Low-velocity impact on high-strength steel sheets: an experimental and numerical study

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Abstract

Low-velocity impact tests were performed on dual-phase and martensitic steel sheets and compared with corresponding quasi-static tests. The geometry and loading condition of the specimens were similar to formability tests, and the average strain rates before failure were in the range 80-210 s\(^{-1}\) for the low-velocity tests and 0.002-0.005 s\(^{-1}\) for the quasi-static tests. For both loading rates, the sheets failed under pre-dominant membrane loading, and by varying the specimen geometry, the stress states prior to failure ranged from uniaxial tension to equi-biaxial tension. Thus, the most important stress states occurring during an impact event in a thin-walled structure are covered. The experiments were complemented by nonlinear finite element simulations, where higher-order solid elements and a refined mesh were applied to capture the failure of the sheets. The materials were modelled using the Hershey high-exponent yield function combined with the associated flow rule and isotropic hardening. Quasi-static tensile and shear tests and tensile tests at elevated strain rates were performed to calibrate the constitutive relation. The results in terms of force-displacement curves and strain histories at critical positions in the specimens were similar for low-velocity and quasi-static loading, independent of material and specimen geometry. This indicates that the quasi-static test gives a good description of the sheet behaviour under low-velocity impact loading. The numerical simulations were found to be in good agreement with the experimental results, and strengthened the experimental finding that all the sheet-impact tests, except the martensitic steel sheet in a state close to equi-biaxial tension, displayed local necking before final fracture.

Keywords: Sheet-impact; advanced high-strength steel; necking; failure

1 Introduction

The low-velocity sheet-impact problem is of interest in many engineering applications, such as protection against dropped objects in the design of offshore structures [1], design against

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In most of the low-velocity sheet-impact studies reported in the literature, the parameters investigated have been the nose shape, the mass or the impact velocity of the impactor, the position of impact on the target, the boundary conditions of the sheet, or the sheet material, e.g. [5-8]. Common to these studies is that failure occurs locally at the nose of the impactor and the failure mode is typically plugging in the case of a blunt nose and petaling in the case of an ogival nose. Other failure modes, which may occur in sheets exposed to membrane stretching, are local necking and through-thickness shear fracture. However, these failure modes have mainly been studied in the quasi-static regime.

Alsos and Amdahl [9] studied the indentation resistance of stiffened and unstiffened panels exposed to quasi-static loading. Simulations with a local instability criterion and large shell elements [10] were able to give a reasonable prediction of failure. Simonsen and Lauridsen [11] presented experimental results on 1 mm steel sheets exposed to quasi-static loading by semi-spherical impactors with various diameters. The sheets were fixed with square, rectangular or circular boundaries, and failed by local necking followed by material fracture.

A detailed study on the failure modes in Nakajima formability tests on 1.5 mm thick steel sheets was conducted by Björklund and Nilsson [12]. Local necking occurred before fracture in all the tests, except for those experiencing stress states close to equi-biaxial tension. Hogström et al. [13] observed necking before fracture in formability tests on 4 mm thick mild-steel sheets for stress-states ranging from uniaxial tension to equi-biaxial tension. Usually formability tests on steel sheets exhibit failure by necking rather than fracture, an exception being equi-biaxial tension where the failure mode depends on the material. Stören and Rice [14] proposed a model to predict material instability based on the assumption that this phenomenon appears simultaneously as the initiation of a vertex on the yield surface. This model predicted well failure in several equi-biaxial tension tests reported in literature. An extension of this model introduced by Jie et al. [15], taking into account the strain-rate effect, improved considerably the prediction of failure in formability tests on steel sheets with strain rates around 0.4 s^{-1}.

In formability testing, the experimentalist has control of the stress state in the material before failure. However, this type of tests is usually restricted to quasi-static loading conditions. An exception is the study of Walters [16] who performed dynamic Hasek tests on DP780 steel
sheets in a drop tower. The tests were carried out to investigate the effect of stress state and strain-rate on the material’s ductility. It was found that the influence of strain-rate on the ductility depended on the stress state.

In this study, two experimental programmes were carried out for thin sheets made of either dual-phase steel or martensitic steel. The main experimental programme involved a novel set-up for low-velocity and quasi-static punch tests on sheet metals. The test set-up was designed to obtain sheet failure under membrane loading and to cover stress states within the range of traditional formability tests. This way the most dominant stress states occurring during an impact event are covered. The second experimental programme consisted of materials tests, namely quasi-static and dynamic tension tests and quasi-static shear tests. The sheet-impact tests were studied experimentally and numerically, using the results from the materials tests to calibrate constitutive models for the materials. It was found that the low-velocity and quasi-static sheet-impact tests exhibited similar response, which implies that the quasi-static tests give a good indication of the sheet behaviour during low-velocity impact. Further it was found that all tests failed by local necking, except for the martensitic steel sheet in a state close to equi-biaxial tension which failed by through-thickness shear fracture induced by shear-banding.

2 Materials

Two high-strength steel sheet materials, Docol 600DL and Docol 1400M, were provided from Swedish Steel AB (SSAB). The sheet thickness was 1.8 mm for Docol 600DL and 1.0 mm for Docol 1400M. Docol 600DL is a dual-phase steel with low yield strength and high work hardening. The nominal yield and ultimate stresses are reported from the manufacturer to be in the range from 280 MPa to 360 MPa and from 600 MPa to 700 MPa, respectively [17]. Through heat treatment the material is given a two-phase structure of ferrite and martensite. The ferrite gives good formability, while the martensite provides the strength. Docol 1400M is a cold-reduced and fully martensitic steel with high strength. The manufacturer reports a minimum yield strength of 1150 MPa and nominal ultimate strength between 1400 MPa and 1600 MPa [18]. The high strength is produced by very fast water quenching from an elevated austenitic temperature range. The chemical compositions of the materials are given in Table 1.
3 Material tests

To form the basis for constitutive modelling of the materials, a set of material tests was carried out. Uniaxial tension tests were used to provide true stress versus plastic strain curves up to necking and to investigate if the materials display significant plastic anisotropy. Shear tests were used to find the stress-strain behaviour for large strains and to determine the shape of the yield surface. The rate dependence of the materials was determined from split-Hopkinson tension bar tests.

3.1 Uniaxial tension tests

Uniaxial tension tests were carried out under displacement control in a Zwick/Roll hydraulic testing machine with a capacity of 30 kN. The nominal geometry of the specimens are given in Fig. 1(a). The loading rate was 4 mm/min giving a strain rate before necking of \( \dot{\varepsilon}_0 = 1.0 \times 10^{-3} \text{ s}^{-1} \). For both materials, specimens were cut out at 0°, 45° and 90° to the rolling direction in order to investigate in-plane anisotropy. Two successful tests in each loading direction are presented, thus giving a total of 12 tests for the two materials.

