Identification of material parameters for springback prediction using cyclic tension-compression test

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Identification of material parameters for springback prediction using cyclic tension–compression test

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Abstract

In sheet metal forming process, springback is a critical problem for die makers, particularly in case of advanced high strength steels. Therefore, FE simulation was often used to calculate materials deformation behavior and the springback occurrence of formed sheet metals. Recently, the Yoshida–Uemori model, a kinematic hardening model has showed great capability for describing elastic recovery of material. Nevertheless, determination of model parameters is sophisticated for industrial applications. In this work, an AHS steel grade JIS JSC780Y was investigated, in which tension–compression tests were carried out and procedure of parameter identification was analyzed. Different fitting methods were examined and verified by evaluation of cyclic stress–strain responses
obtained from simulations of 1–element model and experiments. The most appropriate parameter set was determined. Finally, hat shape forming test was performed and springback obtained by calculation and experiment was compared. It was found that the introduced procedure could be acceptably applied.

Keywords: High strength steel; Springback; Yoshida-Uemori model; Material parameters

1. Introduction

At present, production of automotive components have become very high competitive. Thus, part manufacturers require shorter time for developing dies. Besides, more accurate method is needed in order to improve quality of produced parts. For current automotive parts, Advanced High Strength (AHS) steel grades have been continuously introduced and employed so that both lightweight and higher strength characteristics are fulfilled. However, one great challenge of the AHS steels in forming process is the springback effect. It significantly affects the final shape of parts and can raise critical problem by part assembly. To precisely describe this behavior of such steels is sophisticated. Die makers as well as researchers have paid much attention to prediction and compensation of the springback (Gan et al., 2004; Carden et al., 2002). In the industries, Finite Element (FE) simulation has been widely employed in support of springback description. Accuracies of the FE simulation results depend not only on forming process parameters like tool and binder geometries, contact conditions, but also on the choice of material constitutive model. The material model plays an important role for calculating deformation behavior of formed sheet metal, which greatly influences arising elastic recovery, residual stresses and corresponding springback. Different types of material constitutive laws such as isotropic hardening law, kinematic hardening law, mixed isotropic–kinematic hardening law (Hodge et al., 1957), the Armstrong–Frederick hardening law (Armstrong et al., 1966), the Geng–Wagoner hardening law (Geng et al., 2000) have been developed and extended for describing individual material
behavior. The Yoshida–Uemori model (Yoshida et al., 2002; Yoshida et al., 2003; Yoshida et al., 2002) have been introduced for representing deformation behavior of large–strain cyclic plasticity and stress–strain responses at small scale re–yielding after large pre–strain. In the model, backstress equation was defined in order to capture the transient Bauschinger effect. Eggertsen et al. (2009) investigated five different hardening models and compared resulted cyclic curves between simulations and experiments for various steel grades. It was shown that the Yoshida–Uemori model could well characterize the transient Bauschinger effect, permanent softening and work hardening stagnation of the materials.

For using the Yoshida–Uemori model, a cyclic materials testing is required. Under compression, sheet specimen generally tends to buckling. Due to these difficulties, many testing methods have been developed to obtain the characteristic cyclic stress–strain curve; Yoshida et al. (2002) successfully bonded a few thin sheets of metal to provide a support for sheet specimen during in plane cyclic tension–compression test at large strain. Another kind of frequently used experiment is the bending test, as reported in Zhao et al. (2007), Mattiasson et al. (2006) and Yoshida et al. (1998). However, stress–strain curve could not be directly gathered from experimental data of such bending tests. Furthermore, optimization methods have been often applied for obtaining precise material parameters. They are usually based on an inverse approach, in which appropriate algorithm enables a minimization between experimental observed variables and simulated ones; Yoshida et al. (2003) used an iterative multipoint approximation concept for minimizing differences between test and corresponding numerical results. This approach was verified by comparing simulated stress–strain curves using the constitutive model incorporating parameters identified from experimental cyclic bending curves. Collin et al. (2009) employed an inverse approach code in SidoLo software, which offered the minimization in order to evaluate the Chaboche model parameters. In a recent study, Eggertsen et al. (2010) performed FE simulations of a three point cyclic bending test in the explicit code of LS–DYNA (LS–DYNA, 2007). Parameter identification of applied
hardening model was done by means of the optimization code LS-OPT. Nevertheless, such
optimization methods are still not suitable for industrial purposes.

