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Analysis was carried out to predict the attainable compressive strains using novel finite element (FE) modeling and a single parameter characteristic of the machine and fixtures. The limits of compressive strain vary primarily with the material thickness and the applied-side-force-to-material-strength ratio. Predictions for a range of sheet alloys with measured buckling strains from 0.04 to 0.17 agreed within a standard deviation of 0.025 (0.015 excluding one material that was not initially flat).

In order to demonstrate the utility of the new method, several sheet materials were tested over a range of temperatures. Some of the data obtained is the first of its kind. Magnesium AZ31B sheets were tested at temperatures up to 250°C with strain rate of 0.001/s. The inflected stress-strain curve observed in compression at room temperature disappeared between 125°C and 150°C, corresponding to the suppression of twinning, and suggesting a simple method for identifying the deformation mechanism transition temperature. The temperature-dependent behavior of selected advanced high strength steels (TWIP and DP) was revealed by preliminary tests at room temperature, 150°C and 250°C.
Highlights

- Tension-compression testing of sheet metals at elevated temperature was demonstrated.
- A method for predicting the compressive buckling strain was developed and demonstrated.
- The principal determinants of buckling strain are sheet thickness and side-force-to-UTS ratio.
- Cyclic tests of Mg AZ31B reveal a transition temperature for twinning.
- TWIP and DP steels show large Bauschinger effects from 25°C to 250°C.
A Sheet Tension/Compression Test for Elevated Temperature

K. Piao¹, J. K. Lee², J. H. Kim³, H. Y. Kim⁴, K. Chung⁵, F. Barlat⁶, R. H. Wagoner¹*

¹Department of Materials Science and Engineering, 2041 College Road
Ohio State University, Columbus, OH 43210, USA

²Department of Mechanical Engineering, Scott Laboratory, 201 West 19th Avenue
Ohio State University, Columbus, OH 43210, USA

³Materials Deformation Group, Korea Institute of Materials Science
797 Changwondaero, Changwon, Gyeongnam 641-831, Republic of Korea

⁴Division of Mechanical Engineering & Mechatronics, Kangwon National University,
192-1 Hyoja 2-Dong, Chunchon, Gangwon-Do, 200-701, Republic of Korea

⁵Department of Materials Science and Engineering, Research Institute of Advanced
Materials, Seoul National University, 599 Gwanak-ro, Gwanak-gu Seoul 151-742,
Republic of Korea

⁶Graduate Institute of Ferrous Technology, Pohang University of Science and
Technology, Pohang 790-784, Republic of Korea

ABSTRACT

An apparatus was designed, simulated, optimized, and constructed to enable the large-strain, continuous tension/compression testing of sheet materials at elevated temperature. Thermal and mechanical FE analyses were used to locate cartridge heaters, thus enabling the attainment of temperatures up to 350°C within 15 minutes of start-up, and ensuring temperature uniformity throughout the gage length within 8°C. The low-cost device also makes isothermal testing possible at strain rates higher than corresponding tests in air.

Analysis was carried out to predict the attainable compressive strains using novel finite element (FE) modeling and a single parameter characteristic of the machine and fixtures.
The limits of compressive strain vary primarily with the material thickness and the applied-side-force-to-material-strength ratio. Predictions for a range of sheet alloys with measured buckling strains from -0.04 to -0.17 agreed within a standard deviation of 0.025 (0.015 excluding one material that was not initially flat).

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Keywords: Mechanical testing; Elevated temperature tension/compression testing; Magnesium alloy AZ31B; TWIP steel; Dual Phase (DP) steel; Buckling analysis

*Corresponding Author: R. H. Wagoner; Tel.: +1-614-292-2079, Fax: +1-614-292-6530,

E-mail address: wagoner.2@osu.edu

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1. Introduction

The room-temperature testing of bulk materials under uniaxial tension (ASTM-E8-00 2000) and compression (ASTM-E9-89a 2000) are well established. However, methods for large-strain compression testing in the plane of sheet materials remain more challenging and specialized; they require suppression of buckling.

Two basic approaches have been presented, as reviewed elsewhere (Boger et al. 2005). The first method relies on a small specimen with specific length-to-thickness ratio in a range of 2 to 16 corresponding to a limit compressive strain from -0.01 to -0.15 (Abel and Ham 1966; Bate and Wilson 1986; Karman et al. 2001). The specimen geometry precludes accurate strain measurement, particularly for higher strain limits, because of the inhomogeneity of stress and strain.

The second method uses side force to stabilize compressive deformation of a typical tensile specimen. (Tan et al. 1994; Kuwabara et al. 1995; Balakrishnan 1999; Geng and Wagoner 2002; Yoshida et al. 2002; Boger et al. 2005; Lee et al. 2005). One such device (Kuwabara et al. 2001) uses comb-shaped “fingers” to reduce the size of the unsupported regions of specimen. Alternatively, sliding blocks can be used to clamp the specimen (Cheng et al. 2007) rigidly, thus permitting compressive strains around -0.1. The clamping force cannot be controlled, thus making the biaxial stress correction and friction correction problematic (Cao et al. 2009).
Boger et al. (2005) extended the second method to test sheet metals in continuous large-strain tension/compression tests at room temperature using a standard tensile testing machine. In the Boger method, two solid support plates sandwiched the specimen with a constant restraining side force generated by a hydraulic cylinder. An exaggerated dog-bone specimen was optimized by FEA simulations in order to suppress buckling in the thickness direction (T-Buckling), in the unsupported gap (L-Buckling), and in the width direction (W-Buckling). Compressive strains of -0.08 were achieved in cyclic tests, and homogeneous strains were accurately measured using a non-contact EIR™ laser extensometer. Biaxial stress and friction corrections were introduced to obtain equivalent uniaxial plastic behavior (Balakrishnan 1999). The current work focuses on developing an improved method and device capable of extending the temperature range of the technique to above room temperature.