To acquire local strain data from the tests, digital image correlation analyses were performed. Before testing, one side of the specimen was spray-painted with a combination of black and white paint, obtaining a high-contrast speckle pattern to improve the optical measurements. The tests were recorded by a Prosilica GC2450 digital camera equipped with 50 mm Nikon lenses at a frequency of 2 Hz. The images were post-processed using an in-house digital image correlation (DIC) software [19], thus producing the displacement fields.

The force, \( F \), was measured by the load cell of the hydraulic actuator, while displacements were collected by a synchronized virtual extensometer with initial gauge length \( L_s = 60 \text{ mm} \) based on the DIC measurements, see Fig. 2(a). The engineering stress, \( s \), and the engineering strain, \( e \), were calculated as

\[
s = \frac{F}{A_s}, \quad e = \frac{L}{L_s} - 1
\]

where \( A_s \) is the measured initial area of the specimen and \( L_s \) is the extensometer gauge length. The true stress, \( \sigma \), true strain, \( \varepsilon \), and true plastic strain, \( \varepsilon^p \), before necking were calculated by standard relations as
\[ \sigma = s(1 + e), \quad \varepsilon = \ln(1 + e), \quad \varepsilon^p = \varepsilon - \frac{\sigma}{E_m} \]  

(2)

where \( E_m \) is the measured Young’s modulus in each test. Fig. 3(a-b) shows the engineering stress-strain curves for the whole loading history of the two materials.

To investigate the plastic anisotropy of the materials, the Lankford coefficients and the flow stress ratios were calculated for each test. At a given angle \( \alpha \) between the loading direction and the rolling direction, the Lankford coefficient is defined as

\[ R_\alpha = \frac{d\varepsilon^p_W}{d\varepsilon^p_T} = -\frac{d\varepsilon^p_W}{d\varepsilon^p_L + d\varepsilon^p_W} \]  

(3)

where \( d\varepsilon^p_W \), \( d\varepsilon^p_T \) and \( d\varepsilon^p_L \) are the incremental true plastic strains in respectively the width, thickness and longitudinal directions of the specimen. The last equality in Eq. (3) stems from the assumption of plastic incompressibility. Two virtual extensometers were applied to measure the strains in the longitudinal and width directions of the specimen. The gauge length of the extensometers was approximately 150 pixels, which corresponds to 7.2 mm. The true plastic strains \( \varepsilon^p_L \) and \( \varepsilon^p_W \) were then calculated using standard relations, and the average Lankford coefficients were determined in the range \( 0.018 \leq \varepsilon^p_L \leq 0.137 \) for 600DL and \( 0.0027 \leq \varepsilon^p_L \leq 0.012 \) for 1400M. The results are summed up in Table 2. The flow stress ratio for an angle \( \alpha \) at a specified amount of specific plastic work, \( W_p = \int \sigma_\alpha d\varepsilon^p_\alpha \), is defined as

\[ r_\alpha = \frac{\sigma_\alpha}{\sigma_0} \bigg|_{W_p} \]  

(4)

where \( \sigma_0 \) is the flow stress for \( \alpha = 0^\circ \). In this study, \( \sigma_0 \) is defined as the average value from the two parallel tests loaded in the rolling direction. The average flow stress ratios were calculated in the intervals \( 20 \text{ MPa} \leq W_p \leq 90 \text{ MPa} \) for 600DL and \( 5 \text{ MPa} \leq W_p \leq 25 \text{ MPa} \) for 1400M, and are compiled in Table 2. As can be seen from
Table 2, both the Lankford coefficients and the flow stress ratios are close to unity for both materials, indicating that the plastic anisotropy of these materials is negligible.

3.2 In-plane shear tests

The in-plane shear tests were carried out under displacement control in the same Zwick/Roll testing machine. The applied loading rate was 0.3 mm/min corresponding to an initial strain rate in the gauge area of 1.0·10^{-3} s^{-1}. The geometry of the shear specimen is given in Fig. 1(b). Two successful parallel tests for each material are presented. The specimens were cut out with the longitudinal axis in the rolling direction of the sheet. The force level was measured by the load cell of the hydraulic test machine and the displacement was measured by a virtual extensometer, see Fig. 2(b). The gauge length of the shear specimen is 5 mm. To account for differences in the gauge area of the duplicates, a normalized force $F/A_s$ was calculated, where $F$ is the measured force and $A_s$ is the measured initial area of the gauge section. The normalized force versus displacement curves are plotted in Fig. 3(c-d) for the two materials.

3.3 Split-Hopkinson tension bar tests

Tensile tests at strain rates in the range 200-600 s^{-1} were conducted in a split-Hopkinson tension bar set-up. A detailed description of the experimental set-up and data processing is given by Chen et al. [20]. By using the relations from one-dimensional wave theory, and assuming force equilibrium in the specimen, the engineering stress $s$ and the nominal engineering strain $\varepsilon$ in the specimen are calculated from the transmitted engineering strain $\varepsilon_t$ and the reflected engineering strain $\varepsilon_r$ in the bars as

$$s(t) = \frac{E_0 A_b}{A_s} \varepsilon_t(t), \quad \varepsilon(t) = -2 \frac{c_s}{L_s} \int_0^t \varepsilon_r(t) dt \quad (5)$$

where $E_0 = 204$ GPa and $A_b = 78.54$ mm² are the Young’s modulus and the cross-section area of the bar, $L_s = 5$ mm is the nominal gauge length, $A_s$ is the measured initial cross-section area of the specimen, and $c_s = 5100$ m/s is the speed of sound in the bars. Since some deformation takes place in the transition part of the specimen, the measured strain is overestimated. A correction was carried out following the method proposed by Albertini and Montagnani [21], and a corrected engineering strain was calculated as
where \( E_m \) is the measured Young’s modulus and \( E = 210 \) GPa is used as the correct Young’s modulus of the steel sheets. Initially it was planned to calculate the engineering strain from DIC measurements, but the paint came loose during testing and the measurements could not be used. Since the materials under investigation are delivered as thin sheets, the test specimens had to be glued to fixtures. Afterwards the fixtures were threaded and used to fasten the specimen to the steel bars, see [22]. Fig. 1(c) shows the geometry of the test specimen and the fixtures. For comparison, tensile tests at lower strain rates (\( \dot{\varepsilon}_0 = 1.0 \cdot 10^{-3} \) s\(^{-1}\) and \( \dot{\varepsilon}_0 = 1.0 \) s\(^{-1}\)) were conducted in the Zwick/Roll hydraulic testing machine using the same small-sized specimens. The force and displacement from the load cell in the testing machine were applied in calculating the nominal engineering stress-strain curve for each test. An exception is the 1400M tests with \( \dot{\varepsilon}_0 = 1.0 \cdot 10^{-3} \) s\(^{-1}\) where DIC measurements were used to calculate the engineering strain. A gauge length of \( L = 5 \) mm was applied to derive the engineering strain, and the nominal engineering strain from the load cell measurements was corrected for machine stiffness by use of Eq. (6).