The aim of this work was to introduce parameter determination procedure of the Yoshida–
Uemori model by a simple fitting method. Cyclic tension–compression experiments were
performed, in which a designed sample fixture installation for preventing buckling by spring
force was used and a video extensometer was employed to measure the sample elongation.

Subsequently, cyclic stress–strain curves were determined and each parameters of the
Yoshida–Uemori model were identified. Different obtained sets of parameters were evaluated
by comparing the stress–strain responses from experiments and FE simulations of 1–element
model. Finally, forming tests of a hat shape sample were carried out for verifying the
parameter sets.

2. Yoshida–Uemori hardening model and generalized parameter identification

The Yoshida-Uemori (Y-U) (Yoshida et al., 2002) constitutive model takes into account
both translation and expansion of the bounding surface \( F \), while the active yield surface
\( f \) only evolves in a kinematic manner. A schematic of yield surfaces according to the Y-U
model was presented in Chongthairungruang et al. (2013). \( O \) is the original center of the
yield surface, \( \beta \) is the center of the bounding surface. \( \alpha \) is the relative kinematic motion of
the yield surface with respect to the bounding surface and can be expressed by

\[
\alpha_r = \alpha - \beta
\]  

\[
\alpha_r^* = C \left[ \frac{\alpha}{Y} (\sigma - \alpha) - \alpha_r \sqrt{\frac{\alpha}{a}} \right] \dot{\varepsilon}_p
\]  

\[
\dot{\varepsilon}_p = \sqrt{\frac{2}{3}} D^p : D^p , \quad \bar{\alpha}_r = \phi(\alpha_r) , \quad \alpha = B + R - Y
\]
$Y$ is the size of the yield surface and is constant throughout the deformation, $B + R$ represents the size of the bounding surface and $B$ is the initial size of bounding surface with $R$ being associated with isotropic hardening. Uniaxial cyclic stress–strain curves described by the Y–U model were shown in Yoshida et al. (2002) for the yield and bounding surface. $\dot{\varepsilon}_p$ is the effective plastic strain rate, which is defined as the second invariant of $D^p$, and $C$ is a material parameter that controls the rate of the kinematic hardening. Here, $(\cdot)^\circ$ stands for the objective rate. The change of size and location for the bounding surface is defined as:

$$
\dot{R} = k(R_{sat} - R)\dot{\varepsilon}_p^\circ.
$$

$$
\dot{\beta}^\circ = k\left(2b\dot{D}^p - \beta\dot{\varepsilon}_p^\circ\right).
$$

$$
\sigma_{bound} = B + R + \beta^\circ.
$$

$\beta^\circ$ and $\dot{\beta}^\circ$ are the deviatoric component of $\beta$ and its objective rate, respectively and $b$ is a material parameter. The work hardening stagnation during reverse deformation, where $Y$, $C$, $B$, $k$, $b$, and $R_{sat}$ are material constants were described in Yoshida et al. (2002). In addition, all parameters, except the parameter $h$, control the translation and expansion of the surface $g_\sigma$ in Chongthairunguang et al. (2013). It could be used to fit the hardening stagnation behavior with experimental results. The Y–U model is referred to the kinematic hardening transversely anisotropic material model or the MAT_125 model in LS-DYNA.

Moreover, degradation of the elastic stiffness taking place during plastic loading could be considered by the Y–U model. It was found that varying elastic moduli should be defined for a simulation of springback problem. The Young’s modulus $E$ was taken into account using following equation (Yoshida et al., 2002).

$$
E = E_0 - (E_0 - E_{sat})\left[1 - \exp\left(-\frac{\varepsilon_p}{\xi}\right)\right]
$$

(7)
Where $E_0$ and $E_{\text{sat}}$ represent the Young’s modulus value at initial state and infinitely large pre-strain state of material, respectively. $\xi$ is a material constant.