Elevated-temperature methods for monotonic testing of metals in tension (ASTM-E21-92 1998) and compression (ASTM-E209-00 2005) are also standard. For compression testing, springs or screws are usually used to provide constraint lateral pressure without control or measurement. Various methods to reduce friction have been used: grooves (Gerard 1961; King 1961), balls (Hyler 1956; Gerard 1961; King 1961), leaf springs (Breindel et al. 1961; Gerard 1961), rollers (Bernett and Gerberich 1961), or solid blocks with lubricants (Fenn 1960; Gerard 1961; ASTM-E209-00 2005).

Heat sources for elevated temperature testing include convection (Hyler 1956; Gerard 1961; King 1961), radiation (Macdougall 1998; Yang et al. 2001), specimen resistance
(Fenn 1960; Bernett and Gerberich 1961; Fenn 1961), induction (Rosenberg et al. 1986), and embedded electrical resistance cartridges (Hyler 1956). Enclosing atmospheric furnaces are most common (Zhao 2000), but have drawbacks in terms of the extensometry, mechanical pass-through, slow heating rates, and limited isothermality at high testing rates (Hyler 1956; Gerard 1961; King 1961). Radiant heating offers rapid heating but requires placement of multiple thermocouples to insure accuracy and uniformity (Macdougall 1998; Yang et al. 2001), and can make laser extensometer difficult depending on the wavelength of the radiant energy. Electrical self-resistance heating (Fenn 1960; Bernett and Gerberich 1961; Fenn 1961; Sladik and Longauerova 1992) is attractive, but requires electrical isolation of the specimen.

The present work was aimed at providing a low-cost and readily-reproduced device and method for elevated temperature tension/compression testing, with a particular goal of testing Mg alloys between 25°C and 350°C. The following attributes of such a device and method were sought:

- Continuous, uninterrupted tension and compression cycling
- Uniform strain throughout the gage length, similar to that of room-temperature tensile testing
- Direct, accurate strain measurement from within the specimen gage length (thereby avoiding machine compliance issues)
- Use with standard tensile machine; self aligning for low capital cost and simplicity
- Heating from 25°C to 250°C in less than 10 minutes
- Temperature uniformity in the gage length, less than 5°C variation at 250°C
• Controlled, constant side force to enable accurate compensation for biaxial stresses and friction

2. Design and optimization for elevated-temperature tension/compression test

The Boger fixture design was modified by developing an integrated heating system to meet the objectives listed above, then performing thermal and mechanical finite element (FE) simulations to optimize it. The Boger alignment principle and specimen design were retained in order to maintain the advantages of large compressive limit strains and a simple, low-cost device to be used with standard tensile testing machines.

2.1. Design overview

In order to describe the design and optimization process concretely and briefly, Figure 1 shows the final design. The heating plates are equivalent to what were called “support side plates” in the Boger room-temperature device (Boger et al. 2005). They provide both the means for applying the anti-buckling side force to the specimen and for heating the specimen. Figure 1 shows 12 holes where cartridge heaters are installed in the heating plates, which are adjacent to wooden “insulating plates” that abut tool steel “backing plates.” These sets of three plates replace the side plates in the room-temperature Boger design.

Not visible in Figure 1 are the following details:

• The heating plates have Teflon®PTFE sheets (0.8 mm thickness) adhesively bonded onto the surfaces in contact with the specimen in order to minimize friction.
• Temperature control is provided by a Staco-Energy™ variable autotransformer (Staco-Energy™), Omega™ CSS-01125/120V cartridge heaters (Omega Engineering), Omega™ CN8201-R1 temperature controller (Omega Engineering), and a K-type thermocouple attached using high-temperature conductive paste OT-201-2 (Omega Engineering) at the center point on the edge of the sample. This system controls temperature fluctuation at the thermocouple within 1°C for testing temperature up to 300°C.

• A constant side force is supplied during testing by an air cylinder (101.6mm diameter working piston) controlled by a dial-set air regulator. Transient calibration tests of the air cylinder using a hydraulic testing machine showed that pressures were generally maintained within 5% of the set pressure even under “jump displacement” conditions, with an average precision of 3% above 40 psi and 7.5% below 40 psi. 40 psi corresponds to 2.23kN force, with other pressure / force settings being proportional¹.

• The air cylinder applies the side force through a set of rollers (the ends of which can be seen in Figure 1) adjacent to the backing plate in order to help reduce friction.

2.2. Thermal analysis

Convection, radiant, and embedded resistance heating systems were initially considered. Embedded resistance cartridges were selected because they offer an attractive combination of low cost, good temperature uniformity, and simplicity of extensometry.

¹ The air cylinder operating characteristics improve on the side force consistency as compared with the hydraulic cylinder configuration employed by Boger (Boger et al. 2005).
A thermal FE model of one eighth of the three-dimensional test configuration (as reduced by symmetry) was constructed as shown schematically in Figure 2 using Abaqus Standard (ABAQUS Inc. 2005). The thermal model consists of 9000 3-D solid elements (Abaqus DC3E8): 294 elements in specimen, 8080 elements in heat plate, 456 elements in insulating plate, 50 elements in backing plate, and 120 elements in grip. The design and optimization procedure was performed for a magnesium alloy AZ31B specimen, with properties as shown in Table 1. Table 1 also shows the pertinent properties for materials considered for other parts of the device along with definitions of symbols for these properties. Figure 2 presents the dimensions of devices, as well as heat transfer coefficients $h$ and boundary conditions used in the analysis. The heat transfer coefficients were determined as outlined in the appendix. The specimen design and dimensions have appeared elsewhere (Boger et al. 2005).

The principal design variables addressed by the FE thermal analysis were:

- cartridge capacity and number (based on transient heating time)
- number, material(s) and thickness(es) of the various plates (based on temperature attained)
- location of cartridges (for temperature uniformity)

The outcomes sought by adjusting these parameters are listed in the introduction. The first simulations determined the number and type of equal-spaced cartridge heaters (i.e. $Y_a=7.5, Y_b=22.5, Y_c=37.5$ mm in Figure 2(a)) in transient thermal modes. The cartridges were represented in the model by a surface heat flux on the hole interiors equivalent to the input power rating of the devices. It was concluded that 12 cartridges of the type CSS-01125 (Omega Engineering), which have maximum power ratings of 25 W, would likely
be adequate, and therefore were selected. These cartridges have diameters of 3.18mm and lengths of 38mm.

