Adiabatic heating under various loading situations and strain rates for advanced high-strength steels

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Abstract. Adiabatic softening has an important impact on material behavior under dynamic loading. Temperature rise depends on the amount of plastic work converted into heat, as well as on the quantity of heat dissipation influenced by several parameters. In order to avoid complex thermomechanical coupling in simulation, a pseudo thermomechanical model based on an analytical approach of Dixon and Parry [1] was applied in the past [2, 3, 4, 5] considering the influence of strain rate on temperature rise. In this work the additional influence of the stress state and the size of the localized zone on temperature rise is investigated for advanced high-strength steels (AHSS). Therefore high speed tests were performed for different multiaxial stress states at loading rates ranging from isothermal to adiabatic conditions. Local strain fields were measured by high-speed video recording, and evaluated by digital image correlation (DIC), and temperature fields were recorded by high-speed infrared (IR) measurement. For shear loading, the results show a significantly larger amount of local plastic work dissipated by heat transfer until failure emergence compared to tensile loading at comparable strain rates. Hence, an extended model “adiabatic tension-shear model (ATS)” is proposed considering adiabatic softening under shear-dominated loading conditions in simulations.

1. Introduction
Heat is generated by conversion of plastic deformation energy in the material. Taylor and co-workers [6, 7] initially found that for metals plastic work is not completely transformed into heat. The percentage of plastic work converted into heat is characterized by the Taylor-Quinney coefficient (TQC), $\beta$, and for most metals this factor is assumed to be nearly constant as 0.9 [3, 8, 9]. However, numerous investigations were performed in the past showing that the TQC further depends on strain as well as stress state and the material itself [10, 11]. The fraction of plastic work which is not transformed into heat is stored in the crystal lattice as cold work due to recrystallization processes [2, 8]. The TQC in this original definition doesn’t consider heat transfer. However under dynamic loading conditions, as they arise in experiments, in real crash scenarios but also in common forming processes, a fraction of the generated heat is transferred by heat conduction. Since thermal softening plays a significant role in the deformation behavior after necking, in modelling of dynamic processes correct temperature levels have to be taken into account. In order to avoid complex FE-simulations with fully thermomechanical coupling, an efficient pseudo thermomechanical model was proposed and applied in the past [1, 2, 3, 4, 5] assuming a constant TQC and also considering heat transfer to be dependent on the plastic strain rate, $\dot{\varepsilon}_{pl}$. The TQC is corrected by a strain rate dependent isothermal-adiabatic weighting factor, $\omega$, 

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which takes values between 0 for isothermal and 1 for adiabatic conditions, as shown in Figure 1 left [1]. For \( \omega \), several mathematical functions with one or two independent parameters are proposed, for example an arctan- or tanh-function [2, 3, 4, 5]. The local temperature increase can be calculated by

\[
dT = \frac{\omega(\dot{\varepsilon}_{pl})\beta}{\rho c_p} \, d\varepsilon_{pl} = \frac{\omega(\dot{\varepsilon}_{pl})\beta}{\rho c_p} \, \sigma \, d\varepsilon_{pl}
\]

as shown in Figure 1 right, considering density, \( \rho \), and specific heat capacity, \( c_p \), assumed as constant. Since temperature increase leads to a decrease of yield stress, a coupling process with a commonly used Johnson-Cook plasticity model representing the effects of strain hardening, strain rate hardening and thermal softening in a multiplicative function is performed and described in detail in [5, 12, 13].

\[\text{Figure 1. Left: Example for weighting factor, } \omega(\dot{\varepsilon}_{pl}) \text{ [1], right: isothermal flow curves and calculated temperature increase for different strain rates [5].}\]

Since heat transfer is controlled by temperature gradients and shear-dominated stress states do show large temperature gradients caused by thin shear bands [14], the stress state with the size of the localized zone has also an influence on the local temperature increase. By use of a strain rate dependent weighting factor adapted to the isothermal-adiabatic transition strain rate range of uniaxial tension tests the adiabatic temperature increase and the resulting adiabatic softening for other stress states, especially for shear loading, might not be calculated correctly [5]. Therefore, in this work the influence of stress state and size of the localized zone on adiabatic heating is investigated for two advanced high-strength steels (AHSS). To obtain experimental data, high-speed tests were performed with transient strain and temperature field measurement for different stress states and under strain rates leading to thermal conditions between nearly isothermal and nearly fully adiabatic. For shear-dominated loading, the experimental results show higher local heat transfer in the highly deformed zone than for tension-dominated loading, regarding tests at strain rates on a comparable scale. From a more global point of view this difference between shear and tensile loading is less pronounced. To investigate the influence of this very local effect on global deformation and failure behavior, the existing pseudo thermomechanical model [5] was validated by conducting dynamic compression tests of components. The model predicts moderately higher adiabatic softening than the experimental results show. To improve the model by taking the elevated heat transfer in localized shear zones into account, an extended “adiabatic tension-shear model (ATS)” is introduced here, proposing different weighting factors for tension-dominated loading situations, \( \omega_t \), and for shear dominated loading situations, \( \omega_s \).