The Yoshida–Uemori model involves overall seven material parameters, $Y$, $C$, $B$, $R_{\text{sat}}$, $b$, $k$, $h$. All of the parameters could be obtained from uniaxial stress–strain curve determined under a defined cyclic deformation, as depicted in Yoshida et al. (2002). A general approach for identifying each parameter was summarized as followed (Yoshida et al., 2002). $Y$ is the initial yield stress. It was determined as the elastic limit of material. The forward stress–strain curve (line (a)–(c)) in Yoshida et al. (2002) was fitted with the Eq. 8. Then, the parameters $B$, $k$ and term ($R_{\text{sat}} + b$) could be derived by this manner. And a forward bounding surface curve similar to the line (b)–(c) in Yoshida et al. (2002) was plotted.

$$\sigma_{\text{bound}}^{(\text{fow})} = B + R + \beta = B + (R_{\text{sat}} + b)(1 - e^{-ke^p})$$

(8)

The parameter $b$ represented the beginning of reverse bounding surface curve ($\sigma_{\text{B}0}^{(p)}$). It could be considered as the initial value at $\dot{e}^p = 0$ of the reverse bounding surface curve. The reverse bounding surface curve was afore determined by extrapolating data of the reverse stress–strain curve from its saturation point, as line (e)-(j) illustrated in Yoshida et al. (2002). The difference between values at point (j) and (k) in Yoshida et al. (2002) was described by $\sigma_{\text{B}0}^{(p)}$.

Since the parameter $k$ has been already derived, the parameter $b$ could be directly identified using the experimentally obtained $\sigma_{\text{B}0}^{(p)}$ value and the relationship in Eq. 9.

$$\sigma_{\text{B}0}^{(p)} = 2\beta_0 = 2b(1 - e^{-ke^p})$$

(9)

The parameter $C$ is a material parameter, which controls the rate of the kinematic hardening. It was determined according to the range of transient Bauschinger deformation of the reverse
stress–strain curve, as line (d)-(c) shown in Yoshida et al. (2002). The parameter $C$ could be expressed as a function given in Eq. 10.

$$C \approx \frac{2}{\bar{\varepsilon}^p} \left( (1 + \ln 2) - \sqrt{\frac{\alpha_c}{a}} + \ln \left( 1 + \text{sgn}(\alpha) \sqrt{\frac{\alpha_c}{a}} \right) \right)$$  \hspace{1cm} (10)$$

The transient stress was defined as $\sigma^{(i)}_B = a + \alpha$. And $\frac{\alpha}{a}$ was in turn described as a function of the transient stress offset $\sigma^{(i)}_B$ as:

$$\frac{\alpha}{a} \approx \frac{\sigma^{(i)}_B}{a_0} - 1 = 2 \left( \frac{\sigma^{(i)}_B}{\sigma^{(i)}_{B0}} \right) - 1, \quad a_0 = \frac{1}{2} \sigma^{(i)}_{B0} = \sigma_0 - \frac{1}{2} \sigma^{(r)}_{B0}$$  \hspace{1cm} (11)$$

The parameter $h$ is a material parameter, which demonstrates expansion rate of non–isotropic hardening (non–IH surface), as shown in Chongthairungruang et al. (2013). It described the rate of expansion of surface $g_\sigma$. Larger value of the parameter $h$ gave a rapid expansion of Non-IH surface, in which a prediction with lower saturated and smaller cyclic hardening was provided. Regarding Yoshida et al. (2002), this parameter was used for adjusting the calculated cyclic stress–strain curves to best fit with experiment data. The value of $h$ ranged between 0 and 1.