In the second step, five choices of structure and materials for backing plates were simulated: 1) monolithic plates of Al7075 or D2 tool steel, 2) two-layer plates (D2 tool steel backing plate for strength and stiffness, Al7075 heat plate for heat conduction), and 3) three-layer plates (with oak wood central layer for thermal insulation). The total input power in the model was 200W, i.e., a surface heat flux of 0.05W/mm² was applied on the inner surfaces of heater locations as determined by

\[
\frac{50W/\text{in}^2 \times \frac{200W}{12}}{25W} = \frac{33W/\text{in}^2}{0.05W/\text{mm}^2}.
\]

Table 2 summarizes the results for these cases (numbered 1-5). The three-layer plates (Al 7075 / oak wood / D2 tool steel) provided both the fastest heating times and most temperature uniformity along the specimen gage length.

For the last step in the design of the thermal system, the 3 sets of cartridge heater locations, shown in Table 2 were simulated. The set at 10, 16, 22mm provided the fastest heating rates (295s to 200 deg C, 725s to 300 deg C) with only minor difference of temperature uniformity along the specimen length (4.8 degrees at 200 deg C, 7 degrees at 300 deg C, within 1 degree of the values for the other heater locations), so these spacings were selected for the final design.

In order to verify the simulation accuracy heating plates were constructed with the 10, 16, 22mm cartridge locations and 11 thermocouples were embedded at the specimen
centerline through the edge of sample. The temperatures were recorded for a voltage of 105V and current of 1.85A, corresponding to a simulated heat flux of 0.048W/mm². A side force of 2.5kN was applied as the assembly was mounted in an MTS810 test frame (as shown in Figure 1). The results are compared in Figure 3, which verifies the fidelity of the simulation as well as the targeted capability to reach 250°C in 10 minutes with a 5°C variation of temperature within the gage length. The simulation also shows that 350°C is reached in 15 minutes with a maximum temperature difference of 8°C along the gage length and 1°C across the specimen width. Simulation using D2 steel as the testing sample shows that 250°C is achieved in 6.5 minutes with a 4.5°C variation of temperature within the gage length.

2.3. Mechanical analysis

Mechanical analysis was conducted using a simplified two-dimensional ABAQUS/Standard model for the x-y plane cross section (Figure 4) with maximum side force of 8kN (limited by the cylinder used in experiments). Plane strain elements (CPS4R) were used, so the dimensional change was omitted in the third direction. Concentrated forces applied at the backing plate simulated the contact with the rollers. Material properties are shown in Table 1. The maximum simulated von Mises stresses in the Mg sample and various combinations of side plates are summarized in Table 3. With reference to Table 1 for yield stresses of the component materials, all designs are suitable at room temperature, and there would be little concern for steel components at the temperatures encountered. However, for a 350°C test, the temperature in the Al7075 heat plate can reach 385°C, at which temperature the yield stress is approximately 95MPa
(ASM 1980; Lee et al. 2000), less than half of the stress experienced. Therefore, the tool-steel backing plates are essential for reducing the stress concentrations from the roller contacts and heater cartridge holes to manageable levels. For the final design, Design #4, the peak von Mises stress in the critical heating plate is less than 20% of the yield stress for a 350°C test.

2.4. Buckling analysis

Three modes of buckling behavior were simulated previously (Boger et al. 2005) in order to optimize the current specimen design in terms of extending the range of compressive strains that could be achieved. Buckling in the width direction was completely suppressed by the final design of the specimen. Buckling in the thickness direction, either within or outside of the plate contact regions, limits the compressive strain attainable\(^2\). In the current work, through-thickness buckling was simulated in order to predict the limit of attainable compressive strain in terms of specimen material properties, sheet thickness, and applied side force. These results can then be used to select the proper side force without trial and error and unnecessary sacrifice of specimens and time for that purpose.

Abaqus Standard simulations were carried out using the model shown in Figure 5. The specimen uses 8976 solid elements (C3D8R), 4 elements through the sheet thickness, while each side plate is represented by 1250 rigid-body elements (R3D4). Only the

\(^2\)The maximum compressive strain is also limited by the interference of the side plates with the grips. For the 10mm initial clearance used here, the maximum compressive strain allowed is -0.32. Ten millimeter is the minimum clearance that can be accommodated by the geometry of the grips.
region of the specimen between the two grip lines was modeled. Zero displacement
boundary conditions were enforced at each node at the bottom grip surface. For the top
grip surface, a constant buckling-initiating offset displacement in the z direction, $\delta_z$, was
enforced. The values of $\delta_z$ representing this machine and experimental set-up was
determined by matching results for a few experiments, as described next. The other
components of displacement at the top grip surface were enforced as follows: $\delta_x = 0$
(width direction) and $\delta_y$ (longitudinal direction) was incremented to allow simulation of a
compression test.

In order to attain convergence of the solution with Abaqus Standard, it was necessary to
use relaxed contact enforcement ("soft tools") in the form of a penalty function with the
parameters, $c$ and $p_o$:

$$p = \begin{cases} 
0 & h \leq -c \\
\frac{p_o}{\exp(1)-1} \left[ \frac{h}{c} + 1 \right] \exp \left( \frac{h}{c} + 1 \right) - 1 & h > -c 
\end{cases}$$

(1)

where $h$ is the "overclosure distance", that is, the distance that the one contacting body
penetrates into the other contacting body and $c$ is the value of $h$ when the contact pressure
$p$ becomes zero. $p_o$ is the contact pressure when the overclosure is zero ($h=0$). For a
typical simulation (DP800, side force=3.3kN, $t=1.40$mm), values of $c=0.1$mm and
$p_o=5$MPa were found to provide convergence with little degradation of solution
accuracy.\(^3\) In order to allow for the natural and the desirable scaling of material strength
and force boundary conditions (Chung and Wagoner 1986; Chung and Wagoner 1998), it

\(^3\) Applying a value $p_o$ five times larger, i.e., 25MPa, changed the simulated buckling strain for this case by
less than 0.005.
is essential that the only non-zero boundary conditions are displacement ones, or that any force boundary conditions are scaled with material strength. Therefore, $p^0$ was scaled according to the material UTS (ultimate tensile strength) as follows:

$$p^0 = \frac{5\text{MPa}}{800\text{MPa}} \cdot \sigma_{\text{UTS}} \text{MPa}$$

in which 800MPa is the UTS of DP800. Such a formulation assures that, in view of the invariance principle (Chung and Wagoner 1986; Chung and Wagoner 1998), that only the ratio of side force, $F_s$, to material strength (say UTS), i.e.:

$$F_s = \frac{F_s}{A_s}/\text{UTS}$$

is relevant. In Eq. 3, $F_s$ is the normalized side force, as shown, and $A_s$ is the contact area of the specimen upon which side force $F_s$ operates.

