calibrated with a simple numerical approach based on FE simulation and the data of the uniaxial test.

2. CDM

Damage pertains to the gradual decrease in or sudden decadence of the mechanical strength of materials through loading, thermal, and chemical effects. At the microscale, damage is the accumulation of micro stresses in a neighborhood of defects or interfaces and the breaking of bounds, both of which damage the material [19]. From a physical point of view, damage is related to plastic or irreversible strains and generally to strain dissipation either at the mesoscale, the scale of the representative volume element, or microscale. The concept of effective stress was introduced by Rabotnov in 1968. If S_D is the area of microcracks and microvoids in a cross section of a material and microforces do not act on the microsurface of microcracks and microvoids, effective stress ($\bar{\sigma}$), which is related to effective surface, can be conveniently derived as

$$\bar{\sigma} = \frac{F}{S - S_D} . \tag{1}$$

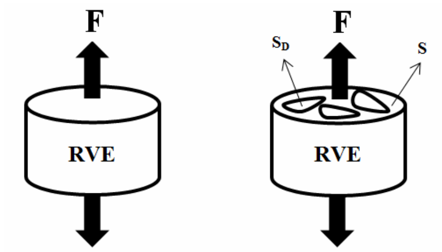

Fig. 1. 1D element (left: No damage, right: Damaged).

According to Fig. 1, by introducing the damage variable, (D = S/S$_D$) effective stress can be written in the following form [19].

$$\bar{\sigma} = \frac{\sigma}{1 - D} . \tag{2}$$

When damage occurs, the yield function (*f*) is changed to [19]

$$\sigma = (\sigma_y + R + X)(1 - D) . \tag{3}$$

The elasticity modulus for the damaged material is defined [19] as

$$\tilde{E} = E(1 - D) . \tag{4}$$

Eqs. (3) and (4) and the experiments show that the damage equally decreases yield stress, isotropic strain hardening stress, backstress, and elasticity modulus. The thermodynamics approach indicates that damage rate $\dot{D}$ is associated with variable Y, which is the energy density release rate. Many observations and experiments have shown that damage is governed by plastic strain, which is introduced through the plastic multiplier $\dot{\lambda}$ as [20]

$$\dot{D} = \frac{\partial F_D}{\partial Y} \qquad \text{if} \qquad P > P_D . \tag{5}$$

Many experimental results have also shown that F_D must be a nonlinear function of *Y*. Therefore, a simple and good choice can be presented as follows [20]:

$$F_D = \frac{S}{(s+1)(1-D)} (\frac{Y}{S})^{s+1} . \tag{6}$$

Lemaitre presented a damage criterion by using the thermodynamics of irreversible processes [20].

$$\dot{D} = \left(\frac{\sigma_{eq}^2 R_v}{2ES(1-D)^2} \right)^s \dot{p} \tag{7}$$

$$R_v = \frac{2}{3}(1 + \upsilon) + 3(1 - 2\upsilon)(\frac{\sigma_H}{\sigma_{eq}})^2 . \tag{8}$$

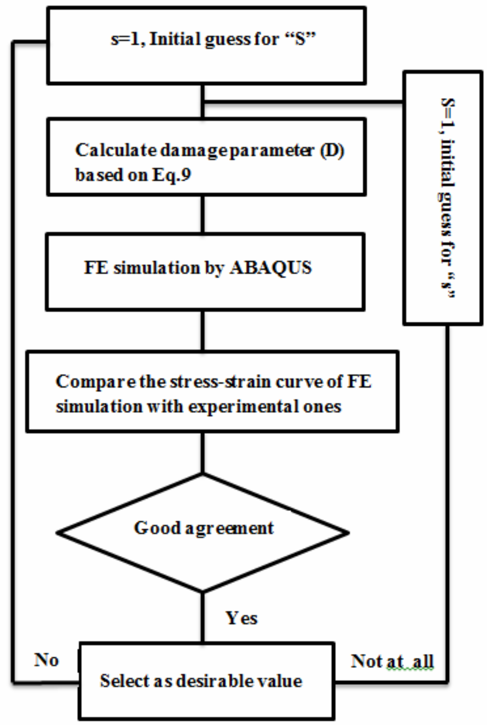

Fig. 2. Process of calibrating the damage model constants.