The results in terms of engineering stress-strain curves for various strain rates are shown in Fig. 3(e-f). Clearly both materials display an increase in flow stress for increasing strain rate. The dynamic SHTB tests on the dual-phase steel do not display a large scatter in stress level, while one of the dynamic SHTB tests on the martensitic steel is significantly lower than the other tests and is considered to be an outlier. Both materials display oscillations for low strain levels. However, only the flow stresses at equivalent plastic strain equal to 10 % and 15 % for Docol 600DL and 1.5 % for Docol 1400M are to be further used from these tests. The flow stress as a function of plastic strain-rate at the aforementioned plastic strains is shown in Fig. 4(a-b) for the two sheet materials. For both materials, the flow stress displays a logarithmic increase with strain rate, and by increasing the strain rate from \( 10^{-3} \) s\(^{-1}\) to \( 10^{2} \) s\(^{-1}\) an approximate increase of 70 MPa is observed.
\[ \Delta T = \chi \frac{W_p}{\rho C_p} \]  

(7)

where \( \chi = 0.9 \) is the Taylor-Quinney coefficient, \( \rho = 7850 \text{ kg/m}^3 \) is the density of the steel specimens, \( C_p = 450 \text{ J/(kg·K)} \) is the specific heat of the steel specimens, and \( W_p \) is the specific plastic work as defined previously. At diffuse necking, \( W_p \) is approximately 120 MJ/m\(^3\) for the 600DL tests and 35 MJ/m\(^3\) for the 1400M tests. According to Eq. (7), this indicates an increase in temperature of approximately 30°C and 9°C at diffuse necking for the 600DL and 1400M tests, respectively. Consequently, the influence of temperature on the material behaviour before diffuse necking, and thus on the results in Fig. 4, can be neglected.

By comparing the engineering stress-strain curves from the quasi-static tensile tests on the small SHTB-type specimens (\( \dot{e}_0 = 1.0 \cdot 10^{-3} \text{ s}^{-1} \)) in Fig. 3(e-f) with those on the large specimens in Fig. 3(a-b), an apparent size effect is disclosed. At necking, the tests on the smaller SHTB-type specimens give 3-4% lower stresses than the tests on the larger specimens. It is emphasized that before necking both specimen types are in uniaxial tension with strain rates close to the nominal values, and the specimens were cut from neighbouring positions in the centre of the delivered steel sheets. The reason for this apparent size effect is not known, but it does not influence the results from the strain-rate tests in Fig. 4 as these are performed on the same type specimens.

4 Constitutive model

The elastic properties of the materials were described by a Young’s modulus of 210 GPa and a Poisson ratio of 0.3, while the material density was set to 7850 kg/m\(^3\). Due to the almost isotropic behaviour of both materials, the high-exponent Hershey yield function [23] with associated plastic flow and isotropic work hardening was found appropriate. The dynamic yield criterion is given as

\[ f = \sigma_{eq} - Y = 0 \]  

(8)

where \( \sigma_{eq} \) is the equivalent stress and \( Y \) is the flow stress. The equivalent stress is defined by

\[ \sigma_{eq} = \sqrt{\frac{1}{2} \left( (\sigma_I - \sigma_{II})^m + (\sigma_{II} - \sigma_{III})^m + (\sigma_I - \sigma_{III})^m \right)} \]  

(9)
where \( \sigma_i \geq \sigma_{ii} \geq \sigma_{iii} \) are the ordered principal stresses and \( m \) is an exponent controlling the shape of the yield surface. For \( m = 2 \) and \( m = 4 \), the von Mises yield surface is obtained, while \( m \to 0 \) and \( m \to \infty \) gives the Tresca yield surface. According to Logan and Hosford [24], \( m = 6 \) is a good approximation for BCC materials, and this was confirmed for both steel sheets during the calibration process. The flow stress is defined by [25]

\[
Y = \left( \sigma_0 + \sum_{i=1}^{3} Q_i \left( 1 - \exp(-C_i \rho) \right) \right) \left( 1 + \frac{\dot{\rho}}{\dot{\varepsilon}_0} \right)^c
\]

(10)

where \( \dot{\rho} \) is the equivalent plastic strain-rate, \( \rho = \int \dot{\rho} \, dt \) is the equivalent plastic strain, \( \sigma_0 \) is the yield stress, and \( Q_i \) and \( C_i \) \( (i = 1, 2, 3) \) are parameters governing the work hardening. The parameters \( c \) and \( \dot{\varepsilon}_0 \) define the strain-rate sensitivity of the material. The equivalent plastic strain-rate is defined by \( \dot{\rho} = \sigma : D^p / \sigma_{eq} \), where \( \sigma \) is the Cauchy stress tensor and \( D^p \) is the plastic rate-of-deformation tensor defined by the associated flow rule.

The test results from the split-Hopkinson bar tests were used to determine the parameters \( c \) and \( \dot{\varepsilon}_0 \), and the resulting curve fits are shown in Fig. 4(a) and (b). The other material parameters were found by use of inverse modelling of the tensile and shear tests.

To this end, the nonlinear finite element (FE) solver IMPETUS Afea [26] was used; the response parameters being the engineering stress-strain curves from the tensile tests and the normalized force-displacement curves from the shear tests. The specimens were discretized by fully integrated hexahedral elements with cubic shape functions, see Fig. 5. An in-plane element size of approximately 0.95 mm was found sufficient to give a good description of the diffuse and local necking phenomena in the tensile specimens. The shear specimens were not exposed to significant local necking, but the large deformations occurring over the relatively small gauge area required a resolution with an in-plane element size of 0.27 mm. To reduce the number of elements, a symmetry plane in the thickness direction was applied; this is illustrated in Fig. 5(b) for the shear specimen. Two elements were used over half the thickness for both the tensile and shear specimens. As IMPETUS Afea is an explicit FE code, mass scaling was applied to reduce the computational time. To evaluate the mass scaling effect, several simulations of the uniaxial tensile test for the 600DL material were run with decreasing mass scaling factor. Mass scaling by a factor \( 4.0 \times 10^8 \) was found to have only minor influence on the diffuse and local necking predictions. After all simulations, it was
carefully checked that the kinetic energy remained a small fraction of the internal energy of
the specimen. In the simulations of the tensile tests, prescribed velocities were applied to rigid
parts at an appropriate distance away from the gauge region, see Fig. 5(a). The prescribed
velocity was ramped up over the first 30 seconds using a smooth transition function. In the
simulations of the shear tests, the nodal displacements from a DIC analysis of one duplicate
were imposed directly to the finite element mesh, see Fig. 5(c). The nodal displacements were
applied to two parts that were discretized with trilinear elements and merged with the cubic
elements used in the central part of the specimen. It was assumed that the displacement was
homogenous through the thickness of the specimen. The results from the final simulations are
shown in Fig. 3(a-d). The constitutive material parameters for both materials are summed up
in
Table 3.