Table 4 presents standard tensile properties for a range of sheet metals tested, along with buckling-limited true strains from experiments, $\varepsilon_{b}^{\text{Exp}}$, and from simulations, $\varepsilon_{b}^{\text{FE}}$ (as will be introduced below). Constitutive equations were fit in a plastic strain range from 0.01 to $\varepsilon_{b}$ for compressive tests at a nominal initial strain rate of $10^{-3}/s$. Experimental stress-strain data points were input directly into Abaqus for the plastic strain range of $\varepsilon_{p}=0$ to 0.01 because the equations did not always capture this strain range adequately. A friction coefficient of 0.08 was determined from the slope of compression force vs. side force at a strain of -0.1 for DP780 and DP800 steels. The procedure followed has been presented elsewhere (Boger et al. 2005) and the value of friction coefficient obtained here was identical to the one determined in that work.
Figures 6 compare the buckling patterns from experiments and simulations. Figures 6a-c show through-thickness buckling in the heating plate contact region, accompanied by a corresponding increase of the plate clearance, $\delta_p$. If the side force is sufficiently large to suppress buckling in the plate-contact region, the non-contact buckling mode eventually intervenes without a change of $\delta_p$, as shown in Figs. 6d-e. The non-contact buckling mode cannot be suppressed with the design used here because of the unavoidable unsupported region outside of the plate contact.

Figure 7 illustrates the experimental and simulated buckling behavior of DP800 during a single compression test. The maximum load point corresponds to the strain at the initiation of buckling, $\varepsilon_b$. This criterion for the detection of buckling, maximum load or engineering stress, was used to define true buckling strain $\varepsilon_b$ subsequently (as shown in Table 4, for example). Figure 7 shows that the best-fit value of $\delta_z$ (the axial offset utilized in the simulation to initiate buckling, Fig. 5b) for this test and machine set-up is approximately 0.1mm.

Figures 8 compare simulations and experiments for DP steels for a range of side forces, sheet thicknesses (Fig. 8a, b) and material strengths (Fig. 8c). All of these results confirm that the value of $\delta_z$ is constant, equal to approximately 0.1mm. These figures also show that much larger strains could be obtained with larger side forces, at the expense of degraded stress-strain accuracy by the larger corrections required. Such corrections always carry uncertainty in terms of friction coefficient, plastic anisotropy, and so on.
Grip-plate interference also limits compressive strains to approximately -0.32 for the current configuration.

With the constant value of $\delta_c=0.1\text{mm}$ established and validated, the remaining part of Table 4 can now be appreciated. The last two columns compare measured and simulated buckling true strains for the full range of material strength (UTS from 250 to 1570MPa) and thicknesses (0.8-1.9mm). The difference between measured and simulated buckling strains is within 0.03, except for the AA6022 sample which has a relatively small thickness (0.93mm) and the lowest UTS. The experimental scatter for the buckling strain of AA6022 is double that of DP590, $t=1.4\text{mm}$. For DP590, four repeated compressive tests at side force of 3.3kN were conducted showing a standard deviation of 0.016. The buckling strains for all 20 tests (as presented in Figures 8 and Table 4) range from -0.04 to -0.17. The standard deviation of simulated buckling strain is 0.015 (excluding $\varepsilon_b$ of AA6022) or 0.026 (including $\varepsilon_b$ of AA6022). The simulations showed accurate predictions on buckling strain, with the standard deviation of 15\% ($\left(\frac{\varepsilon_b^{\text{FE}}-\varepsilon_b^{\text{Exp}}}{\varepsilon_b^{\text{Exp}}}\right)$ for 19 tests (excluding $\varepsilon_b$ of AA6022). The large deviation between simulations and experiments for AA6022 is presumably related to the noticeable non-flatness of these sheets. The other materials were nominally flat.

In order to verify the application of the invariance principle, a matrix of virtual test and material conditions was constructed and simulated. The matrix has 270 members as follows:

UTS: 200, 400, 600, 800, 1000MPa
Thickness: 0.5, 0.75, 1.0, 2.0, 3.0mm
Side force: 1.1, 2.2, 3.3, 4.4, 6.6, 9.9, 13.8, 16.5, 33kN

The constitutive equations were all based on that for DP800 (Table 4), with the strength factor scaled to obtain the various ultimate tensile strengths. Thus, for “DP200” for example, all of the stress values at various plastic strains in the constitutive model were divided by 4. The results, shown in Figure 9, show that to a very close approximation only the normalized side force is important, not the side force and material strength considered independently. Precise invariance was not observed because Young’s modulus was maintained constant, but the variation of buckling strains was smaller than experimental scatter, 0.016.

3. Demonstration tests

In order to demonstrate the usefulness of the new device, a few preliminary experiments were performed. The data obtained is usually the first of its kind because of the large-strain, continuous, reversing deformation at elevated temperature.

3.1. Mg Alloy AZ31B

The initial motivation for developing an elevated temperature, tension-compression test was to allow construction of a constitutive model as well as to discern the critical temperature for the suppression of twinning of Mg AZ31B. The target temperatures and time-to-temperature were set by this application. In this connection, continuous tension-compression-tension (T-C-T) (Figure 10a) and compression-tension-compression (C-T-C)

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4 To test the precise application of the invariance principle, a series of buckling simulations was conducted with scaled Young’s modulus consistent with the UTS. The results were identical, as expected from the invariance principle.
tests (Figure 10b) of Mg AZ31B (t=2mm) were carried out at a series of temperatures from room temperature to 250°C. A nominal strain rate of 10^-3/s was used and a side force of 2.5kN (0.4% of UTS of tensile test at room temperature) was applied. Corrections for biaxial loading and friction used procedures presented elsewhere (Boger et al. 2005). A friction coefficient of 0.03 was determined by a series of tensile tests with various side forces at room temperature. Data such as shown in Figure 10 is sufficient to develop an enriched 1-D constitutive model in corresponding temperature and strain reversals.

