Predicting Shear Failure of Dual-Phase Steels

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Abstract. Dual-phase (DP) steels are being used increasingly to make automotive panels because of their advantageous combinations of high ductility (for forming) and high strength (for service). However, their adoption has been limited because of failures during die tryout that are unpredicted by the usual methods of finite element modeling and forming limit diagrams. The failures, often called “shear failures” occur at regions of high curvature (low R/t) where sheet of thickness t is drawn over a tool radius R. Recent work revealed that the type of failure and the formability of DP steels depend not only on R/t, but also on strain rate, an effect derived from the propensity of these steels to locally heat in areas of high strain when strain rates are sufficiently high to limit heat transfer. The formability is reduced significantly by the thermal effect for rates greater than approximately 0.1/s. This result explains at least partially why forming limit diagrams, which are measured quasi-statically (and thus isothermally) do not reflect the behavior of DP steels formed industrially (at typical strain rates of approximately 10/s). In order to apply laboratory test results of draw-bend formability to industrial forming operations, the inputs to commercial finite element codes (constitutive equations, forming limits) must be adapted to the reality of the material (DP steel) and underlying physics (thermal effects on constitutive behavior). Toward this end, two procedures have been developed and tested, one numerical and one analytical. Together they predict similar forming limits and provide a path for understanding the applied formability of DP steels.

Keywords: Advanced High Strength Steel, Plane Strain Draw-Bend, Localized Necking, Thermo-Mechanical Simulation

PACS: 62.20.M-

INTRODUCTION

Advanced high strength steels (AHSS) offer impressive combinations of strength and ductility that can reduce the mass and improve the crash worthiness of sheet-formed automotive parts and vehicles. Depending on the application and the grade, failures in AHSS are not always predictable by usual forming simulations and applications of forming limit diagrams (FLD) which are successful in predicting failures for conventional steels. Failures in AHSS often occur at die corner radii and at sheared edges of sheet steels, where FE simulations do not predict failures [1]. The former type of failure, so-called “shear fracture,” was observed at sharp radii where sheet steel experienced bending and unbending under tension. Contrary to failures observed with traditional steels (HSLA, for example), cracks appear in regions of bending. Conventional wisdom attributes both of these phenomena to a damage / void growth mechanism unique to AHSS [2].

The objective of this research is to understand the draw bend failure of AHSS based on the draw bend fracture tests and to improve FEM failure predictions by taking into account the thermal effect using an accurate constitutive equation relating flow stress to strain, strain rate, and temperature.

H/V CONSTITUTIVE MODEL

Dual-phase steel DP980, with a thickness 1.43 mm and hot-dipped galvanneal coating, was used for this study. For understanding mechanical behavior at room and elevated temperatures, tensile tests were conducted under isothermal conditions at 25°C, 50°C, 75°C, and 100°C, a range which covered expected temperatures induced by plastic deformation during forming. Standard ASTM E8-04 tensile specimens with a reduced gage section of 50 mm...
x 12.5 mm and 2% width taper were placed between optionally heated aluminum side plates pressed with 2.24 kN side force to keep maintain good contact and temperature uniformity. Specimens were deformed at a strain rate of \(10^{-3}\) s\(^{-1}\) after reaching the testing temperature. Stress data were corrected in terms of bi-axial stress and friction after testing. PTFE sheets were used on the side plates to reduce friction between the specimen and the side plates. The detailed correction process and the reliability of the correction can be found in Boger et al. [3] and Piao et al. [4].

For measuring the strain rate sensitivity of DP steels, strain rate jump-down tests, changing from a higher strain rate to lower strain rate, were employed. For a material which has low strain rate sensitivity such as most steels at room temperature, the strain rate jump test is preferred because it eliminates specimen-to-specimen variations. For better results, rate changes were applied only one time per test at an engineering strain of 0.1. The strain rate was changed in the range from 1 to \(10^7/s\) by controlling the crosshead speed.