Material constants "s" and "S" must be specified. The material constants of the Lemaitre isotropic unified damage law (s and S) are conventionally specified through fatigue tests. The uniaxial tensile test was used to extract the desirable data and calibrate the material constants instead of utilizing other types of mechanical tests (e.g., cyclic loading-unloading test). The material constants of the Lemaitre damage model were obtained in the current study according to an iterative process as follows:

Considering the uniaxial case leads to $R_v = 1$ and $p = \varepsilon_p$. Therefore, Eq. (7) is simplified as

$$\dot{D} = \left(\frac{\sigma_{eq}^2}{2ES(1-D)^2} \right)^s \dot{\varepsilon}_p . \tag{9}$$

By integrating Eq. (9), damage parameter D can be obtained in terms of other effective parameters. The experimental results indicate that s = 1 for most materials [19]. The FE simulation in the uniaxial tensile test based on ASTM (E8m) is conducted considering s = 1 and various values of "S". Notably, no specific and exclusive approach is available to identify the initial guess of "S" or "s". However, as an approximate general rule, for a material with yield stress less than 300 MPa, s < 0.5 is recommended as an initial guess and s > 0.5 is considered for yield stress more than 300 MPa. This approach is not a general and accurate law, and the choice of the initial guess is empirical. To investigate the certainty of the selected material constants ("s" and "S"), the stress-strain curve of the simulation outputs is compared with that of the experimental results. If the difference between the stress-strain curves obtained experimentally and numerically is less than a certain value (e.g., 10 %), the values of "s" and "S" can be considered the best ones; otherwise, this process will be repeated.

If desirable agreement is not achieved for s = 1, setting S = 1 is recommended, and the process above is repeated to obtain a reasonable "s". Fig. 2 depicts this process. According to this process, the values of s = 1 and S = 0.4 MPa result in a maximum error of 5 % between the numerical and experimental results (Fig. 3). Good agreement between the simulation and experimental results can provide reasonable assurance on the selected material constants.

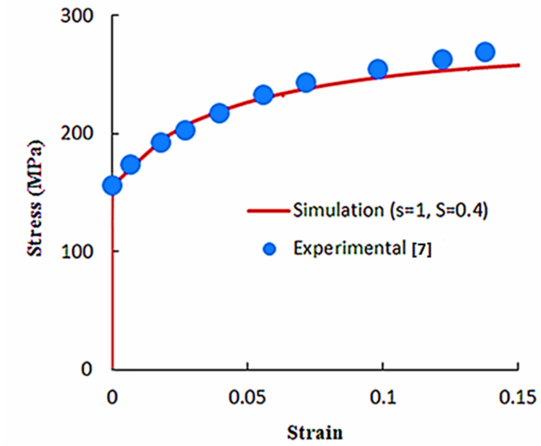

Fig. 3. Stress-strain curve for s = 1, S = 0.4 MPa.

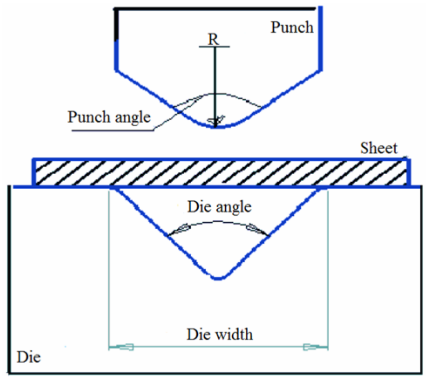

Fig. 4. Sketch of the V-bending test [7].

3. V-bending sheet metal forming process

The investigated V-bending problem in the study is shown in Fig. 4. All the geometrical conditions and mechanical properties are presented according to Farsi and Arezoo [7]. The sheet is a low-carbon steel that is frequently used in various industries. Its dimensions are L = 80 mm, W = 50 mm and t = 0.95 mm with mechanical properties of E = 193 GPa, σ_y = 155 MPa and υ = 0.3. The geometrical characteristics of the v-bending test are as follows: die angle = 90 (deg.), punch angle = 84 (deg.), and punch radius = 1 mm.

