

Experimental and numerical study of the influence of slide burnishing on fracture resistance of advanced high-strength steels under stretch-bending conditions

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ABSTRACT

Advanced high strength steels (AHSS) play a critical role in the automotive industry due to their superior strength-to-weight ratio, enabling vehicle weight reduction while maintaining safety standards. However, their limited ductility and formability under complex loading conditions pose manufacturing challenges. Slide burnishing has emerged as a promising surface treatment technique to improve mechanical properties by inducing compressive residual stresses. In this context, the stretch-bending test is an effective method for evaluating fracture resistance in perforated AHSS sheets. This study investigates the influence of slide burnishing forces on the fracture displacement of two AHSS types: DP590-R and TRIP440-Y. An experimental design (3²) was employed to evaluate the effect of three burnishing forces (125 N, 250 N, 400 N) and three punch diameters (3 mm, 6 mm, 12 mm) during stretch-bending tests. Numerical simulations using a finite element model with the Barlat anisotropic yield criterion accurately predicted thinning patterns near the hole, supporting experimental observations.

Keywords: stretch-bending, TRIP, dual phase, finite element, fracture.

INTRODUCTION

Advanced high-strength steels (AHSS) are essential in the automotive and aerospace industries due to their ability to reduce weight while maintaining safety, thus improving fuel efficiency and lowering environmental impact [1]. Their multiphase microstructure, typically combining soft ferrite, bainite, and hard martensite provides an excellent strength–ductility balance. However, as strength increases, AHSS exhibit limitations such as reduced formability, edge cracking, and variability in mechanical properties [2–4]. These limitations are particularly critical in structural components with pierced holes or sharp edges, where stress concentration accelerates failure.

Evaluating edge stretchability is critical because failures often initiate near punched,

drilled, laser-cut or wire-cut holes. The hole expansion test (HET), standardized in ISO 16630 [5], is widely used to assess the hole expansion ratio (HER). Prior studies have shown that HER is influenced by tensile strength [6], edge quality [7], and strain path [8], while higher fracture toughness delays crack initiation [9]. Moreover, numerical simulations have highlighted the role of thickness gradients in predicting localized thinning and fracture [10]. These results underline the need for complementary techniques that can improve fracture resistance under complex loading paths.

Burnishing is a surface process that induces plastic deformation through a hard ball or roller, generating compressive residual stresses that improve hardness, fatigue strength, and wear resistance [11–13]. By applying a normal force to

the surface, plastic deformations are induced on the material's surface by controlling parameters like normal force, speed, path, and number of passes [12]. It has been applied to several materials such as steels and aluminum alloys, demonstrating reductions in roughness [14], increases in elongation and hardness [15,16], and enhancements in fatigue limit [17]. In AHSS, burnishing has been shown to positively influence the yield strength, anisotropy, and surface quality [16], suggesting potential benefits in delaying fracture initiation. Yet, most studies focus on tensile or fatigue performance, leaving limited evidence on its role in sheet metal forming, particularly under stretch-bending conditions.

Therefore, this study investigates the influence of slide burnishing on the fracture resistance of two AHSS grades (DP590-R and TRIP440-Y) using stretch-bending tests on perforated specimens. The effect of burnishing force and punch diameter is evaluated experimentally and supported by finite element simulations. This work aims to determine whether slide burnishing can enhance fracture displacement and delay failure initiation in critical edge-loaded conditions.

MATERIALS AND METHODS

Design of experiments

Slide burnishing was applied to tensile and stretch-bending specimens using three burnishing forces 125 N, 250 N, and 400 N. These values were determined from a force depth calibration curve obtained with a spring-loaded load cell mounted on a CNC Fadal VMC machine. Two types of sheets were used for the experiment, a 1.4-mm-thick dual-phase steel (DP590-R) and a 1.07-mm-thick transformation-induced plasticity steel (TRIP440-Y). A linear toolpath with 50% overlap of the indentation footprint ensured complete surface coverage, as recommended by Alghazou et al. [18]. To ensure homogeneous

plastic deformation, both sides of each specimen were treated: a 120 mm length in tensile samples and 50 mm in stretch-bending specimens. Table 1 shows footprints and indentation for each force and material.