2. Experimental investigations on specimens
The experimental investigations in this study were performed for ZStE340 and DP1000 steel sheets having a thickness of 1.5 mm. The loading direction was chosen perpendicular to the rolling direction. In a first step uniaxial tension tests were carried out on flat tensile specimens at nominal strain rates ranging from 0.0008 s\(^{-1}\) to 100 s\(^{-1}\) covering the whole isothermal-adiabatic transition range. Addition-
ally, strain rate dependent tests were performed for a shear-dominated loading situation with notched shear tensile specimens, and for a plane strain loading state with notched tensile specimens. The specimen geometries are shown in Figure 2. For characterization under nearly adiabatic conditions the crash-typical nominal strain rate of 100 s⁻¹ was chosen for the ZStE340 material. The DP1000 material, which reveals a lower fracture strain value compared to ZStE340 material, was explored at a nominal strain rate of 10 s⁻¹, with regard to the maximal frame rate of the high-speed video frames of 15000 s⁻¹. 

The tests were performed on a high speed testing machine according to FAT guideline [16] and ISO 26203-2 [15], with quasi-local force measurement [19] and with DIC based local strain field analysis of high speed video frames. The high speed videos were recorded with a spatial resolution of 0.02 mm/pixel for all test speeds using a Photron FASTCAM camera of SA series. The frame rates for the different test conditions are given in Table 2. The stress-strain curves and the evaluation procedure of local strains by DIC are shown in detail in [18]. The transient adiabatic temperature field in the highly deformed zone was detected by high-speed IR measurement on the back side of the specimens. The calibration of the IR camera (IRCAM GEMINIS 327k ML) was performed in situ for each test series to consider the influence of the various parameters on IR-radiation. A detailed description of the calibration procedure is given in [20]. The IR videos are performed with a spatial resolution of 0.19

### Table 1. Test speeds and strain rate values according to FAT guideline [16] and ISO 26203-2 [15].

| Test speed | Material | Strain rate [s⁻¹] | uniaxial tension | shear tension | notched tension |
|------------|----------|------------------|-----------------|--------------|---------------|
|            |          | nominal engineering | mean engineering | plastic engineering | mean local plastic | mean local plastic | mean local plastic |
| 0.02 mm/s  | ZStE340  | 0.0008           | 0.0007          | 0.0009        | 0.0042        | -               | -               |
|            | DP1000   | 0.0008           | 0.0006          | 0.0006        | 0.0008        | -               | -               |
| 2.5 mm/s   | ZStE340  | 0.1              | 0.09            | 0.08          | 0.30          | -               | -               |
|            | DP1000   | 0.1              | 0.07            | 0.07          | 0.21          | -               | -               |
| 0.025 m/s  | ZStE340  | 1                | 0.9             | 0.9           | 2.4           | 5.4             | 5.4             |
|            | DP1000   | 1                | 0.8             | 0.7           | 2.1           | 4.8             | 2.7             |
| 0.25 m/s   | ZStE340  | 10               | 6.5             | 6.6           | 11.6          | 51              | 19              |
| 2.5 m/s    | ZStE340  | 100              | 117             | 94            | 241           | 584             | 824             |

### 2.1. Test setup

The tests were performed on a high speed testing machine according to FAT guideline [16] and ISO 26203-2 [15], with quasi-local force measurement [19] and with DIC based local strain field analysis of high speed video frames. The high speed videos were recorded with a spatial resolution of 0.02 mm/pixel for all test speeds using a Photron FASTCAM camera of SA series. The frame rates for the different test conditions are given in Table 2. The stress-strain curves and the evaluation procedure of local strains by DIC are shown in detail in [18]. The transient adiabatic temperature field in the highly deformed zone was detected by high-speed IR measurement on the back side of the specimens. The calibration of the IR camera (IRCAM GEMINIS 327k ML) was performed in situ for each test series to consider the influence of the various parameters on IR-radiation. A detailed description of the calibration procedure is given in [20]. The IR videos are performed with a spatial resolution of 0.19
mm/pxl for the test speed of 2.5 m/s and 0.08 mm/pxl for all other test speeds and the frame rates are documented in Table 2.