A new empirical work hardening constitutive model, H/V model [5], was constructed from three multiplicative functions relating to strain hardening, strain rate sensitivity, and temperature softening, given as

\[
\sigma = \sigma(\varepsilon, \dot{\varepsilon}, T) = f(\varepsilon, T) \cdot g(\dot{\varepsilon}) \cdot h(T)
\]  
(1)

The temperature-sensitive strain hardening function, \(f\), was expressed as a novel linear combination of the Hollomon [6] and Voce [7] equations with a temperature-dependent proportion, given as

\[
f(\varepsilon, T) = \alpha(T) f_H + (1 - \alpha(T)) \cdot f_V
\]  
(2)

where \(\alpha(T) = \alpha_1 - \alpha_3(T - T_{RT})\), \(f_H = K \varepsilon^n\), \(f_V = A[1 - B \exp(-C\varepsilon)]\), and \(T\) and \(T_{RT}\) are the current and room temperatures, respectively. The other functions, \(g\) and \(h\), have standard forms representing the strain rate sensitivity and temperature softening, respectively [8,9,10], given as

\[
g(\dot{\varepsilon}) = \left(\frac{\dot{\varepsilon}}{\dot{\varepsilon}_0}\right)^m
\]  
(3)

\[
h(T) = (1 - D \cdot (T - T_{RT}))
\]  
(4)

where \(m = a[\log \dot{\varepsilon}] + b\) [5,11]. The best-fit material constants for the dual-phase steel DP980 are summarized in Table 1.

**ADIABATIC CONSTITUTIVE EQUATION**

Though a fully coupled thermo-mechanical finite element model may provide accurate failure prediction capability [12], it requires intensive computation. Commercial sheet-forming FE programs typically do not have a thermo-mechanical capability. For computational efficiency at sufficiently high rates, an adiabatic constitutive equation may be used with standard isothermal FEA to approximate the full thermo-mechanical solution.

The adiabatic flow stress is calculated by

\[
\sigma_a(\varepsilon) = \sigma(\varepsilon, \dot{\varepsilon}(\varepsilon), \Delta T)
\]  
(5)

where the temperature increment is given by

\[
\Delta T = \frac{\eta}{\rho C_p} \int_0^\varepsilon \sigma d\varepsilon
\]  
(6)

with the given history of \(\dot{\varepsilon}\). Here, the following material constants for the steel are used: \(\rho = 7.9 \text{ g/cm}^3\), \(C_p = 0.45 \text{ J/g \cdot K}\), and \(\eta = 0.9\).
<table>
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<th>K (MPa)</th>
<th>n</th>
<th>A (MPa)</th>
<th>B</th>
<th>C</th>
<th>α₁</th>
<th>α₂</th>
<th>a</th>
<th>b</th>
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<td>0.154</td>
<td>910</td>
<td>0.376</td>
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**THERMO-MECHANICAL FINITE ELEMENT SIMULATION**

A novel draw-bend fracture (DBF) test has been developed [13] based on a modification of a draw-bend test that has been used for friction and springback test, as shown in Fig. 1. In the DBF test the front grip moves at a constant speed, \( V_1 \), and the back grip moves at a constant speed, \( V_2 \), giving a fixed ratio of \( V_2 / V_1 \). This guarantees the forward movement of the sample over the die radius, thus fracture always occurs toward the front leg of the specimen (this is not guaranteed for previously-used back-force-controlled DBF tests [14,15], leading to difficult-to-interpret results). In the experiments, 25 mm wide sheet samples were with the strip axis parallel to the sheet rolling direction. Tests were performed with fixed rollers with diameters between 6.3 mm and 38.1 mm. For all tests a standard stamping lubricant was used to mimic interfacial conditions in typical forming processes. The tests proceeded until sample failure, either by tensile plastic localization/necking or by shear failure, depending on the material properties and testing conditions.

**FIGURE 1.** Schematic of draw-bend fracture test

**FIGURE 2.** Three fracture types observed during the draw-bend fracture test
DBF tests of the DP steel revealed three failure patterns based on process conditions, as shown in Fig. 2. Type I is a standard tensile failure in the un-bent front free ligament at a position unaffected by the localized bend region near the tool. Type III is what is often called “shear failure” that occurs at the exit tangent point along a line oriented perpendicular to the strip axis with limited deformation in the width direction. Type II has a mixed appearance that appears to initiate like Type III but propagates at an angle in material that has been drawn over the tooling.