The experimental matrix combined the three burnishing forces with three punch diameters 3, 6, and 12 mm, arranged in a 3^2 DOE. Each condition was replicated three times, resulting in 27 tests per material. Burnishing was performed using a 10 mm diameter fixed diamond tip tool, with a constant feed rate of 100 mm/min and a zigzag trajectory. The experimental setup for the burnishing process and the toolpath is depicted in Figure 1 a) and b).

Experimental configuration

Stretch-bending tests were performed using the stretch-bending tooling shown in Figure 2. The tooling was mounted on an Instron universal testing machine, which provided precise control of punch displacement. Sheets were clamped between the die and blank holders using eight screws, applying a total holding force of 50 kN to restrict lateral movement. Rigid AISI 1018 steel punches with diameter of 3, 6, and 12 mm were used to apply bending loads until fracture occurred. Load–displacement data were continuously recorded by the Instron system. Relevant tooling dimensions, such as clearance (c) and die radii (R), are illustrated in Figure 2.

Fracture initiation was monitored with an 8 MP digital camera controlled by a Raspberry Pi 3B+, recording video and images at 10 Hz during deformation. The complete experimental setup is presented in Figure 3, and specimen geometry is shown in Figure 4.

Finite element model

The stretch-bending test was simulated using LS-DYNA. The sheet was modeled with

Table 1. Calibration table Force [N] vs Indentation [μm]

Force (N)	DP590-R		TRIP440-Y	
	Footprint diameter [mm]	Indentation [μm]	Footprint diameter [mm]	Indentation [μm]
125	0.3440	1.6154	0.4610	1.3770
250	0.4880	2.9779	0.5860	2.7240
400	0.6150	4.6129	0.7360	4.3870

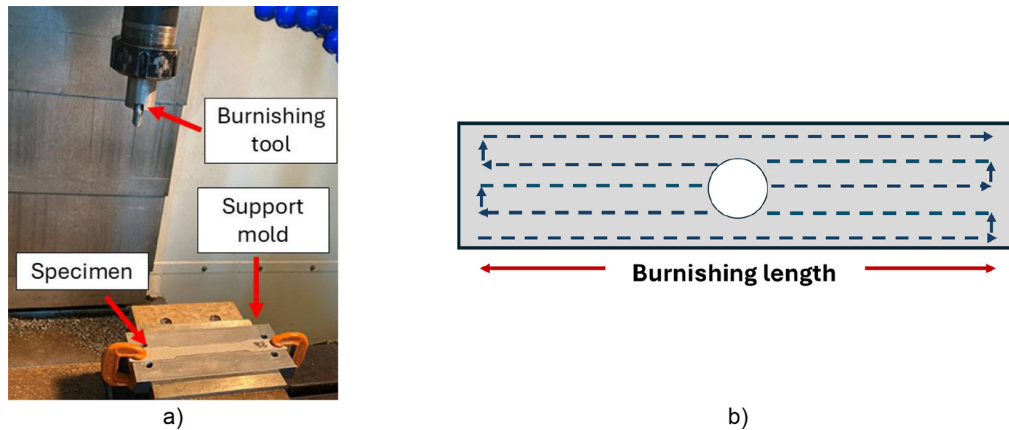


Figure 1. a) Experimental set-up of the slide burnishing process, b) Zigzag burnishing path