**Table 2. Test conditions.**

| test speed | 0.02 mm/s | 2.5 mm/s | 0.025 m/s | 0.25 m/s | 2.5 m/s |
|------------|-----------|----------|-----------|----------|---------|
| Frame rate high speed videos [fps] | 1 | 125 | 500 | 5000 | 25000 - 100000 |
| Frame rate IR videos [fps] | 10 | 780 | 870 | 10100 | 11000 |

**Figure 2.** Specimen geometries.

**Figure 3.** Example for measured local temperature and local equivalent plastic strain in the highly deformed zone for a uniaxial tension test at 0.025 m/s.

### 2.2. Results of specimen tests

The local temperature was evaluated as maximum temperature in the highly deformed zone. The local logarithmic equivalent plastic strain (v. Mises), \( \varepsilon_{pl} \), was determined by DIC [17] with a local gauge length ranging from 0.2 mm to 0.3 mm and for a specimen surface point showing a maximum amount of strain immediately before fracture [18]. The logarithmic equivalent plastic strain (v. Mises) was chosen as a suitable strain value to get comparable results for the different stress states. Figure 3 shows exemplary results of the measured local temperature and local equivalent strain development in the highly deformed zone up to point of time of fracture for a DP1000 uniaxial tensile specimen. Since temperature and strain measurement was performed with different frame rates a time correlation was realized by interpolating the strain signals in a time scale between two temperature values.

For the uniaxial tension tests, the recorded results of temperature increase versus local equivalent plastic strain are shown in Figure 4 (left diagram for ZStE340, right diagram for DP1000). For the ZStE340 in Figure 4 left the results of retries at 2.5 m/s and 0.025 m/s represent a good reproducibility of temperature and strain measurement. With increasing nominal strain rate higher temperature values were measured immediately before fracture. This is due to an increase of plastic work [18], as well as to reduced heat transfer when increasing the strain rate.
Figure 4. Measured local temperature rise versus local equivalent plastic strain for uniaxial tension tests at different nominal strain rates, left: ZStE340, right: DP1000.

The results for the different stress states are shown in Figure 5 (left ZStE340, right DP1000). Up to the failure strain of notched tension tests, the temperature strain correlation emerges nearly linear and is comparable for the different geometries investigated, neglecting moderate deviations for the ZStE340. For larger local strains, a lower temperature rise was measured for the shear tests compared to tension loading.

Figure 5. Measured local temperature rise versus local equivalent plastic strain at two test speeds for a shear tension, uniaxial tension and notched tension test (left: ZStE340, right: DP1000).

The significant result of a lower local temperature increase under shear loading compared to uniaxial tension loading for larger strains could be caused by stress-state dependent amounts of local plastic work converted into heat and local heat flux. The influence of local strain rate is here neglected (see Table 1 and [5]), as well as the influence of stress state on the TQC for these two materials [11]. For an energy-based comparison of stress-state dependent thermal conditions, the development of specific local heat energy, which is still stored in the material, is shown versus the specific local plastic work in Figure 6, in this case for ZStE340 material. The specific local heat energy is calculated according to equation (2) and the specific local plastic work is calculated according to equation (3)

\[ u = \rho c_p \Delta T \quad (2) \]

\[ w_{pl} = \int \sigma d\varepsilon_{pl} \quad (3) \]

with \( \rho \) equal to 7.86 g/cm\(^3\) and \( c_p \) equal to 0.47 J/(gK). \( \varepsilon_{pl} \) as the local equivalent plastic strain evaluated by DIC and the measured force. Lateral forces presumably emerging in the clamping device are
neglected in this approach by use of a rotatable clamping design. However, volume constancy, uniaxial stress-state for the flat tensile specimens, and simple shear stress state for the shear tests also are assumed [21], neglecting stress gradients in the width and thickness directions, respectively. Regarding the experiments performed at test speed of 2.5 m/s the ratios \( u/w \) immediately before fracture reach values of nearly 0.9 for all stress states investigated (Figure 6). The results for the test speed of 0.025 m/s in contrast show for shear loading significant lower \( u/w \) values compared to uniaxial tensile loading for large specific plastic work. This effect indicates higher local heat transfer under shear loading compared to tension loading and is investigated more deeply in the following diagrams in Figure 7.