To validate the material model for high strain-rate conditions, simulations of the SHTB tests
were run. The specimens were discretized with cubic elements with an in-plane size of
0.25 mm and two elements over half the thickness in the necking region. A symmetry plane
was applied in the thickness direction. The boundary conditions were prescribed velocities
applied to rigid parts at an appropriate distance away from the gauge region, see Fig. 5(d). In
addition to the dynamic tests in the SHTB with nominal strain rate 200 s\(^{-1}\), the quasi-static
tests on the same type of specimens with nominal strain rate 0.001 s\(^{-1}\) were simulated.
Similar to the experimental data processing, the displacement at the boundaries (rigid parts),
were applied to calculate the nominal engineering strain, which was subsequently corrected
according to Eq.(6).

The simulated engineering stress-strain curves are shown in Fig. 3(e-f). For both materials,
the ultimate stress in the quasi-static simulations, Fig. 3(e-f), is similar to the ultimate stress in
the simulations of the large specimens, Fig. 3(a-b). This indicates that the size effect observed
in the experiments is not only related to the geometry of the specimens. Since the calibration
of the material model is based on the results from the larger uniaxial tension specimen, and
the apparent size effect is only present in the experiments, both the quasi-static and dynamic
simulations with the small specimen display higher engineering stress levels than their
respective experiments as shown in Fig. 3(e-f).

Considering the dynamic simulations, the post-necking part of the 1400M engineering stress-
strain curve displays the same slope as the dynamic experimental curves, see Fig. 3(f). For the
600DL material, the post-necking stress level in the simulation actually drops more rapidly than in the experiments, as can be seen in Fig. 3(e). Although a temperature increase up to as much as 150°C-200°C can be expected locally at the position of fracture initiation, accurate engineering stress-strain curves to failure were obtained numerically without introducing thermal softening in the constitutive model. However, it is noted that the influence of adiabatic heating may depend on the material as well as the stress state during plastic deformation. For instance, Roth and Mohr [27] presented un-notched and notched tensile tests made from 1.4 mm thick dual-phase steel sheets exposed to high strain-rate loading, and found that a temperature independent material model did not provide a good post-necking force-displacement response for all of the tests.

5 Punch tests on sheet metals

Low-velocity sheet impact tests were carried out on specimens made from the two materials. To perform the tests, a test rig was designed involving steel rings for clamping of the specimen and an arrangement for monitoring the deformation of the surface of the specimen by use of high-speed cameras, see Fig. 6(a-b). A mirror positioned inside a cylinder with cutouts, Fig. 6(a), was applied. Direct recording of the specimen was not practical since the cylinder was attached to the reaction wall. The mirror was not in direct contact with the cylinder during the impact tests in order to avoid disturbance. The specimens had three different geometries named S20, S100 and S150, see Fig. 6(c), where the number indicates the width in mm in the gauge region. The chosen geometries represent the whole range of stress states in traditional metal formability tests from uniaxial tension (S20) to equi-biaxial tension (S150). The S100 geometry is designed to be close to plane-strain tension, a stress state giving low ductility for many materials [28-30]. In addition to the low-velocity tests, a test series with quasi-static loading was completed to study the effect of the loading rate.

During testing, the specimen was clamped between two steel rings, as illustrated in Fig. 6(d). The clamping rings had an inner radius of 75 mm, Fig. 6(e), and the ring facing the cylinder had a rounded edge towards the specimen, see Fig. 6(f). The rounded edge had a smooth surface, while the faces on the rings that were in contact with the specimen had a rough surface to enhance the clamping, see Fig. 6(g). In order to fasten the rings, M16 bolts were used. For the S20 and S100 geometry, 10 bolts were applied, while 16 bolts were applied for the S150 geometry. The bolts were fastened with a torque wrench set to 200 Nm. The cylinder supporting the rings and the specimen was designed so that no plastic deformation would
occur during testing and so that the surface of the specimen could be monitored easily by the cameras. The cylinder was welded to a steel-plate that easily could be fixed to a rigid support. Details of the cylinder and the mounting steel-plate are shown in Fig. 6(h).

The loading was applied by a punch with a hemispherical nose having a radius of 50 mm, as illustrated in Fig. 6(a). The deformation of the specimen was recorded by two cameras via the mirror inside the cylinder that supported the rings and the specimen; see Fig. 6(a-b). The side of the specimen facing the punch was sprayed with a lubricant (Klüber Unimoly C220) to reduce friction, while the side facing the mirror was spray painted with a speckle pattern to enhance the optical measurements. The in-plane logarithmic principal strains and the strain magnitude on the surface of the specimens were determined from the DIC displacement fields. The strain magnitude at a given point is here defined as

$$
\varepsilon_s = \sqrt{\frac{2}{3}(\varepsilon_1^2 + \varepsilon_2^2 + \varepsilon_3^2)}
$$

where $\varepsilon_1 = \ln \lambda_1$ and $\varepsilon_2 = \ln \lambda_2$ are the in-plane logarithmic principal strains, $\lambda_1^2$ and $\lambda_2^2$ being the eigenvalues of the right Cauchy-Green deformation tensor, and $\varepsilon_3 = -\left(\varepsilon_1 + \varepsilon_2\right)$ is the logarithmic principal strain in the thickness direction of the sheet. Plastic incompressibility and negligible elastic strains are assumed. The discretization in the DIC analysis was restricted by the resolution of the high speed cameras and a nodal spacing of 3.5 mm were applied.

### 5.1 Low-velocity tests

The low-velocity impact tests were carried out in a pendulum impactor [31]. Fig. 7(a) shows the schematic set-up of the pendulum impactor. Fig. 7(b) shows how the specimen is positioned in front of the reaction wall. A trolley equipped with a load cell and the punch, see Fig. 7(c), which had a total mass of 417.5 kg, was accelerated to a velocity $v_0 = 10.5$ m/s before impact. Three duplicates were conducted of each test. The recordings in one of the 600DL-S20 and 600DL-S150 duplicates failed and are not reported. The time duration from initial contact to fracture was between 1.7 ms (for 1400M-S20) and 4.5 ms (for 600DL-S150). The load cell recorded the force $P(t)$ at 200000 Hz resulting in between 340 and 900 data-points for each test. The recording by the load cell was triggered when the front of the trolley passed a photocell positioned 260 mm ahead of the specimen. This photocell and another one
positioned 10 mm ahead of the specimen were used to measure the impact velocity of the trolley. The measured impact velocity was in the range $10.45 \text{ m/s} \leq v_0 \leq 10.58 \text{ m/s}$ in the various tests.