3. Experiments

In this work, a Dual Phase (DP) steel sheet grade JIS JSC780Y with a thickness of 1.4 mm was investigated. Various material testing were performed in order to determine mechanical behavior of the steel for parameters identification of the Yoshida–Uemori model. First, uniaxial tensile tests were carried out for obtaining anisotropic parameters of the DP steel. Determined mechanical properties of the investigated DP steel were given in Table 1.
Table 1

Mechanical properties of the investigated steel obtained by tensile test and parameters for relationship between elastic modulus and pre–deformation

A series of uniaxial test was conducted for sheet samples taken in parallel to the rolling direction in order to investigate variation of the elastic modulus at different pre–strain values. Uniaxial pre–deformation of about 0.02 was applied before and followed with unloading. Subsequently, an additional strain of 0.02 was introduced to the same sample and again succeeded with unloading. This procedure was repeated with an incremental strain of 0.02 until a total pre-strain of 0.1 was reached. The determined true stress–strain curves were plotted together, as shown in Fig. 1a. From the determined true stress–strain curves, elastic moduli for each pre–strain were identified. Then, relationship between the elastic modulus and effective plastic strain were established with regard to Eq. 7. The evolutions of the elastic modulus during plastic deformation experimentally obtained and calculated by Eq. 7 are illustrated in Fig. 1b. The involved parameters of the elastic modulus change were the initial elastic modulus $E_0$, saturated elastic modulus $E_{sat}$ and material constant $\xi$, which are presented in Table 1.

Fig. 1 (a) True stress-strain curves from loading-unloading tensile test for investigating elastic modulus change and (b) dependency between elastic modulus and effective plastic strain of the investigated steel

Furthermore, cyclic tension–compression tests were performed in order to obtain cyclic stress–strain curve for identifying material hardening parameters of the Yoshida–Uemori model. During the cyclic test, a buckling of tested sheet specimens could occur. Thus, a specific tool installation was initially designed for the cyclic test in order to prevent the
buckling problem. Kuwabara (2007) proposed a buckling prevention tool for thin sheet copper and aluminum alloy by using hydraulic system. A hard Teflon film was used for reducing friction between sheet specimen and clamping plates. In this work, spring forces were used instead for supporting the sheet specimens during tension–compression movement. In order to minimize the buckling effect and obtain optimum clamping force, array and number of spring needed to be specified. The spring grade SWF 12-50 with a spring constant \( k \) of 73.5 N/mm was finally selected for the fixture. This fixture included eight springs for supporting both clamping plates and exhibited the array in a serial pattern. By this installation, a pre-load of around 1970 N and a maximum pressure of 1.58 MPa were realized. Almost no buckling was observed by the cyclic tests using this fixture installation.

By the tests, specimens were tensioned up to a total strain of about 7–11% in order to achieve a saturation of determined stress-strain curve. Afterwards, they were compressed until the plastic strain became zero. During the experiment, local displacement was measured by an Advanced Video Extensometer (AVE) system. Fig. 2 presents the gathered cyclic stress–strain curve under tension–compression load in comparison with stress–strain curve from monotonic tensile test. A fair agreement in the tension region of both stress–strain curves could be observed. The saturated zone of each cyclic curve could be identified by means of relation between strain hardening \( \frac{d\sigma}{d\varepsilon} \) and true strain. The strain hardening of material continuously decreased until reaching a constant state. Within this stress range model parameters were determined.

**Fig. 2** Comparison between experimentally obtained cyclic stress–strain curve under tension-compression load and monotonic tension stress–strain curve
A stamping test of a hat-shape sample was carried out to verify the determined model parameters. The dimensions of die and punch were described in details in Chongthairungruang et al. (2013). The initial size of blank sheet was 50 mm in width, 314 mm in length and 1.4 mm thick. The clearance of 7% of the sheet thickness was designed for the dies. In forming process, a metal plate with a thickness of 1.6 mm or so-called dummy plates were used in order to inhibit blank holder force. After the forming tests, final dimensions of stamped parts were measured using a 3D laser scanner. Shapes of the formed samples along cross-section after tool removal were considered for springback evaluation. The springback and sidewall curl were characterized by using three variables, the angles $\theta_1$ and $\theta_2$ and curl radius $R$.