![Figure 6](image_url)

**Figure 6.** Specific heat energy versus specific plastic work at two test speeds for a shear tension, uniaxial tension and notched tension test for ZStE340.

The specific local plastic work and the specific heat energy immediately before fracture are represented for section lines oriented perpendicular to the localized zone (left for uniaxial tension test and right for shear tension test in Figure 7). Heat energy is calculated by using a mean value of measured temperature increase for 5 parallel running section lines shown in the IR pictures in Figure 7 (top). The ratio \( u/w \) immediately before fracture is determined for the maximum local energy values (Table 3). Under tension loading, about 63 % of the local plastic work remains as heat energy in the material and under shear loading it is only 30 %. From a more global point of view the ratio \( u/w \) is determined considering the summarized amount of energy for a section line having a length of 4 mm and the ratios \( u/w \) for the two loading situations approach each other, as can be seen in Table 3. It has to be mentioned that the factor \( u/w \) not only includes heat transfer but also the percentage of plastic work converted into heat characterized by the TQC.

Regarding thermal conditions between nearly isothermal and nearly fully adiabatic, significantly less local heat energy related to local plastic work remains in the material under shear loading compared to tension loading. Thus in shear bands, more heat energy is locally dissipated compared to the necking area under tensile loading at comparable strain rates. This effect reveals that local adiabatic temperature increase and resulting adiabatic softening is not only dependent on the strain rate but also on the size of the localized zone caused by different stress states. From a more global point of view this effect is less pronounced and decreases even more with increasing the gauge length (see Figure 7). The following investigations have the purpose to check the influence of this very local physical effect on the global deformation and failure behavior of a component. Therefore, the pseudo thermomechanical model used in the past [5] was validated and improved by taking the elevated local heat transfer under shear loading into account.
Figure 7. Specific heat energy and accumulated specific plastic work along a section line through the highly deformed zone directly before fracture for ZStE340 tested at 0.025 m/s, left: uniaxial tension, right: shear tension (same scaling), top: IR images directly before fracture with 5 section lines used for mean temperature calculation.

Table 3. \( w/w \)-values before fracture (ZStE340, \( v = 0.025 \) m/s, tensile & shear loading).

| Section Description | Uniaxial Tension | Shear Tension |
|---------------------|-----------------|---------------|
| Local               | \( \frac{u_{\text{max}}}{w_{\text{max}}} \) | 0.63          | 0.30          |
| Section from -2 mm to +2 mm | \( \frac{u_{\text{4 mm}}}{w_{\text{4 mm}}} \) | 0.50          | 0.39          |

3. Validation and improvement of the pseudo thermomechanical model

3.1. Experimental and numerical investigations on components

Laser welded rectangular profiles having a length of 350 mm and produced from bent sheets were compressed with a flat impactor at a test speed of 5 m/s. The cross section of the profile is shown in Figure 8. For well-defined initial contact and resulting folding conditions a symmetric 5° chamfer was machined on the impacted side of each test piece. The opposite region of the profiles was fixed by bonding its end section into a fitting shape. The force was measured with a +/- 500 kN piezo load cell and the displacement of the impactor was determined by DIC evaluation of high-speed video records. The temperature was recorded by high-speed IR measurement. The numerical determination of the yielding and damage behavior is based on the experimental results presented in Figure 4 and 5 and was performed by use of LS-Dyna 9.0.1 (explicit) using MAT_TABULATED_JOHNSON-COOK (MAT224) and the GISSMO damage model MAT_ADD_EROSION/GISSMO [13]. The calibration procedure and the results for the strain rate dependent GISSMO damage curves are described in detail in [5]. For the strain rate dependent weighting factor, a two-parametric arctan-function-based approximation according to equation (4) was chosen,

\[
\omega(\dot{\varepsilon}_{\text{pl}}) = 0.5 + \frac{1}{\pi} \arctan\left(b_w \ln\left(\frac{\dot{\varepsilon}_{\text{pl}}}{\dot{\varepsilon}_{\text{pl}, w}}\right)\right)
\]

with the width of the transition region, \( b_w \), and the transition strain rate, \( \dot{\varepsilon}_{\text{pl}, w} \). The parameters \( b_w \) and \( \dot{\varepsilon}_{\text{pl}, w} \) were determined by adjusting temperature fields obtained from simulation to experimental re-
sults of uniaxial tension tests before fracture emerged [5]. The simulation of the profile was performed with Belytschko-Tsay shell elements with an element size of 2 mm. The friction was considered by a constant Coulomb coefficient $\mu = 0.1$. The impactor and clamping device were modelled as rigid bodies.