Under the assumption that the trolley, the load cell and the punch had identical acceleration equal to $\ddot{u}(t)$, the force between the punch and the specimen $F(t)$ were found from Newton’s second law as

$$F(t) = (M_T + M_p)\ddot{u}(t) \quad \Rightarrow \quad F(t) = \left(1 + \frac{M_p}{M_T}\right)P(t)$$

(12)

Here, $M_T = 385 \text{ kg}$ is the mass of the trolley and the back-part of the load cell, and $M_p = 32.5 \text{ kg}$ is the mass of the punch and the front part of the load cell, see Fig. 7(d).

Notably, Eq. (12) predicts that the force between the punch and the specimen is 8.4 % larger than the measured force in the load cell. The velocity $\dot{u}(t)$ and the displacement $u(t)$ were calculated from the force measurements as

$$\dot{u}(t) = v_0 - \int_0^t \frac{P(t)}{M_T} \, dt$$

$$u(t) = \int_0^t \dot{u}(t) \, dt$$

(13)

Since the initial kinetic energy is much larger than the dissipated work during impact, the displacement $u(t)$ is nearly a linear function of time.

The out-of-plane displacement of the specimen was also obtained from DIC analysis. Here two synchronized Phantom v1610 high speed cameras equipped with 105 mm lenses recorded the deformation of the specimen via the mirror at a framing rate of 21000 Hz, thus resulting in between 36 and 95 images for each test. The positions of the two cameras were approximately 0.5 m below the mirror, see Fig. 7(b). The images of one of the 1400M-S20 duplicates were of too low quality for DIC analysis. The displacements obtained from the load cell calculations, using Eq.(13), and the DIC analysis were in agreement. A Photron SA1.1 high-speed camera recorded the impact event from a position normal to the impact direction. The framing rate was 800 Hz, thus giving between 14 and 36 images in each test. The images from the Photron camera were used to check that the steel rings with the specimen mounted were
not exposed to rigid body motion during loading. No significant rigid body motions were observed during any of the tests. Both the Phantom cameras and the Photron camera were triggered by the same photocell used to trigger the load cell recordings, but the three recordings were not entirely synchronized as the time lag was slightly different for the three devices.

### 5.2 Quasi-static tests

The quasi-static tests were carried out in an Instron 1332 hydraulic testing machine with a capacity of 250 kN. The set-up was mounted vertically as shown in Fig. 8, with the punch moving downwards onto the specimen. The loading was under displacement control with a loading rate of 0.3 mm/s. The hydraulic testing machine recorded the crosshead displacement and the force in a load cell placed above the crosshead. In addition, two Prosilica GC2450 cameras equipped with 28-105 mm Nikon lenses were recording the deformation in the gauge area at a framing rate of 2 Hz for the 600DL material and 4 Hz for the 1400M material. The cameras were positioned approximately 1 m from the mirror, see Fig. 8, and the camera recordings were synchronized with the recording of the load cell. Two duplicates per geometry for each material were conducted, thus resulting in a total of 12 quasi-static tests. The images of one of the 600DL-S20 duplicates were of too low quality for DIC analysis.

### 5.3 Results

The results in terms of force-displacement curves for both the low-velocity and the quasi-static tests are given in Fig. 9. All tests display an initially low increase in force level since membrane stresses are not significantly present at low punch displacement, thus the initial reaction force stems from the bending stiffness of the specimen. Both the low-velocity and the quasi-static impact tests display good repeatability. For the 600DL material, the force level is generally higher in the low-velocity tests; this is attributed to the positive strain-rate dependence. For the 1400M material, the force levels in the low-velocity and the quasi-static impact tests are of the same magnitude. Oscillations can be seen in the low-velocity tests, these may stem from a combination of stress waves and dynamic effects as the eigenperiods of the clamped specimens are comparable to the loading time. At failure, a significant drop in the force curve is observed.

It was observed to a various degree in all experiments that the location with the largest deformation changed during testing. Initially most deformation occurred in the centre of the specimen, for then gradually to translate radially until necking or fracture occurred. This is
illustrated in Fig. 10 by the evolution of the strain magnitude measured by DIC on the surface of the quasi-static 600DL-S20 test. This observation implies that the point with the largest strain-rate is moving during loading. Further, DIC measurement of the strain magnitude in the last image before fracture $\varepsilon'_f$ was collected. Fig. 11 shows contour plots of $\varepsilon'_f$ for a selected duplicate of each test. With $\varepsilon'_f$ values in the range 0.41-0.67, the 600DL material shows a significantly more ductile behaviour than the 1400M material, which displays $\varepsilon'_f$ values in the range 0.14-0.41. A tendency, particularly observed in the S20 geometries, is that the low-velocity tests display equally large strain concentrations on both sides of the centre of the specimen, while in the quasi-static tests the strain concentration on one side is dominant. This tendency was also observed in the post-fracture specimens, selected duplicates are shown in Fig. 12. Here a distinct local necking (diffuse necking for 600DL-S20) is present in regions not exposed to fracture in some of the low-velocity specimens, marked with an ellipse, while a similar distinct necking is not observed in the quasi-static tests. The crack started to propagate in the rolling direction in the S150 tests, except in the low-velocity tests of the 1400M material. In the S100 and S20 tests, the rolling direction coincided with the direction of the major principal stress, and fracture occurred normal to the rolling direction, except in the 1400M-S20 tests where fracture followed the local neck which appeared approximately $64^\circ$ to the rolling direction.

In order to study the failure mode, the thickness in the fracture zone, $t_f$, and the thickness 1.5 mm from the fracture zone, $t_{1.5}$, were measured for each test. Failure is here defined as the incipient necking or onset of fracture, whichever comes first. The measurements were carried out on light microscopy images of selected tests, see Fig. 13. The positions of the measurements are illustrated for one case in Fig. 13. The values of $t_f$, $t_{1.5}$ and $t_f/t_{1.5}$ are summed up in Table 4. All of the selected tests of the dual-phase material display $t_f/t_{1.5}$ ratios around 0.6, suggesting that the tests fail due to necking. From Fig. 13 it can be seen that shear lips are present in the 600DL tests, indicating that shear banding occurs prior to fracture. For the martensitic material, the S20 geometry display $t_f/t_{1.5}$ ratios around 0.5, while the S100 and S150 geometries display $t_f/t_{1.5}$ ratios around 0.8. This indicates that the S20 geometry fails due to necking, while the S100 and S150 geometries fail due to through-thickness shear fracture. For both materials the low-velocity and quasi-static loading conditions give
comparable results. Further, the S20 and S100 tests display comparable values of $t_{1.5}$ for both steel sheets, while the S150 tests are exposed to more thinning.