A thermo-mechanical finite element model of draw-bend tests was developed to investigate failure mechanisms in draw-bend testing, as shown in Fig. 3. The model accounts for deformation heating and heat transfer and is capable of representing softening and altered strain hardening of materials measured at elevated temperatures by adopting the H/V constitutive model. A symmetric 3D solid model (C3D8RT) was used with five element layers through the thickness in Abaqus 6.7 Standard [16]. Isotropic yield and isotropic hardening were assumed for simplicity. Thermal coefficients were measured from independent experiments or obtained from the literature [17]. The fraction of plastic work converted to heat was measured as 0.9 in agreement with the value from the literature [18]. A Coulomb friction coefficient of 0.06 was adjusted by comparing forces of front grip and back grip for FE simulation and DBF test and used in the failure predictions.

Figure 4 shows the front stress-front displacement curves for the case with \( V_1 = 51 \text{ mm/s}, V_2/V_1 = 0, \) and \( R/t = 2.2. \) The front stress is defined as the front force divided by the product of the initial cross-section area. The stress increased with sheet displacement over the tool radius and the sheet failed at the tool (Type III). The measured front displacement to failure was 13 mm. Both the thermo-mechanical and isothermal FE solutions over-predicted the displacement to failure but the error of the isothermal simulation is much greater than that of the thermo-mechanical simulation: the thermo-mechanical simulation predicted 20 mm (an increase of 54%), while the isothermal simulation predicted 28 mm (an increase of 115%). Both simulations predicted Type III failure which matches the observed failure type.

Figure 5 shows the variation of maximum front stress for tests imposing various R/t ratios. For the small R/t ratios (R/t < 6), Type III failures were observed and the maximum stress increased as R/t increased. For larger R/t ratios (R/t > 6), Type I failures were observed and the maximum stress remained constant, close to the ultimate tensile strength. The predicted maximum stress variation with R/t compared well with the measurements. The thermo-mechanical model correctly predicted the failure types and captured the transition from Type III to Type I as R/t increased.

**PLANE STRAIN FRACTURE CRITERIA**

Draw-bend testing is not precisely analogous to the critical situation in most industrial forming, where drawing and stretching over a die radius occurs in long sections that impose conditions close to plane strain, particularly in the sheet-tool contact region. In order to translate draw-bend fracture information to industrial practice, two plane-strain models were constructed: one numerical (FE), the other analytical.
FIGURE 4. Comparisons of the measured and predicted the front stress vs. front displacement curves.

FIGURE 5. Maximum front stress for various R/t ratios.

FIGURE 6. Finite element model of the plane strain draw-bend fracture test.
An adiabatic finite element model of a plane strain draw-bend test was developed using the plane strain element (CPE4R) in Abaqus 6.7 Standard [16] to investigate the failure criteria of AHSS in draw-bend testing, as shown in Fig. 6. The model accounts for deformation heating under adiabatic conditions and is capable of representing softening and altered strain hardening of materials measured at elevated temperatures by the use of the H/V constitutive model. The finite element model had five layers of elements through thickness. Isotropic yield and isotropic hardening were assumed for simplicity. The friction coefficient of 0.06 was used. The conditions \( V_1 = 51 \text{mm/s}, \) and \( V_2 / V_1 = 0 \) were used. The simulation proceeded until the front load decreased due to plastic localization/necking.

An analytical procedure for plane-strain sheet bending under tension was developed, Fig. 7. At a plane where the radius of curvature is \( r \), the normal logarithmic strains in the \( \theta \)- and \( r \)-directions are given by

\[
\varepsilon_\theta = \ln \left( \frac{r}{r_n} \right), \quad \varepsilon_r = -\ln \left( \frac{r}{r_n} \right)
\]  

(7)

where \( r_n \) is the radius of curvature of the neutral plane.

Ignoring elasticity and assuming plastic isotropy, the equivalent plastic strain is given by

\[
\bar{\varepsilon} = \frac{2}{3} \varepsilon_{\theta \theta} = \frac{2}{3} \ln \left( \frac{r}{r_n} \right)
\]

(8)

The equilibrium equation is given by

\[
\frac{\partial \sigma_r}{\partial r} = \frac{\sigma_{\theta \theta} - \sigma_r}{r} = \pm \frac{2}{\sqrt{3}} \bar{\sigma} \quad (\text{upper: } r > r_n, \text{ lower: } r < r_n)
\]

(9)

where \( \sigma_r \) and \( \sigma_{\theta \theta} \) are the normal stresses in the \( r \)- and \( \theta \)-directions, respectively, and \( \bar{\sigma} \) is the effective stress.