The form of the results in Figures 10 suggests a method to reveal, from purely mechanical observation, the critical temperature at which the deformation mechanisms change from twinning and slip (low temperature) to slip only (high temperature). For test temperatures up to and including 125°C, the compression (and tension following compression) curves show the concave up or inflected appearance characteristic of the activation of deformation twinning (Li 2006; Lou et al. 2007). For test temperature at 150°C and higher, all of the hardening curves have the “normal” concave-down aspect observed for cubic and non-twinning metals. Thus, the reverse testing suggests that the deformation mechanism transition temperature lies between 125°C and 150°C.

In order to verify that the critical twinning temperature obtained from the purely mechanical measurements is consistent with the disappearance of twinning, the microstructures of samples testing at various temperatures were examined. Samples before deformation and after C-T-C tests (Δε = -0.08, +0.06, -0.02) at 125°C and 150°C
were cut from the gauge regions, and were mounted, ground and polished to 1 mm diamond paste in ethanol. Acetic picral solution (4.2 g picric acid, 10 ml acetic acid, 70 ml ethanol and 10 ml water) was used to etch for 5 seconds. The microstructures obtained are presented in Figure 11. The initial microstructure of Mg AZ31B alloy sample, Figure 11a, is free of twins. The sample from the C-T-C test at 125°C shows an area twin fraction of 43%; while the corresponding sample deformed at 150°C shows minimal residual area twin fraction of 5%. The metallography confirms the mechanical identification of the deformation mechanism transition temperature as between 125°C and 150°C is consistent with the microstructure.

3.2. **TWIP Steel**

Recent results in the literature for advanced high strength steels emphasize significant impact of aspects of their constitutive response that are usually neglected for sheet forming applications. Modest temperature increases, for example, from deformation heating on the constitutive behavior, (Sung et al. 2009; Sung et al. 2010) affect plastic localization and failure markedly (Kim et al. 2009; Wagoner et al. 2009; Kim et al. 2009a; Kim et al. 2009b; Kim et al. 2011). Similarly, the strain hardening following stress reversals is significant in the prediction of springback following sheet forming operations (Geng and Wagoner 2002; Sun et al. 2009; Sun and Wagoner 2011).

In order to preliminarily estimate the magnitude of such effects, separately and together, compression-tension tests of TWIP steel were conducted at room temperature, 150°C and
250°C, Figure 12. TWIP (twinning induced plasticity) steel, exhibits high work-hardening rate, ductility, and strength, because of a twinning deformation mechanism (Grassel et al. 2000; Cornette et al. 2005; Bouaziz et al. 2008; Idrissi et al. 2010). Cyclic tests show a very significant Bauschinger effect that shows large deviations from isotropic hardening for multiple cycles. While the monotonic tensile tests show significantly lower strain hardening and reduced stresses at higher temperatures, the cyclic stress-strain behavior is much less dependent on temperature. The monotonic curves for this twinning steel do not exhibit the inflected hardening behavior associated with twinning of Mg, although the large tensile deformation following the TCTC cycles does show minor inflection.

3.3. Dual Phase Steels

Three dual-phase steels (with basic mechanical properties as summarized in Table 4) were subjected to compression-tension tests at room temperature and 150°C, Figure 13. All of the dual-phase steels have remarkably large Bauschinger effects, with the higher-strength grades (i.e. those with higher fractions of martensite) having larger effects. For example, at the first reversal of DP600 in the T-C-T test at a strain of ~0.04, the ratio $\beta_1$ between the reverse yield stress (0.2% offset) to the before-reversed flow stress is 0.72 at room temperature and 0.49 at 150°C. The second reverse in each test shows even larger Bauschinger effect, i.e., $\beta_1 > \beta_2$. Quantity results are summarized in Table 5.

In all cases, the transient strain hardening following a reversal is significantly different from the monotonic curve, and the difference persists to all subsequent strains. The
DP600 steel shows a minor inflection in the subsequent stress-strain response following the reversal.

3.4. Isothermal testing

Materials such as advanced high strength steels, which can absorb large amounts of plastic work, are difficult to perform tensile test isothermally at normal strain rates in air. It was expected that having the thermally conductive aluminum side plates in intimate contact with a tensile specimen would improve the ability to test isothermally. For example, tensile tests of DP590 steel (Sung et al. 2010) conducted using the device described in this paper showed temperature decreases to 1/10 as large as ones conducted in furnace (less than 1°C vs. 10°C). Those tests were conducted at 100°C and at a nominal strain rate of 10^-3/s.

A more rigorous set of tests was conducted using DP980 steel in rolling direction, deformed in air at ambient temperature and using the fixture described here at strain rates of 10^-2/s and 10^-1/s with a thermocouple welded onto the narrow edge of the specimen at its center. The deformation-induced temperature increases in air, 25°C and 38°C, respectively, were reduced to 1°C and 4°C, respectively, by using side plates with a clamping force of 1kN. Strain rate of 10^-1/s was found to be the limit for laser extensometer due to its highest working frequency of 100Hz.

4. Conclusions

The following conclusions were reached:
(1) The continuous, cyclic, large strain testing of sheet metals and alloys can be conducted conveniently at elevated temperatures. Temperature of 350°C can be achieved in 15 minutes, and the temperature difference along a 36mm gage length is less than 8 degrees.

(2) The same device and procedures used for tension/compression cycles can also enhance the isothermality of standard tensile tests at higher strain rates.

(3) A finite element method to predict buckling strains was shown to be accurate to within 0.015 for nominally flat sheet with a variety of materials, strengths, and thicknesses.

(4) The principal parameters affecting buckling are sheet thickness, side-force-to-material-strength ratio, and initial sheet flatness.