The principal strain histories were collected from the DIC element closest to the spatial fracture-initiation point, and Fig. 14 shows $\varepsilon_1 - \varepsilon_2$ plots from the tests. The principal strain history and the principal strains from the last image before fracture are approximately the same in the low-velocity and quasi-static tests. It is found that the S20 tests are close to uniaxial tension ($\varepsilon_2 / \varepsilon_1 = -0.5$), the S100 tests are close to plane-strain tension ($\varepsilon_2 / \varepsilon_1 = 0$), and the S150 tests are close to equi-biaxial tension ($\varepsilon_2 / \varepsilon_1 = 1$). The strain paths in uniaxial tension (UT), plane-strain tension (PST) and equi-biaxial tension (EBT) are shown with black thick lines in Fig. 14.

6 Numerical simulations of sheet impact tests

6.1 Modelling of the sheet impact tests

A detailed numerical analysis of the impact tests was conducted using the explicit finite element solver IMPETUS Afea. Fully integrated hexahedral elements with cubic shape functions were used to discretize the test specimens, while the material behaviour was described by the constitutive model presented in Section 4. A denser mesh with an in-plane element size of 0.625 mm and two elements in the thickness direction was applied in the regions exposed to the largest deformations, see Fig. 15(a). To provide accurate boundary conditions, the steel rings clamping the specimen were modelled as rigid parts constrained in all translational degrees of freedom, and the nodes on the edges of the specimen were fixed in the in-plane translational degrees of freedom, as shown in Fig. 15(a). In the contact between the specimen and the steel rings, a Coulomb friction coefficient of 0.4 was applied.

To ensure similar mechanical impedance in the simulations and the experiments, detailed models of the impacting parts were applied, as shown in Fig. 15(b). The impacting parts were discretized with trilinear hexahedral elements, except for the punch nose that was discretized by tetrahedral elements with cubic shape functions to provide a smooth contact surface. A Coulomb friction coefficient of 0.1 was applied in the punch-specimen contact. According to [32], the friction coefficient for greasy steel-steel surfaces is in the range 0.029-0.12. Two simulations of the low-velocity 600DL-S20 test were run with a friction coefficient of 0.025
and 0.1. The simulation with friction coefficient equal to 0.1 gave a force-displacement curve in good agreement with the experiment and this value was used in all further simulations. The impacting parts were mainly made of steel with an assumed Young’s modulus of 210 GPa, a Poisson ratio of 0.3 and a density of 7850 kg/m$^3$. The trolley was partly made of aluminium, see Fig. 15(b), and the aluminium was assumed to have a Young’s modulus of 70 GPa, a Poisson ratio of 0.3 and a density of 2700 kg/m$^3$. The constitutive behaviour of the impacting parts was given by linear elastic material models with the above mentioned properties. The force and the displacement were derived from the force $P(t)$ acting in the centre of the load cell by applying Eq.(12) and Eq.(13).

In the quasi-static simulations, a node set covering the back of the punch was given a prescribed motion in the loading direction, smoothly ramped up to 0.3 mm/s over the first 6.6 s of the simulation. The contact force between the punch and the specimen and the displacement of the apex of the punch-nose were collected for the force-displacement curves. To reduce computational costs, mass scaling was introduced by restricting the maximum time step to $1.3 \cdot 10^{-3}$ s for the 600DL simulations and $5.85 \cdot 10^{-4}$ s for the 1400M simulations.

### 6.2 Results

The simulated force-displacement curves are shown for the low-velocity tests in Fig. 16 and for the quasi-static tests in Fig. 17. The simulations were run without any fracture criterion and thus the drop in force level is due to necking. The quasi-static and low-velocity simulations of the 600DL material display good agreement with the experimental force-displacement curves, which suggests that the constitutive model and the boundary conditions are appropriate. The simulated drop in force level occurs at approximately the same displacement as in the experiments, which supports the experimental finding that these tests are experiencing necking before fracture. The simulated force-displacement curves of the 1400M material are in close agreement with the experimental curves, but the drop in the force level occurs at a larger displacement than in the experiments, particularly for the S20 and S150 geometries.

As a more local measure of the onset of necking, the element exposed to the largest deformation in each simulation was identified and a local necking criterion was applied based on the strain-rate in this critical element and in two neighbouring elements located further away from the centre of the specimen in the radial direction. Fig. 18(a) illustrates the positions of the critical and the neighbouring elements just after necking in the quasi-static 600DL-S20
simulation. Three elements covering a strip of 1.9 mm and with a total of \( n_i = 192 \) integration points were found sufficient as the spatial region for the necking prediction in this analysis. The equivalent plastic strain-rate histories from the integration points, \( \dot{\varepsilon}_p(t), i = 1,2,...,n_i \), were collected and the mean strain-rate history, \( \dot{\varepsilon}_p(t) = \frac{1}{n_i} \sum_{i=1}^{n_i} \dot{\varepsilon}_p(t) \), and the maximum strain-rate deviation history, \( \dot{\varepsilon}_{dev}(t) = \max \dot{\varepsilon}_p(t) - \min \dot{\varepsilon}_p(t) \), were calculated. The normalized maximum strain-rate deviation history, \( E_{dev}(t) \), was then calculated as

\[
E_{dev}(t) = \frac{\dot{\varepsilon}_{dev}(t)}{\dot{\varepsilon}_{mean}(t)} \cdot 100\%
\]  

The time at onset of necking, \( t_{neck} \), is defined as \( E_{dev}(t_{neck}) = 20\% \). Fig. 18(b) illustrates the \( \dot{\varepsilon}_p(t) \) and \( \dot{\varepsilon}_{mean}(t) \) histories from the three elements in the quasi-static 600DL-S20 simulation together with \( \dot{\varepsilon}_{mean}(t_{neck}) \), while Fig. 18(c) display the \( E_{dev}(t) \) history and \( E_{dev}(t_{neck}) \) from the same analysis. This approach to identify local necking has similarities with the experimental approaches used in [13, 33]. The onset of necking is marked with a circle in Fig. 16 and Fig. 17. The simulations display a trend where onset of necking is either predicted close to the experimental displacement at fracture or at a lower displacement. An exception is the simulations of the 1400M-S150 tests where onset of necking is predicted at a significantly larger displacement. The experimental onset of fracture is marked with a square in the simulated force-displacement curves for the 1400M-S150 tests shown in Fig. 16 and Fig. 17. Generally the predicted onset of necking in the simulations supports the experimental findings. An exception is the 1400M-S100 tests, for which the experimental analysis was inconclusive: strain concentration was observed in the DIC measurements, but local necking was not observed from visual inspection (Fig. 13). The simulations indicate that necking occurs before through-thickness shear fracture in these tests.