4. Determination of model parameters

The Yoshida–Uemori hardening model used two sets of parameter for describing material behavior. These both parameter sets are presented as following.

a) Parameters representing relationship between Young’s modulus and degree of pre-strain. This parameter set consists of $E_0$, $E_a$ and $\xi$.

b) Parameters representing plastic behavior of material. This parameter set includes seven parameters, namely, $Y$, $C$, $B$, $R_{sat}$, $b$, $k$, and $h$.

The parameters for elastic behavior were determined by interrupted tensile tests with various pre-strains as mentioned before. Determination of the plastic parameters is discussed here. Regarding constitutive equations of the Y–U model, the plastic parameters are dependent. Therefore, each parameter could not be identified individually or varied without any effect on other parameters. Thus, in this work, a simply method was introduced for determining sets of parameters and investigating their influences on cyclic stress–strain curves. This procedure
could be applied in general without any further optimization method. The determination procedure could be presented in detail as following.

4.1 Parameter Y

Principally, the parameter $Y$ is the material yield stress and could be determined by a standard method. The 0.2% offset strain was used to define the elastic limit. Fig. 2 showed this elastic limit or yield strength of the investigated JSC780Y steel in comparison with the monotonic and cyclic curve.

4.2 Parameters from forward stress

Here, from the cyclic experiments, stress–strain data of the plastic zone beginning with the parameter $Y$ were used. This plastic stress–strain curve under tension and compression were used to define the forward and reverse bounding surface. The forward bound stress according to Eq. 8 was related to the Voce hardening model, which could be expressed by the following equation:

$$\sigma_{iso}^{(Voce)} = B + (A)(1 - e^{-k eag})$$ (12)

Where $A = R_{sat} + b$. Therefore, a forward bound curve could be simply described by Eq. 12. However, range of data, which were used for curve fitting, strongly affected on the variation of obtained parameter values. In this work, three ranges of data, namely, 50%, 75% and 100% of the total forward plastic strain $\varepsilon_{0}^p$ were considered for finding the parameter set, as illustrated in Fig. 3a. From these ranges of data, three different sets of parameter were determined from the forward bound stress as following.

50% range of $\varepsilon_{0}^p$:

$$\sigma_{iso}^{(Voce)} = 608.095 + (355.647)(1 - e^{-15.872e^p})$$

75% range of $\varepsilon_{0}^p$:

$$\sigma_{iso}^{(Voce)} = 565.676 + (367.699)(1 - e^{-20.755e^p})$$
100% range of $\varepsilon_0^p$:

$$\sigma_{iso}^{(Voc)} = 489.528 + (399.003)(1 - e^{-32.330\varepsilon^p})$$

Fig. 3 (a) Fitting of forward bound stress curve using different ranges of data (50%, 75% and 100% of the total forward plastic strain) and (b) fitting of reverse bound stress curve using different ranges of data (25%, 50% and 75% of the total reverse plastic strain)

4.3 Parameters from reverse stress

The point (d) in Yoshida et al. (2002) represented a re-yielding point during compression, which was two times the elastic limit or $2Y$. Only plastic zone of the reverse stress was taken into account, thus the reverse stress curve was plotted backward to the plastic strain $\varepsilon_0^p$ of 0.088. In addition, point (k) exhibited an isotropic boundary, for which value of the forward stress at a plastic strain of 0.088 was used. For the investigated steel, the value of point (k) was about -876.12 MPa. Then, point (j) could be identified by an intersection of a linear extrapolation of the saturated reverse stress with the plastic strain of 0.088. Three ranges of data of the reverse bound curve from the saturated point were incorporated, namely, 25%, 50% and 75% of the total reverse plastic strain, as shown in Fig. 3b.

By this manner, three values of point (j) in Yoshida et al. (2002) were obtained for these different ranges of data.