(5) Continuous tension/compression tests of Mg AZ31B alloy sheet show inflected curves, which indicate the existence of twinning, for tests conducted up to 125°C. Slip dominates the hardening behaviors above 150°C, as confirmed by optical metallography.

(6) The cyclic hardening behavior of TWIP, DP600, DP780, and DP980 steel sheets was measured for the first time from ambient temperature up to 250°C. Usually, large Bauschinger effects were apparent.
Acknowledgements

Initial stages of this work were sponsored by the Austem Co., Ltd. Later stages were supported by the National Research Foundation of Korea (Grant NRF-2010-220-D00037). Many thanks to Dr. Shihoon Choi (Sunchon National University, Korea), Dr. Daniel E. Green (University of Windsor, Canada) and Dr. Lou Hector, Jr. (GM R&D Center) for providing samples. Computer time was provided by the Ohio Supercomputer Center (OSC PAS080).
Appendix:

1. Measurement of thermal conductivity k

Figure A1-1 shows the setup for measuring the thermal conductivity of Al 7075 and Mg AZ31B. Metal strip is heated up to 100°C (T₁) by two heat plates holding at the top end, while the other end is put into severely stirred water to maintain uniform temperature near 25°C (T_w). Heat is conducted from top to bottom with fiberglass covering in the middle. The temperature distribution along the metal strip is measured by equidistant thermocouples with 50mm interval. T₁ is fixed at 100°C by temperature controller; and T₂-T₅ and T_w are detected and recorded on computer. Steady state, at which the temperature distribution along Al 7075 is plotted in Figure A1-2, is assumed to be reached when the temperature increment is within 0.1°C/minute. The concave curve is due to the lost energy to surrounding insulations. To be accurate, thermal conductivity k of metal strip can be calculated from the slope of dT/dx at the interface of water by the equation as follow:

\[-kA \frac{dT}{dx} = C_p m_{water} \frac{dT}{dt}\]

where A is the area of cross section of metal strip, dT/dx is the slope of fitted curve at T_w, C_p is 4200 J/kg·K, the heat capacity of water. m_{water} is mass. The calculated thermal conductivity k of Al 7075 is 120 W/m·K, close to the handbook value of 130 W/m·K. In the same strategy, the thermal conductivity of Mg AZ31B is measured as 100 W/m·K, comparable with the book value of 96 W/m·K.
2. Measurement of heat transfer coefficient $h_1$ and $h_2$

Figure A2-1 shows the schematic graph of the principles to measure heat transfer coefficient between two different materials. In steady state, by measuring the temperature difference across B and the temperature difference on the interface between A and B, heat transfer coefficient $h$ can be calculated by the following equations:

$$ q = h_1 A (T_A - T_1) = \frac{k_B A}{\Delta x} (T_1 - T_2) = h_2 A (T_2 - T_C), $$

where $q$ is the heat flow; $A$ is the contact area between two adjacent bodies; and $k_B$ is the thermal conductivity of body B. Constant side force of 2.5kN was applied upon metal blocks and wood block during testing. Steady state was assumed to be achieved when the temperature increment is within $0.1^\circ$C/minute. Heat transfer coefficient between magnesium specimen and aluminum heat plates with Teflon sheet between them was measured as $400 \text{Wm}^{-2} \text{K}^{-1}$. And the heat transfer coefficient between wood and Al7075 heat plate was determined as $100 \text{Wm}^{-2} \text{K}^{-1}$.

3. Fitting Process for heat transfer coefficient $h_3$

Heat transfer coefficient between wedges and sample was fit in FEA thermal analysis by comparing with temperature measurement as described in section 2.2. Heat transfer coefficients of 500, 1000, 2000, 5000 and 10000W/m$^2$K$^{-1}$ were tried with all other parameters fixed (Table 1). Figure A3-1 showing the temperature profile along the centerline of sample in both simulation (lines) and experiment (dots) indicates that $1000 \text{Wm}^{-2} \text{K}^{-1}$ fit the measurement best.
References


Green, D. 251 Essex Hall, 401 Sunset Avenue, Windsor, Ontario Canada.


Omega Engineering, I. One Omega Drive, P.O. Box 4047, Stamford, Connecticut. USA.


Staco-Energy™. 301 Gaddis Blvd., Dayton, OH 45403 USA.


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Table 3: Simulated maximum stresses of sample and fixture.
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Figure 3: Comparison of measured and simulated temperature profiles along the centerline of specimen during heating.

Figure 4: Model for ABAQUS/Standard mechanical analysis.

Figure 5: Model for ABAQUS/Standard buckling analysis.

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Figure 10: Cyclic testing of Mg AZ31B alloy sheets: (a) Tension-compression-tension test cycle; (b) Compression-tension-compression test cycle.

Figure 11: Optical micrographs of Mg AZ31B sample before and after deformation: (a) No deformation; (b) Deformed: C(-8\%)-T(6\%)-C(-2\%) at 125\degree C; (c) Deformed: C(-8\%)-T(6\%)-C(-2) at 150\degree C.

Figure 12: Cyclic test results for TWIP steel sheets from room temperature to 250\degree C: (a) T-C-T-C-T; (b) C-T-C-T-C

Figure 13: T-C-T test results for (a) DP600, (b) DP780, and (c) DP980 steels at room temperature and 150\degree C.

Figure A1-1: Setup for measuring thermal conductivities of metal sheets.
Figure A1-2: Temperature distribution along Al 7075 sheet.

Figure A2-1: Schematic plot showing the method for measuring heat transfer coefficients.