The equivalent plastic strain in the last image before failure is shown in Fig. 19. The image is before incipient necking in all the simulations, except for the low-velocity and quasi-static 1400M-S150 simulations, where the last image before the experimental displacement at fracture in the tests (marked with a square in the force-displacement curves in Fig. 16 and Fig. 17) are shown. Generally, the contour plots from the quasi-static and low-velocity simulations are similar, although the low-velocity simulations display somewhat larger strains. The contour plots in Fig. 19 resemble the experimental contour plots in Fig. 11, but the tendency
of a dominant strain concentration on one side of the centre of the specimen seen in the quasi-
static experiments is not observed in the simulations.

In order to gain information on the deformation histories at critical positions, the first and
second in-plane principal strains, $\varepsilon_1$ and $\varepsilon_2$, and the equivalent plastic strain-rate, $\dot{p}$, were
collected from the critical element in each simulation. The positions of the critical elements
are indicated with arrows in Fig. 19. Note that the first in-plane principal strain occurs along
the $y$ axis and the second in-plane principal strain along the $x$ axis of the coordinate system
given in Fig. 19. The simulated $\varepsilon_1 - \varepsilon_2$ curves are shown in Fig. 14. Here the points
corresponding to the displacement at fracture in the 1400M-S150 tests (marked with a square
in Fig. 16 and Fig. 17) are marked with squares in the 1400M-S150 simulations. Incipient
necking, as predicted by the applied necking criterion, is marked with a circle in each of the
other simulations. For the S20 and S100 geometries, good agreement between the
experimental and numerical strain paths is observed. For the S150 geometries, the numerical
strain paths for both materials are closer to equi-biaxial tension than the experimental strain
paths. Notably, the numerical strain paths are independent of the loading rate, while the low-
velocity experimental strain paths are closer to plane-strain tension than the quasi-static strain
paths. In general, the numerical models capture not only the global force-displacement
behaviour of the samples, but also the general trends of the local deformation histories at the
critical locations.

The strain-rate histories from a selected integration point in the critical element for each
simulation are shown in Fig. 20. Here a moving average filter was applied on the strain rate in
the 1400M simulations. Similar to Fig. 14, onset of necking is marked with circles, while
onset of fracture in the 1400M-S150 simulations are marked with squares. Since the point
with maximum strain-rate is moving during deformation, as illustrated in Fig. 10, the critical
elements experience a gradual increase in strain-rate up to necking. The average strain-rate up
to failure, $\dot{p}_{avg} = \Delta t^{-1} \int_0^N \dot{p}(t) dt$, where $\Delta t$ is the time from incipient plastic strain to failure,
was calculated and the results are summed up in Table 5. The low-velocity impact tests have
an initial punch velocity 35000 times the punch-velocity in the quasi-static tests. As can be
seen from Fig. 20 and Table 5, the same scale factor applies reasonably well for the strain-rate
in the critical elements in the low-velocity and quasi-static simulations.
7 Concluding remarks

Low-velocity impact tests were conducted on sheets from the dual-phase steel Docol 600DL and the fully martensitic steel Docol 1400M. In addition, corresponding quasi-static tests were carried out as a reference. For both loading rates, the sheets failed under membrane loading and experienced stress states from uniaxial tension to equi-biaxial tension, thus covering important stress states that may occur during a generic impact situation. Generally the results in terms of force-displacement curves and strain histories at critical positions in the specimens were similar for low-velocity and quasi-static loading, independent of material and specimen geometry. This suggests that the quasi-static tests may give a good indication of the membrane failure of both materials in a generic low-velocity load case. All tests failed due to necking except for the martensitic steel sheet exposed to a nearly equi-biaxial loading. In this case, through-thickness shear fracture occurred without significant necking.

The finite element simulations were in good agreement with the experiments and supported the experimental finding that all tests apart from the martensitic sheet in a state close to equi-biaxial tension failed due to necking. Further, the in-plane principal strains and the equivalent plastic strain-rate were extracted from critical locations of the specimen in the finite element simulations. The evolution of the in-plane principal strains obtained numerically captured the general trends in the experimental results. The strain rates at critical locations in the tests were estimated in the range of $80-210 \text{ s}^{-1}$ before failure for low-velocity loading and $0.002-0.005 \text{ s}^{-1}$ before failure for quasi-static loading. Although adiabatic conditions prevail in the low-velocity tests, a material model which did not incorporate thermal effects provided good results in this study.

Acknowledgements

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References


Tables and figures

Table 1 Chemical compositions of the materials (in weight %) [17, 18].

<table>
<thead>
<tr>
<th>Material</th>
<th>C</th>
<th>Si</th>
<th>Mn</th>
<th>P</th>
<th>S</th>
<th>Al tot</th>
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<tbody>
<tr>
<td>600DL</td>
<td>0.10</td>
<td>0.40</td>
<td>1.50</td>
<td>0.010</td>
<td>0.002</td>
<td>0.040</td>
</tr>
<tr>
<td>1400M</td>
<td>0.17</td>
<td>0.20</td>
<td>1.40</td>
<td>0.010</td>
<td>0.002</td>
<td>0.040</td>
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Table 2 Lankford coefficients $R_\alpha$ and flow stress ratios $r_\alpha$ in uniaxial tension for two parallel tests in each direction ($\alpha = 0^\circ, 45^\circ, 90^\circ$).

<table>
<thead>
<tr>
<th>Material</th>
<th>$R_0^\circ$</th>
<th>$R_{45^\circ}$</th>
<th>$R_{90^\circ}$</th>
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<th>$r_{45^\circ}$</th>
<th>$r_{90^\circ}$</th>
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<tbody>
<tr>
<td>600DL</td>
<td>1.06 / 1.05</td>
<td>0.94 / 0.92</td>
<td>1.22 / 1.17</td>
<td>1.00/1.00</td>
<td>0.99/0.99</td>
<td>0.99/0.99</td>
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<tr>
<td>1400M</td>
<td>1.01 / 1.02</td>
<td>1.30 / 1.21</td>
<td>1.08 / 1.15</td>
<td>1.00/1.00</td>
<td>0.97/0.97</td>
<td>1.00/1.00</td>
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Table 3 Constitutive model parameters for the two materials.