Table 1. Compression of various considerations to model the unloading step.

Automatic stabilization method of unloading step modeling	Springback
Specify dissipated energy fraction	0.03
Specify damping factor	2.51
Use damping factor from previous general step	2.56

4. FE simulation

To simulate the V-bending test, the forming and springback steps were simulated with an implicit solver. The die and punch were regarded as analytical rigid surfaces, and the sheet was considered a deformable part. According to a previous study [7], $\mu = 0.1$ is considered the friction coefficient among all involved surfaces. The die was fixed during the simulation of the sheet metal-forming process, but the punch was moved downward to make a 90° V-shape part. After the loading step, the unloading step was simulated to achieve springback modeling. Similar to the loading step, FE modeling of the unloading step was conducted with an implicit solver. Thus, a general static step was generated in consideration of nonlinearity effects (i.e., "on" case of Nlgeom). "Specify damping factor" was regarded as automatic stabilization. However, obtaining a reasonable estimate for the damping factor would be difficult unless a value is known from the output of previous runs. The damping factor depends on the amount of damping, mesh size, and material behavior. The adaptive automatic stabilization scheme, in which the damping factor can vary spatially and with time, is an effective alternative approach. In this case, the damping factor is controlled by the convergence history and the ratio of the energy dissipated by viscous damping to the total strain energy. The damping factor is determined in such a manner that the dissipated energy for a given increment with characteristics similar to that in the first increment is a small fraction of the extrapolated strain energy. The fraction is called the dissipated energy fraction and has a default value of 2 × 10-4. Several choices are available for modeling the unloading (springback) step. To select the best approach, FE simulation of the springback step was conducted with each method, and the results were compared and are shown in Table 1. Table 1 indicates that considering the "specify damping factor" case produces the best results. Feifei et al. [21] performed a partly comprehensive FE investigation to specify the best suggestions for modeling the unloading step. Their investigation indicated that considering the "specify damping factor" approach results in improved springback prediction. Their investigation also indicated that the initial increment size does not significantly influence the simulation results.

The direct method and the full Newton solution technique were utilized to solve the FE equations. The other conditions governing the problem, such as loading and boundary conditions, were similar to those in the loading step, except that the die moved upward at the end of V-bending. The sheet used was meshed with 9954-node bilinear plane strain quadrilateral elements, with five elements through the sheet thickness. Reduced integration and hourglass control were also considered. To economize the solving time, only the middle part of sheet was meshed with small elements. The implicit approach was employed to solve the FE equations, and convergence of the results did not occur. Fig. 5 illustrates the mesh generation of the sheet in the present study.

Table 2. Effect of CDM on springback prediction (deg.).

Experimental = 2.00 [7]		
Hardening model	With damage	Without damage
IH	2.13 (6.5 %)	2.56 (28 %)
LKH	1.74 (13 %)	1.71 (14.5 %)

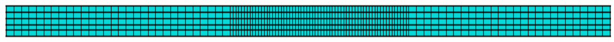

Fig. 5. Mesh generation of the sheet.

The isotropic hardening model of $\sigma = 370.52(\varepsilon_p^{0.16})$ and the Ziegler linear kinematic hardening model were utilized to describe the hardening behavior of the material [22].

$$\dot{x} = C\frac{1}{\sigma_0}(\sigma - x)\dot{\varepsilon}_p \ . \tag{10}$$

The test data obtained from a half cycle of unidirectional tension or compression must be linearized because this simple model can predict only linear hardening. Linear kinematic hardening requires two data pairs to define the linear behavior: the stress at zero plastic strain (σ0; yield stress) and the stress at an arbitrary finite plastic strain value (σ). To employ Ziegler's linear kinematic hardening law, σ_0 = 155 MPa at ε_p = 0 and σ = 296 MPa at ε_p = 0.26 were specified.