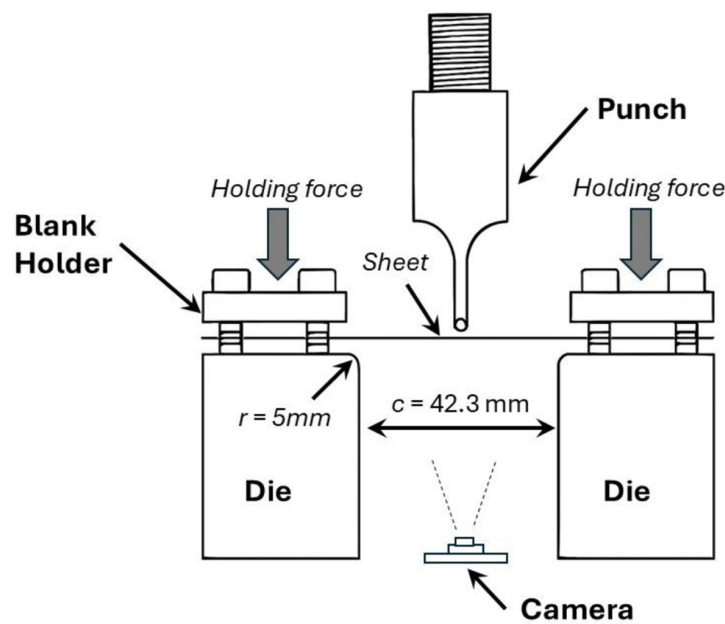


Figure 2. Schematic diagram of the stretch-bending experiment

a thickness of 1.07 mm for TRIP440-Y and 1.40 mm for DP590-R. Punch displacement was limited to 10 mm, consistent with the experimental observations of fracture initiation. The sheet was discretized using Belytschko–Tsay shell elements with selective reduced integration. A refined mesh of 0.5 mm was used in the deformation zone near the hole and punch contact area, while a coarser mesh of 2 mm was applied elsewhere to reduce computational cost. A mesh convergence study was conducted to ensure that the element size adequately captured strain localization without significantly affecting computational time. Punches and die were modeled as rigid analytical surfaces. Boundary conditions restricted the punch to vertical displacement along the Z-axis, while the blank holder applied a constant

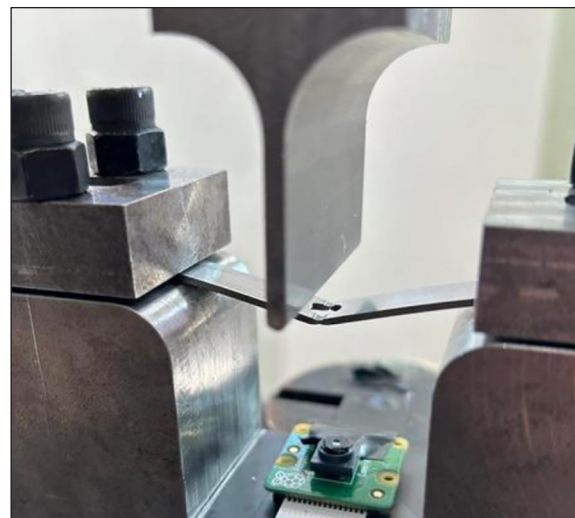


Figure 3. Experimental set-up of the stretch-bending experiment

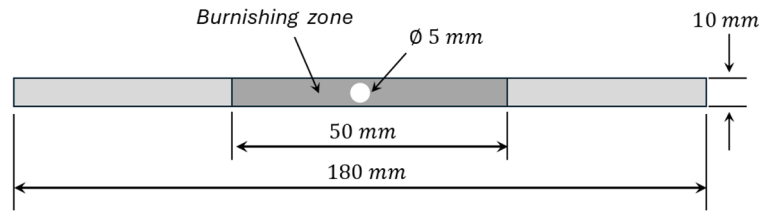


Figure 4. Stretch-bending specimen geometry

clamping force of 50 kN. The die was fully constrained in all degrees of freedom, and the sheet was allowed vertical displacement and in-plane rotation around the Y-axis. Contact interactions were modeled using the Forming Surface-to-Surface (FTSS) algorithm with a friction coefficient of 0.1, which is commonly reported for dry sheet forming operations. The finite element model is presented in Figure 5.

Material hardening behavior was described by the Hollomon power law:

$$\sigma = K\varepsilon^n \quad (1)$$

where: σ is the true stress, ε the plastic strain, K the strength coefficient, and n the strain-hardening exponent.

Planar anisotropy was incorporated using Barlat's three-parameter yield criterion, which requires the r -values at 0° , 45° , and 90° from the rolling direction, as well as the parameter m . For face-centered cubic (FCC) metals, $m = 6$ is typically assumed:

$$\phi = |S_1 - S_2|^m + |S_2 - S_3|^m + |S_3 - S_1|^m = 2\bar{\sigma}^m \quad (2)$$

where: S_1 , S_2 , S_3 are the principal values of the deviatoric stress tensor and $\bar{\sigma}$ is the effective stress.