<table>
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<tr>
<th>Material</th>
<th>$\sigma_0$ [MPa]</th>
<th>$Q_1$ [MPa]</th>
<th>$C_1$</th>
<th>$Q_2$ [MPa]</th>
<th>$C_2$</th>
<th>$Q_3$ [MPa]</th>
<th>$C_3$</th>
<th>$\dot{\varepsilon}_0$ [s$^{-1}$]</th>
<th>$c$</th>
<th>$m$</th>
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<tbody>
<tr>
<td>600DL</td>
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<td>201.2</td>
<td>38.42</td>
<td>347.5</td>
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<td>6</td>
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<tr>
<td>1400M</td>
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<td>253.6</td>
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<td>97</td>
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<td>6</td>
<td>$1 \cdot 10^{-3}$</td>
<td>0.004</td>
<td>6</td>
</tr>
</tbody>
</table>

Table 4 Thickness in fracture zone, $t_f$, thickness 1.5 mm from fracture zone, $t_{1.5}$, and $t_f/t_{1.5}$ ratio.

<table>
<thead>
<tr>
<th>Load</th>
<th>Geometry</th>
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<th>1400M</th>
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</thead>
<tbody>
<tr>
<td></td>
<td>$t_f$ [mm]</td>
<td>$t_{1.5}$ [mm]</td>
<td>$t_f/t_{1.5}$</td>
</tr>
<tr>
<td>Low-velocity</td>
<td>S20</td>
<td>0.72</td>
<td>1.28</td>
</tr>
<tr>
<td></td>
<td>S100</td>
<td>0.75</td>
<td>1.21</td>
</tr>
<tr>
<td></td>
<td>S150</td>
<td>0.65</td>
<td>0.98</td>
</tr>
<tr>
<td>Quasi-static</td>
<td>S20</td>
<td>0.76</td>
<td>1.19</td>
</tr>
<tr>
<td></td>
<td>S100</td>
<td>0.69</td>
<td>1.20</td>
</tr>
<tr>
<td></td>
<td>S150</td>
<td>0.61</td>
<td>0.91</td>
</tr>
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</table>
Table 5  Average strain-rate up to failure in critical elements (in s\(^{-1}\)).

<table>
<thead>
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<th>Loading</th>
<th>Low-velocity</th>
<th>Quasi-static</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>S20</td>
<td>S100</td>
</tr>
<tr>
<td>600DL</td>
<td>119</td>
<td>95</td>
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<tr>
<td>1400M</td>
<td>78</td>
<td>126</td>
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</table>
Fig. 1 Geometries of material test specimens: (a) uniaxial tension, (b) in-plane shear and (c) uniaxial tension in split-Hopkinson tension bar.
Fig. 2 Positions of the virtual extensometer for displacement measurements in (a) uniaxial tension tests and (b) in-plane shear tests. The loading direction of the shear test is indicated by arrows.
Fig. 3  Experimental and numerical quasi-static engineering stress-strain curves for (a) Docol 600DL and (b) Docol 1400M, experimental and numerical normalized force versus displacement curve in in-plane shear for (c) Docol 600DL and (d) Docol 1400M, and experimental and numerical engineering stress-strain curves from quasi-static and dynamic tests on small SHTB-type specimens for (e) Docol 600DL and (f) Docol 1400M
Fig. 4  Flow stress at various strain rates for the two steel sheets.
Fig. 5 Finite element models of material test specimens: (a) mesh of tensile specimen with cubic elements in the deformable part, a denser mesh in the region exposed to necking and rigid (RB) elements in parts with prescribed displacement, (b) mesh of shear specimen with nodes on the symmetry plane marked with dots, (c) mesh of shear specimen where nodes with prescribed displacement are marked with dots, and (d) mesh of SHTB specimen.
Fig. 6 Experimental set-up for the impact tests (in mm): (a-b) overview of the set-up from different angles, (c) specimen geometries, (d) specimen clamped to rings, (e-g) details of steel ring between specimen and cylinder (the steel ring closest to the punch has similar geometry, but without the rounded edge towards the specimen), and (h) details of the supporting cylinder and the mounting steel plate.
Fig. 7 Low-velocity impact tests: (a) pendulum impactor [31], (b) detail of specimen attached to brackets with high-speed cameras below and deceleration buffers on both sides, (c) close-up of the punch attached to the load cell and mounted on the trolley, and (d) schematic view of the set-up with forces acting in the load cell and between the punch and the specimen.
Fig. 8 Set-up for quasi-static testing.
Fig. 9 Force-displacement curves from quasi-static and low-velocity impact tests. All successfully completed tests are shown.
Fig. 10 Evolution of spatial strain distribution in one duplicate of the quasi-static 600DL-S20 test
Fig. 11 Strain magnitude plots in the last image before fracture of selected duplicates of the low-velocity and quasi-static tests on Docol 600DL and Docol 1400M.
Fig. 12 Post-fracture images of selected duplicates of the sheet-impact tests.
Fig. 13 Fracture zone profiles of selected specimens of the sheet-impact tests. The image to the right illustrates the position from where the fracture zone images are taken.
Fig. 14 Principal strain histories from DIC measurements and numerical simulations of low-velocity (LV) and quasi-static (QS) sheet-impact tests. The S20 geometry tests are close to uniaxial tension (UT), the S100 tests are close to plane-strain tension (PST), the S150 tests are close to equi-biaxial tension (EBT). The squares in the 1400M-S150 simulations correspond to the experimental displacement at fracture, while the circles correspond to incipient necking.
Fig. 15 Finite element (FE) model of low-velocity test set-up: (a) FE mesh of specimen clamped between brackets. The nodes on the specimen with in-plane fixture are marked with dots. (b) FE geometry of trolley, load cell and punch.
Fig. 16 Experimental and numerical force-displacement curves from low-velocity impact tests. Incipient local necking in the simulation is marked with a circle for each case. In the force-displacement curve for the simulation of the 1400M-S150 test, the square marks the displacement at fracture in the test.
Fig. 17 Experimental and numerical force-displacement curves from quasi-static tests. Incipient local necking in the simulation is marked with a circle for each case. In the force-displacement curve for the simulation of the 1400M-S150 test, the square marks the displacement at fracture in the test.
Fig. 18 (a) Equivalent plastic strain contours at $t = 117$ s in the quasi-static 600DL-S20 simulation and the locations of the critical and neighbouring elements. (b) Plots of $\dot{\varepsilon}_p(t)$ and $\dot{\varepsilon}_{\text{mean}}(t)$ histories from the same analysis, and (c) the corresponding $E_{\text{dev}}(t)$ history. The point of incipient necking is shown in (b) and (c).
Fig. 19 Equivalent plastic strain in the last image before failure in the simulations of the sheet-impact tests. For the 1400M-S150 simulations, the image is taken before the displacement at fracture, while the other images are taken before onset of necking. The arrows indicate the positions where the largest deformations occurred.
Fig. 20 Strain-rate history collected from critical elements in the simulations. Onset of necking is indicated with circles, while onset of fracture in the 1400M-S150 tests is indicated with squares.