![Figure 8](image_url)

**Figure 8.** Left: Model setting with chamfer and cross section of the profile, right: Results of a dynamic component test for DP1000 (experiment and simulation).

In the diagram in Figure 8 the measured and calculated forces and work are shown for the DP1000 material. The results determined by simulation and those gained by experiment show a good agreement of the force displacement characteristic. The measured work can be interpreted as the absorbed energy of the bifurcating profile, because of the very stiff impactor and clamping device. The predicted absorbed energy up to an impactor-displacement of 250 mm ranges about 10 % lower than the experimental result. Probable reasons can be either the disregarded forming history of the material in simulation [22], as well as overestimated temperatures in the folding regions. The temperature increase in the folding regions was measured up to the range of 200 - 300 K, and calculated by simulation more than 400 K at some locations. Calculated stress triaxialities, $\eta$, in the folded regions cover the whole range from –0.1 (pressure) to 0.6 (multiaxial tension) including shear loading states. In this model the elevated local heat transfer in the shear-dominated regions is not considered and might be responsible for partially overestimated temperatures. To improve the model with regards to a more accurate prediction of absorbed energy, the stress state should be taken into account, particularly if local shear-dominated loading conditions occur.

3.2. **Adiabatic tension-shear model (ATS)**

Hence, an extension of the presently used model approach is proposed as "adiabatic tension-shear model (ATS)" with two strain rate dependent weighting factors in an analogous approach to the experimentally observed tension and shear localization. The tension weighting factor, $\omega_t(\dot{\varepsilon}_{pl})$, considers tension-dominated stress states with $\eta \geq 1/3$ and the shear weighting factor, $\omega_s(\dot{\varepsilon}_{pl})$, takes shear-dominated loading situations with $\eta < 1/3$ into account. Both weighting factors are related to the TQC and shown exemplarily versus the strain rate as plotted in Figure 9. With this extended model the local temperature increase can be calculated by

$$dT = \frac{\omega_i(\dot{\varepsilon}_{pl})}{\rho c_p} dW_{pl} = \frac{\omega_i(\dot{\varepsilon}_{pl})}{\rho c_p} \sigma d\varepsilon_{pl},$$

with $i = s$ ($\eta < 1/3$), $i = t$ ($\eta \geq 1/3$)  

(5)
The tension weighting factor can be determined on the basis of uniaxial tension tests, in the same way as described for the so far used $\omega_t(\dot{\varepsilon}_p)$. The shear weighting factor can be determined completely independently on the basis of temperature field measurements of dynamic shear tests covering the region between nearly isothermal and nearly adiabatic conditions. In order to reduce the amount of necessary shear tests, this extended approach is formulated in a more general way by introducing a stress dependent shifting of the tension weighting factor along the plastic strain rate axis, $\Delta \dot{\varepsilon}_p(\eta)$, shown in Figure 9. The shear weighting factor can then be calculated according to equation (6). Investigations about the determination of $\Delta \dot{\varepsilon}_p(\eta)$ and the validation of the model are still in progress.

$$\omega_s(\dot{\varepsilon}_p) = \omega_t \left(\dot{\varepsilon}_p - \Delta \dot{\varepsilon}_p(\eta) < \frac{1}{3}\right)$$  \hspace{1cm} (6)

**Figure 9.** Tension and shear weighting factor versus strain rate to use in adiabatic tension-shear model (ATS).

4. Conclusions
In this work a weakness of the pseudo thermomechanical model describing the adiabatic softening dependent on the plastic strain rate is addressed. Experimental results from high-speed specimen tests under shear and tension dominated loading and from dynamic component tests show that adiabatic softening depends not only on the strain rate but on the size of the localized zone caused by different stress states, too. Under shear loading more heat is dissipated locally compared to tension loading situations, as this study shows. As a conclusion, an extended “adiabatic tension-shear model (ATS)” is proposed that considers adiabatic softening under shear-dominated loading situations and will be improved and applied in future investigations.

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