The relationship between \( \bar{\sigma} \) and \( \bar{\varepsilon} \) is given by Equation 5 at a constant strain rate (\( \dot{\varepsilon} = 0.2 / \text{s} \)). The boundary conditions are \( \sigma_r = 0 \) at the outer surface and \( \sigma_r = -T / R \) at the inner surface where \( T \) is the tensile force and \( R \) is the inner surface radius of curvature. The effect of friction during bending under tension is ignored.

By solving Equation 9 numerically with Equations 5 and 8 [19], the tensile force is obtained for a given \( r_n \) and \( R \). Necking failure is assumed to occur when the tensile force reaches a maximum, i.e., where the sheet deformation becomes unstable. The predictions presented here are consistent with the results of the analytical analysis to predict instability and shear fractures at critical R/t values in draw-bend tests of AHSS by Hudgins et al. [20].

![Neutral line diagram](image)

FIGURE 7. Plane strain bending under tension
Stress-based failure criteria (failure stress vs. R/t) were obtained using the finite element and analytical procedures, as plotted in Fig. 8. Two cases were calculated for the finite element procedure: rate sensitive with friction and rate insensitive without friction. The failure criterion obtained using the finite element procedure (rate insensitive, frictionless) agreed well with that obtained using the analytical procedure. The small discrepancy between the two may be attributed to the out-of-plane shear deformation which was ignored in the analytical procedure. The failure stresses of the rate sensitive case with friction are slightly higher than those of the frictionless and rate insensitive case. When a tensile stress is kept below the criterion, the forming process can be done safely without shear fracture. The failure stresses of the finite width specimen (Fig. 5) are also plotted for comparison. The failure stress of the finite width specimen is close to that of the plane strain case when the R/t is small (R/t < 3) but deviates from it as the R/t increases.

Strain-based failure criteria (failure strain in the longitudinal direction at the outer surface vs. R/t) were obtained using the finite element and analytical procedures, as plotted in Fig. 9. Again, the failure criterion obtained using the finite element procedure (rate insensitive, frictionless) agreed well with that obtained using the analytical procedure, while the failure strains of the rate sensitive case with friction are slightly higher than those of the frictionless and rate insensitive case. When the strain at outer surface is kept below the criterion, the forming process can be done safely without shear fracture.

**FIGURE 8.** Stress-based failure criteria obtained by the plane-strain finite element and analytical procedures (μ: friction coefficient)

**FIGURE 9.** Strain-based failure criteria obtained by the plane-strain finite element and analytical procedures (μ: friction coefficient)
CONCLUSIONS

In order to understand the formability behavior of AHSS, novel bi-velocity controlled draw-bend formability (DBF) tests were developed and three failure types, including shear failure, were produced. The effect of deformation induced heating must be considered for accurate prediction of failure at typical industrial strain rates. In order to predict shear failure during plane strain draw-bending, adiabatic plane-strain finite element and analytical procedures were developed. A novel H/V constitutive model was used. Two procedures were developed: a finite element model of the plane strain draw-bend test was constructed using Abaqus Standard 6.7, while the equilibrium equation was solved to obtain the tensile force required to bend a sheet in plane strain over a radius with a given neutral plane. In both procedures, localized necking was assumed to initiate at the maximum tensile force. Stress (and strain)-based failure criteria, (failure stress (and strain) vs. radius plots) were obtained using finite element and analytical procedures, which showed good agreement with each other. The criteria obtained by these models can be used to design the forming processes that exploit the real formability of AHSS.

ACKNOWLEDGMENTS

The work presented here is supported by more than one collaborating project with cooperative funding by the Auto-Steel Partnership, the Department of Energy (Award Number DE-FC26-02OR22910), the National Science Foundation (CMMI-0727641 (RHW) and CMMI-0729114 (DKM)), the Advanced Steel Processing and Products Research (ASPPRC), an industry/university cooperative research center at the Colorado School of Mines, the Transportation Research Endowment Program at the Ohio State University, and the Leading Industry Development Project for Economic Region funded by the Ministry of Knowledge Economy in Republic of Korea.

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