25% range of $\varepsilon_0^p$:

value of point (j) was -777.87 MPa

50% range of $\varepsilon_0^p$:

value of point (j) was -752.36 MPa

75% range of $\varepsilon_0^p$:

value of point (j) was -684.71 MPa

Furthermore, the extrapolated line up to a deviation point of the reverse bound curve represented the region of permanent softening. The extrapolated line beyond this region until
the total plastic strain described the transient Bauschinger effect. They were used in conjunction with Eq. 9 to calculate the parameter $b$.

4.4 Parameter $C$

Generally, in some literatures the parameter $C$ was determined by an optimization method. Such kind of method was sophisticated and expensive for industrial cases. On the other hand, solving for $C$ from Eq. 10 and Eq. 11 could be another possibility. However, in this work more simple method for finding the parameter $C$ was applied. The original backstress model as defined in Eq. 13 was here considered.

$$\dot{\alpha} = C(a\dot{\varepsilon}^p - \alpha_\varepsilon^p)$$  \hspace{1cm} (13)

This was the principle equation before it was modified by Yoshida et al. (2002) to be Eq. 2. Thus, the value of $C$ was then determined by a relation between cyclic tension–compression test data and $C$ parameter, as expressed in Eq. 14.

$$\sigma_B^{(i)} \approx 2ae^{-C\dot{\varepsilon}}$$  \hspace{1cm} (14)

Where $\sigma_B^{(i)}$ described the differences between the reverse stress-strain curve from the range (d)-(e) and the extrapolated line of the transient Bauschinger region ((e)-(j)) in Yoshida et al. (2002). $\dot{\varepsilon}^p$ is the reverse plastic strain. Fig. 4 showed the proposed method for determining the parameter $C$. By this manner, the value of 92.037 was obtained for the parameter $C$. It was noticed that stress–strain curves obtained by this value in the transient Bauschinger zone was acceptable when comparing with experimental results.

Fig. 4 Determination of parameter $C$: (a) $\sigma_B^{(i)}$ and (b) curve fitting by Eq. 14
4.5 Parameter \( h \)

In literatures, \( h \) was a material parameter that was used to adjust the results of cyclic stress–strain curves from experiments and simulations. Nevertheless, it was found in this work that influence of the parameter \( h \) on the results of cyclic simulations was slightly in LS-DYNA solver. Therefore, the default value of \( h = 0.5 \) was taken into account.

From the procedures mentioned above, various parameter sets were obtained and provided in Table 2. All seven plastic parameters of the Y–U model for each range of fitting data were presented. It was seen that the parameters could be quite different when the range of fitting data was varied. These parameter sets were further applied for FE simulations of 1–element model in order to investigate their effects on cyclic stress–strain responses.

Table 2

Determined parameter sets for the investigated JSC780Y steel

5. FE simulations

5.1 1-element model

FE simulations of tension–compression test were performed in LS-DYNA by using a single plane stress shell element model. The model was a square element with a size of 1x1 mm. The left–bottom node was constrained in both x– and y–direction, while the right–bottom node was constrained in the y–direction. Displacements in positive and negative y–direction were applied to the top side of the element. The element formulation type 2 (Belytschko-Tsay) with one integration point through thickness was used. This element type was also further applied in forming simulations. The results of the each parameter set were
compared with the experimental data. Afterwards, averaged deviations between both obtained
stress–strain curves were calculated.

5.2 Forming simulation

In addition, numerical FE simulations of the hat–shape sample were carried out in LS-
DYNA for predicting the springback effect. It was also used to verify springback results of
the parameter set, which was the best fit prediction of the cyclic stress–strain responses. The
models of blank and tools were defined using shell elements and boundary conditions were
generated according to the experimental setup. The friction coefficient of 0.125 was given for
every contact surface. The calculated results were compared with the shape of the stamped
part.