Figure A3-1: Temperature profile on the centerline of sample after 10 minute heating using around 200W input power: measurement (dots) vs. simulation (lines). Different heat transfer coefficients between wedge and sample were tried in FEA model.
<table>
<thead>
<tr>
<th>Material</th>
<th>Mg AZ31B&lt;sup&gt;(1)&lt;/sup&gt;</th>
<th>Al7075-T6&lt;sup&gt;(2)&lt;/sup&gt;</th>
<th>D2 Tool Steel&lt;sup&gt;(3)&lt;/sup&gt;</th>
<th>A2 Tool Steel&lt;sup&gt;(3)&lt;/sup&gt; (wedge grip)</th>
<th>Wood (Oak)&lt;sup&gt;(4)&lt;/sup&gt;</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density ρ&lt;sub&gt;(kg · m&lt;sup&gt;-3&lt;/sup&gt;)&lt;/sub&gt;</td>
<td>1.77 × 10&lt;sup&gt;3&lt;/sup&gt;</td>
<td>2.81 × 10&lt;sup&gt;3&lt;/sup&gt;</td>
<td>7.70 × 10&lt;sup&gt;3&lt;/sup&gt;</td>
<td>7.72 × 10&lt;sup&gt;3&lt;/sup&gt;</td>
<td>0.68 × 10&lt;sup&gt;3&lt;/sup&gt;</td>
</tr>
<tr>
<td>Young’s Modulus E&lt;sub&gt;(GPa)&lt;/sub&gt;</td>
<td>45</td>
<td>72</td>
<td>200</td>
<td>203&lt;sup&gt;(5)&lt;/sup&gt;</td>
<td>2</td>
</tr>
<tr>
<td>Possion’s Ratio ν</td>
<td>0.35</td>
<td>0.33</td>
<td>0.28</td>
<td>est 0.3</td>
<td>0.06</td>
</tr>
<tr>
<td>Yield Strength σ&lt;sub&gt;y&lt;/sub&gt;(MPa)</td>
<td>200</td>
<td>503</td>
<td>590</td>
<td>~1400&lt;sup&gt;(5)&lt;/sup&gt;</td>
<td>50</td>
</tr>
<tr>
<td>(Compressive YS, parallel to grain)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Thermal Conductivity k&lt;sub&gt;(W · m&lt;sup&gt;-1&lt;/sup&gt; · K&lt;sup&gt;-1&lt;/sup&gt;)&lt;/sub&gt;</td>
<td>100</td>
<td>120</td>
<td>20</td>
<td>26</td>
<td>0.17</td>
</tr>
<tr>
<td>Specific Heat Capacity C&lt;sub&gt;p&lt;/sub&gt;(J · kg&lt;sup&gt;-1&lt;/sup&gt; · K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>1000</td>
<td>960</td>
<td>461</td>
<td>460</td>
<td>1000</td>
</tr>
</tbody>
</table>

1. (Roberts 1960)  
2. (ASM 1991)  
3. (Roberts et al. 1998)  
4. (Laboratory 1999)  

Table 1: Piao et al.
| Heat Plate | Steady State $T_{\text{max}}$ ($^\circ$C) | Transient Process |  |
|---|---|---|---|---|
|  | Time to 200$^\circ$C | $\Delta T$ ($^\circ$C) (200$^\circ$C) | Time to 300$^\circ$C | $\Delta T$ ($^\circ$C) (300$^\circ$C) |
| #1 Al 7075-T6 (± 7.5, 22.5, 37.5 mm) | 310 | 310 s | 4.5 | 1120 s | 6.5 |
| #2 D2 Tool Steel (± 7.5, 22.5, 37.5 mm) | 335 | 360 s | 9.0 | 960 s | 13.0 |
| #3 Al 7075-T6 + D2 Tool Steel Cover (± 7.5, 22.5, 37.5 mm) | 295 | 482 s | 5.0 |  |
| #4-a Al 7075-T6 + Wood Insulation + D2 Tool Steel Cover (± 7.5, 22.5, 37.5 mm) | 356 | 305 s | 4.5 | 755 s | 6.5 |
| #4-b Al 7075-T6 + Wood Insulation + D2 Tool Steel Cover (± 15, 30, 45 mm) | 352 | 315 s | 4.0 | 780 s | 6.2 |
| #4-c Al 7075-T6 + Wood Insulation + D2 Tool Steel Cover (± 10, 16, 22 mm) | 360 | 295 s | 4.8 | 725 s | 7.0 |
| #5 D2 Tool Steel + Wood Insulation + D2 Tool Steel Cover (± 7.5, 22.5, 37.5 mm) | 386 | 356 s | 9.0 | 795 s | 13.0 |

Table 2: Piao et al.
<table>
<thead>
<tr>
<th>Plate Assembly</th>
<th>In Mg Sample</th>
<th>In Heat Plate</th>
<th>In Oak Insulation Plate</th>
<th>In Backing Plate</th>
</tr>
</thead>
<tbody>
<tr>
<td>#1 Al 7075-T6 HP</td>
<td>10.4</td>
<td>218</td>
<td></td>
<td>218</td>
</tr>
<tr>
<td>#2 D2 Tool Steel HP</td>
<td>8.9</td>
<td>218</td>
<td></td>
<td>218</td>
</tr>
<tr>
<td>#3 Al 7075-T6 HP + D2 Tool Steel Cover</td>
<td>6.2</td>
<td>16.3</td>
<td></td>
<td>218</td>
</tr>
<tr>
<td>#4 Al 7075-T6 HP + Wood Insulation + D2 Tool Steel Cover</td>
<td>5.8</td>
<td>13.7</td>
<td>33</td>
<td>218</td>
</tr>
</tbody>
</table>