5. Results and discussion

The FE simulation of the V-bending test based on the aforesaid notes was conducted, and the following results were obtained. Table 2 shows the effect of CDM on the springback prediction for various hardening models. The values in parentheses indicate the relative error between numerical and experimental results (e.g., ((2.56-2)/2)*100 = 28 %). Utilizing the hardening models separately cannot describe material hardening models correctly. In fact, material behavior in actual cases necessitates that the yield surface be translated and expanded at the same time. Thus, considering the isotropic and kinematic hardening laws separately is insufficient. These laws should be combined until improved modeling is achieved. Considering the changes in material properties by using CDM improved the accuracy of springback prediction of the sheet to between 28 % and 6.5 % in the IH case with respect to the experimental results. The capability of the hardening models and CDM in bending force prediction was investigated as well. The results are presented in Table 3. Considering CDM during the simulation decreased the prediction of bending force in the

Table 3. Effect of CDM on bending force prediction (N.).

Experimental = 950 [7]			
Hardening model	With damage	Without damage	Decrease (%)
IH	687	692	1
LKH	667	705	5

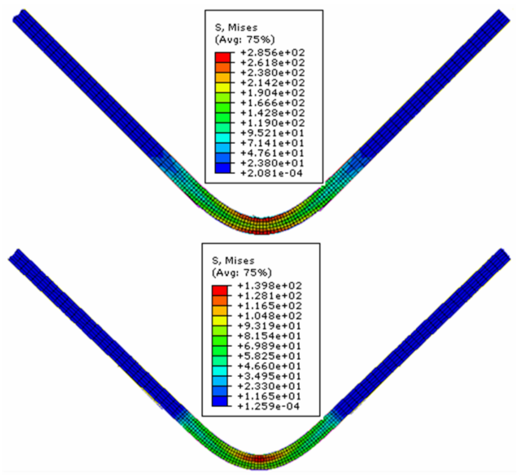

Fig. 6. Stress distribution within the sheet (up: Bending, down: Springback).

V-bending sheet metal forming process.

This decrease can be justified. The bending force of a V-bending process can be calculated analytically with Eq. (11) [23].

$$F = \frac{\sigma_y W t^2}{D_f}\left(\frac{2}{1+\mu\sin\theta - \cos 2\theta}\right). \tag{11}$$

D_f equals 1.3 for rectangular sections. Eq. (11) shows that the bending force is directly related to the yield stress. Eq. (3) indicates that CDM decreases the yield stress. Furthermore, Mkaddem et al. [24] conducted FE prediction of material damage distribution within a workpiece during wiping die bending processes. They found that damage mechanics decreases the predicted loading force. Considering the damage mechanics in predicting the bending force resulted in a mean difference of approximately 5 % compared with the case without damage. In conclusion, although considering the damage mechanics improves the accuracy of springback prediction significantly, its effect on predicting the bending force is not evident. Fig. 6 depicts the stress distribution within the V-bent sheet before and after springback. A portion of elasticity strains was released after the occurrence of springback. Thus, the stress level in the sheet metal decreased.

6. Conclusions

Considering the Young's modulus variation caused by plastic deformation is an effective strategy to simulate metal forming processes. CDM can describe the material's inelastic behavior. Combining CDM and plasticity models improved the precision of springback prediction through FE simulations. Utilizing CDM to describe the variation in the elasticity modulus and plastic follow behavior enhanced the accuracy of springback prediction by approximately 20 %. An iterative approach based on FE simulation was used as a convenient method to calibrate the constants of the damage model. Considering the induced damage by the CDM model in the metal-forming process decreased the predicted bending force.

Nomenclature

C	: Kinematic hardening modulus
D	: Damage parameter
D_f	: Die opening factor
E	: Young's modulus
$\tilde{E}$	: Elasticity modulus of damaged material
F	: Force
F_D	: Dissipative damage potential function
L	: Sheet length
p	: Accumulated plastic strain
P_D	: Damage threshold accumulated plastic strain
R	: Stress due to isotropic hardening
R_v	: Triaxiality function
s, S	: Material constant
t	: Sheet thickness
w	: Sheet width
X	: Back-stress
σ_0	: Initial size of yield surface
σ_{eq}	: Von-Mises equivalent stress
σ_H	: Hydrostatic stress
σ_y	: Yield stress
ε_p	: Plastic strain
υ	: Poisson ratio
μ	: Friction coefficient
θ	: Angle of bent sheet

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