6. Results and Discussion

6.1 Cyclic stress–strain curves

To investigate influences of using different range of fitting data, 1–element FE simulations
were conducted with all parameter sets, given in Table 2. Some deviations of stress–strain
curves from each parameter sets were observed. The cyclic curves mainly differed at the
beginning zone of forward stress and around the saturation zone of reverse stress. When data
range of the forward stress was the same, but data range of the reverse stress was increased,
increases of the stress–strain curves in both tension and compression regions were found. The
whole cyclic curves seemed to shift when only the parameters from the reverse stress were
altered. In case of using increased data range of the forward stress, but the same data range of
the reverse stress, decreases of the forward stress were observed. Additionally, the calculated
curves were compared with the experimental curve for an entire cycle and errors of each case
were then determined. The mean differences and standard deviations for each parameter sets
were summarized in Table 3. It was found that the range of fitting data, which provided the
best agreement with the experimental data, was the 50% range of $\varepsilon_0^p$ from the forward stress in combination with the 75% range of $\varepsilon_0^p$ from the reverse stress (case C). The largest difference value was noticed by the 75% range of $\varepsilon_0^p$ from the forward stress and the 25% range of $\varepsilon_0^p$ from the reverse stress (case D). The cyclic stress–strain curves of both parameter sets were compared with the experimental curve in Fig. 5. The worst parameter set exhibited significant lower forward stress, lower reverse stress in the range of permanent softening and higher reverse stress in the transient Bauschinger region. For the best parameter set, a maximum stress difference of 20 MPa was detected.

Table 3
Mean errors and standard deviations between simulations and experiments for each parameter sets

Fig. 5 Comparison of stress–strain curves experimentally determined and calculated using the best fit and the worst fit parameter sets

6.2 Springback results

To verify springback effects predicted by the Y–U model using the determined parameters, FE simulations of hat–shape sample were carried out. Both parameter sets discussed before were applied to the simulations to investigate influences of model parameters on the calculated springback. Experimental and simulation results regarding both designated angles $\theta_1$, $\theta_2$ and sidewall curl radius $R$ were determined. In Fig. 6, dimension of stamped part obtained from the experiment and predicted by the simulations using two different parameter sets were compared. All three springback variables obtained from the experiments and predicted by the simulations were compared with resulted % error in Fig. 7. The calculated
springback results of both parameter sets were similar and slightly different from the experimental one. Nevertheless, error of the best fit parameter set was a little bit lower than the worst fit parameter set for every springback variables.

**Fig. 6** Shape comparison of stamped part determined by experiment and predicted by simulations applying two parameter sets

**Fig. 7** Comparison of springback angles and sidewall curl radius of stamped parts measured by experiments and predicted by FE simulations and % error.

7. Conclusions

In the current study, the Yoshida–Uemori model was used to investigate springback effect of the JSC780Y steel. This kinematic hardening model consists of seven plastic parameters. The simple approach for determining model parameters from cyclic tension–compression stress–strain curves was introduced in detail. Nevertheless, different parameter sets were obtained. The influences of these parameter sets on the predicted stress–strain responses were examined by means of 1–element FE simulations under tension–compression load. In addition, it was found that the determined parameters provided acceptable agreement with the experimental results. For verification of the parameters, stamping tests of the hat–shape sample were carried out. The calculated springback results of different parameter sets were similar and slightly different from the experiments. The error of the best fit parameter set was a little bit lower than the worst fit parameter set for every springback variables. The recommended procedure for parameter determination of the Y–U model for the investigated steel is here summarized.

- The parameter $Y$ was identified as the 0.2% offset yield stress.
- The forward bounding surface was obtained by fitting the 50% range of the forward stress data and using the Voce hardening model.

- The reverse bounding surface was determined by fitting the 75% range of the reverse stress data and using a linear equation.

- The parameter $C$ was identified by the differences between the reverse stress–strain curve and the extrapolated line of the transient Bauschinger region.