Table 3: Piao et al.
<table>
<thead>
<tr>
<th>Material</th>
<th>Thickness (mm)</th>
<th>Elastic Modulus (GPa)</th>
<th>0.2% YS (MPa)</th>
<th>UTS (MPa)</th>
<th>Constitutive Equation $^5$</th>
<th>$&lt;\sigma&gt;$ (MPa)</th>
<th>$\varepsilon_b^{\text{Expt}}$</th>
<th>$\varepsilon_b^{\text{FE}}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>TWIP steel$^a$</td>
<td>1.46</td>
<td>196</td>
<td>454</td>
<td>1570</td>
<td>$\sigma = 2995\varepsilon_p + 449$</td>
<td>3</td>
<td>-0.13</td>
<td>-0.16</td>
</tr>
<tr>
<td>Trip780$^b$</td>
<td>1.50</td>
<td>211</td>
<td>507</td>
<td>866</td>
<td>$\sigma = 1612\varepsilon_p^{0.15}$</td>
<td>8</td>
<td>-0.09</td>
<td>-0.12</td>
</tr>
<tr>
<td>DP980$^a$</td>
<td>1.40</td>
<td>211</td>
<td>552</td>
<td>990</td>
<td>$\sigma = 1432\varepsilon_p^{0.11}$</td>
<td>5</td>
<td>-0.10</td>
<td>-0.09</td>
</tr>
<tr>
<td>DP800$^a$</td>
<td>1.40</td>
<td>211</td>
<td>422</td>
<td>800</td>
<td>$\sigma = 1300\varepsilon_p^{0.16}$</td>
<td>2</td>
<td>-0.12</td>
<td>-0.12</td>
</tr>
<tr>
<td>DP780$^a$</td>
<td>1.90</td>
<td>211</td>
<td>714</td>
<td>844</td>
<td>$\sigma = 1142\varepsilon_p^{0.08}$</td>
<td>4</td>
<td>-0.12</td>
<td>-0.14</td>
</tr>
<tr>
<td>DP590$^a$</td>
<td>1.40</td>
<td>211</td>
<td>369</td>
<td>613</td>
<td>$\sigma = 1008\varepsilon_p^{0.17}$</td>
<td>3</td>
<td>-0.14</td>
<td>-0.15</td>
</tr>
<tr>
<td>AA6022-TD$^b$</td>
<td>0.93</td>
<td>69</td>
<td>135</td>
<td>250</td>
<td>$\sigma = 341 - 206 \exp(-8.70\varepsilon_p)$</td>
<td>1</td>
<td>-0.10</td>
<td>-0.20</td>
</tr>
<tr>
<td>HSLA$^b$</td>
<td>0.80</td>
<td>205</td>
<td>400</td>
<td>455</td>
<td>$\sigma = 90\ln \varepsilon_p + 699$</td>
<td>1</td>
<td>-0.11</td>
<td>-0.12</td>
</tr>
</tbody>
</table>

$<\sigma>$: Standard error of fit (MPa)

$\varepsilon_b^{\text{Expt}}$: Measured buckling strain

$\varepsilon_b^{\text{FE}}$: Simulated buckling strain

$^5$ Constitutive equations were fit to compressive tests in a strain range from 0.01 to $\varepsilon_b$, at a strain rate of $10^{-3}$/s

a. Material properties from tensile tests were provided by GMNA Materials Lab GMNA (2007). GMNA Materials Lab. 660 South Blvd., Pontiac, MI, USA.

b. Material properties from tensile tests were provided by Green, D. 251 Essex Hall, 401 Sunset Avenue, Windsor, Ontario Canada.
<table>
<thead>
<tr>
<th>Material</th>
<th>25°C</th>
<th>150°C</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$\beta_1$</td>
<td>$\beta_2$</td>
</tr>
<tr>
<td>DP600</td>
<td>0.72</td>
<td>0.59</td>
</tr>
<tr>
<td>DP800</td>
<td>0.33</td>
<td>0.27</td>
</tr>
<tr>
<td>DP980</td>
<td>0.30</td>
<td>0.23</td>
</tr>
</tbody>
</table>

Table 5: Piao et al.
Figure 1: Piao et al.
Figure 2(a): Piao et al.
Sample (Mg AZ31B)
\( k = 100 \text{W/mK} \) [Appendix 1]
\( C_p = 1000 \text{J/kgK} \) [Roberts, 1960]

Heat Plate (Al7075-T6)
\( k = 120 \text{W/mK} \) [Appendix 1]
\( C_p = 960 \text{J/kgK} \) [ASM, 1991]

Insulating Plate (Oak)
\( k = 0.17 \text{W/mK} \)
\( C_p = 1000 \text{J/kgK} \)
[Laboratory, 1999]

Backing Plate (D2 tool steel)
\( k = 20 \text{W/mK} \)
\( C_p = 461 \text{J/kgK} \)
[Roberts et al., 1998]

Wedge (A2 tool steel)
\( k = 26 \text{W/mK} \)
\( C_p = 460 \text{J/kgK} \)
[Roberts et al., 1998]

22.5mm

\( k \): Thermal conductivity
\( C_p \): Specific heat capacity

Figure 2 (b): Piao et al.
Figure 3: Piao et al.
Figure 4: Piao et al.
Figure 5: Piao et al.
Figure 6: Piao et al.
Figure 7: Piao et al.
Figure 8(a): Piao et al.
Figure 8(b): Piao et al.
Figure 8(c): Piao et al.
Figure 9: Piao et al.
Figure 10(a): Piao et al.
Mg AZ31B, RD, CTC test
Thickness = 2.00 mm
Strain rate = 0.001/s, μ = 0.03
Side force = 2.5kN

Figure 10(b): Piao et al.
Figure 11(a): Piao et al.
Figure 11(b): Piao et al.
Figure 11(c): Piao et al.
TWIP steel, RD, TCTCT test
Thickness = 1.46mm
Strain rate = 0.001/s, $\mu = 0.03$
Side force = 3.3kN

Figure 12(a): Piao et al.
TWIP steel, RD, CTCTC test
Thickness = 1.46mm
Strain rate = 0.001/s, \( \mu = 0.03 \)
Side force = 3.3kN

Figure 12(b): Piao et al.
Figure 13(a): Piao et al.
Figure 13(b): Piao et al.
Figure 13(c): Piao et al.
Figure A1-1: Piao et al.
Measurement of Thermal Conductivity $k$
Steady State, Al 7075-T6

$k = 120 \text{ W/m-K}$

Figure A1-2: Piao et al.
Figure A2-1: Piao et al.
Measurement:
Target Temperature = 250°C
U = 105V, I = 1.85A

Simulation:
Surface Heat Flux
=0.048W/m²

\( h_3 = 500 \text{Wm}^{-2}\text{K}^{-1} \)

\( h_3 = 1000 \text{Wm}^{-2}\text{K}^{-1} \)

\( h_3 = 2000 \text{Wm}^{-2}\text{K}^{-1} \)

\( h_3 = 5000 \text{Wm}^{-2}\text{K}^{-1} \)

\( h_3 = 10000 \text{Wm}^{-2}\text{K}^{-1} \)

Figure A3-1: Piao et al.