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References


Fig. 1 (a) True stress-strain curves from loading-unloading tensile test for investigating elastic modulus change and (b) dependency between elastic modulus and effective plastic strain of the investigated steel.
Fig. 2 Comparison between experimentally obtained cyclic stress–strain curve under tension–compression load and monotonic tension stress–strain curve and determination of the parameter $Y$ by using the 0.2% offset strain for the investigated JSC780Y steel.
Fig. 3 (a) Fitting of forward bound stress curve using different ranges of data (50%, 75% and 100% of the total forward plastic strain) and (b) fitting of reverse bound stress curve using different ranges of data (25%, 50% and 75% of the total reverse plastic strain)
Fig. 4 Determination of parameter $C$: (a) $\sigma^{(f)}_B$ and (b) curve fitting by Eq. 14
Fig. 5 Comparison of stress–strain curves experimentally determined and calculated using the best fit and the worst fit parameter sets
Fig. 6 Shape comparison of stamped part determined by experiment and predicted by simulations applying two parameter sets.
Fig. 7 Comparison of springback angles and sidewall curl radius of stamped parts measured by experiments and predicted by FE simulations and % error.
Table 1

Mechanical properties of the investigated steel obtained by tensile test and parameters for relationship between elastic modulus and pre–deformation

<table>
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<tr>
<th>Direction</th>
<th>$E$ (GPa)</th>
<th>$\sigma_y$ (MPa)</th>
<th>$\sigma_u$ (MPa)</th>
<th>Elongation (%)</th>
<th>$r$-value</th>
<th>Poisson's Ratio (assumed)</th>
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<td>00°</td>
<td>199.852</td>
<td>457.804</td>
<td>809.673</td>
<td>14.122</td>
<td>23.894</td>
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</tr>
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<td>45°</td>
<td>187.057</td>
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<td>805.465</td>
<td>15.119</td>
<td>27.019</td>
<td>1.1508</td>
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<tr>
<td>90°</td>
<td>219.998</td>
<td>469.094</td>
<td>819.639</td>
<td>14.561</td>
<td>24.576</td>
<td>1.1116</td>
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<table>
<thead>
<tr>
<th>$E_0$ (GPa)</th>
<th>$E_{sat}$ (GPa)</th>
<th>$\xi$</th>
</tr>
</thead>
<tbody>
<tr>
<td>199.852</td>
<td>168.625</td>
<td>37.046</td>
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Table 2

Determined parameter sets for the investigated JSC780Y steel.

<table>
<thead>
<tr>
<th>Range of fitting data</th>
<th>Parameters</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$Y$</td>
</tr>
<tr>
<td><strong>Forward stress</strong></td>
<td></td>
</tr>
<tr>
<td><strong>Reverse stress</strong></td>
<td></td>
</tr>
<tr>
<td>50%</td>
<td></td>
</tr>
<tr>
<td>25% (A)</td>
<td>608.095</td>
</tr>
<tr>
<td>50% (B)</td>
<td>608.095</td>
</tr>
<tr>
<td>75% (C)</td>
<td>608.095</td>
</tr>
<tr>
<td>75%</td>
<td></td>
</tr>
<tr>
<td>25% (D)</td>
<td>565.676</td>
</tr>
<tr>
<td>50% (E)</td>
<td>565.676</td>
</tr>
<tr>
<td>75% (F)</td>
<td>565.676</td>
</tr>
<tr>
<td>100%</td>
<td></td>
</tr>
<tr>
<td>25% (G)</td>
<td>489.528</td>
</tr>
<tr>
<td>50% (H)</td>
<td>489.528</td>
</tr>
<tr>
<td>75% (I)</td>
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Table 3

Mean errors and standard deviations between simulations and experiments for each parameter sets.

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<th>Error between simulation and experiment</th>
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<tr>
<td></td>
<td>25% (A)</td>
</tr>
<tr>
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<td>50% (B)</td>
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<td>75% (C)</td>
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<tr>
<td>75%</td>
<td></td>
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<tr>
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<td>25% (D)</td>
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<tr>
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<td>50% (E)</td>
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<td>50% (H)</td>
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<tr>
<td></td>
<td>75% (I)</td>
</tr>
</tbody>
</table>