The simulations were performed under quasi-static conditions by controlling punch velocity to minimize inertial effects. Material failure was not modeled explicitly; instead, fracture was identified when element thinning exceeded the experimental threshold observed at fracture onset. Model predictions were validated against experimental fracture force vs fracture displacement curves and thickness reduction measurements near the hole. This calibration ensured that the FEM adequately reproduced both global and local deformation responses under stretch-bending conditions.

EXPERIMENTAL AND NUMERICAL RESULTS

Mechanical characterization

Uniaxial tensile tests were conducted according to ASTM E8 [19] on specimens in the as received and burnishing condition oriented at 0°

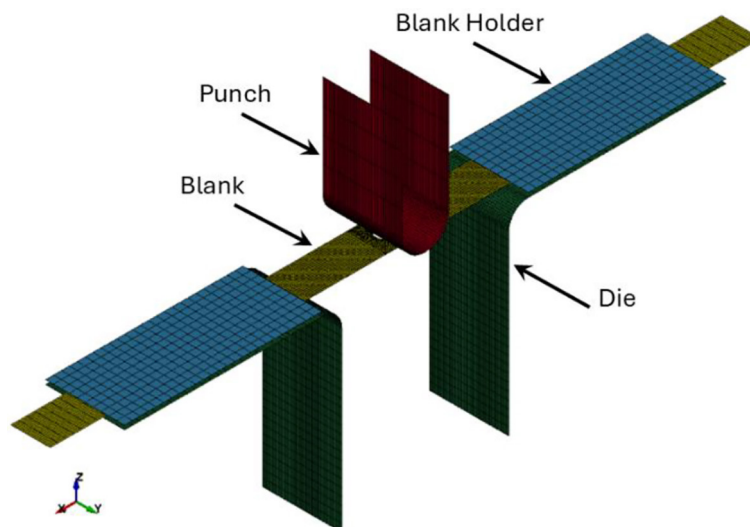


Figure 5. Stretch-bending finite element model.

to the rolling direction, to determine yield strength (S_y), ultimate tensile strength (S_u), and Young's modulus (E). The strain-hardening exponent (n) and the strength coefficient (K) were calculated using the Hollomon law [20–21]. To determine the anisotropy parameters of the Lankford coefficient (r -value), experiments at 0° , 45° , and 90° with respect to the rolling direction were conducted. For each condition, three replicates were performed to ensure repeatability. Results for the mechanical characterization of both materials are summarized in Tables 2 and 3.

In both steels, there is a direct relationship between burnishing force and yield strength. For DP590-R, S_y increases by approximately 30%,

while for TRIP440-Y the increase is about 26%. The ultimate tensile strength (S_u) shows only marginal variations, indicating that burnishing affects the elastic region and the material's strain-hardening capacity. The strain-hardening exponent (n) decreased as burnishing force increased, which may reduce the uniform elongation capacity of both steels. In TRIP440-Y, a marked reduction in Young's modulus (E) was also observed at higher burnishing forces, which could influence stiffness-sensitive applications.

The Lankford coefficients (r_0 , r_{45} , r_{90}) progressively converge toward 1 with increasing burnishing force, suggesting a transition from anisotropic behavior induced by rolling

Table 2. Mechanical properties and anisotropy parameters of DP590-R

Burnishing force	Sy (MPa)	Su (MPa)	E (GPa)	r_0	r_{45}	r_{90}	K (MPa)	n
0 N	369.37	743.33	202.00	0.843	0.782	1.152	970.12	0.166
125 N	378.84	735.74	206.94	0.914	0.905	1.050	944.18	0.157
250 N	425.88	731.93	197.07	0.931	0.901	1.078	932.30	0.151
400 N	480.01	734.31	197.61	0.966	0.966	1.015	923.87	0.142

Table 3. Mechanical properties and anisotropy parameters of TRIP440-Y

Burnishing force	Sy (MPa)	Su (MPa)	E (GPa)	r_0	r_{45}	r_{90}	K (MPa)	n
0 N	438.38	852.54	203.00	0.671	0.750	1.100	1386.22	0.268
125 N	472.60	867.59	208.63	1.002	0.820	0.840	1372.08	0.281
250 N	483.34	862.61	200.99	0.928	0.837	0.990	1223.29	0.257
400 N	552.38	888.63	166.61	0.959	0.990	1.020	1229.47	0.249

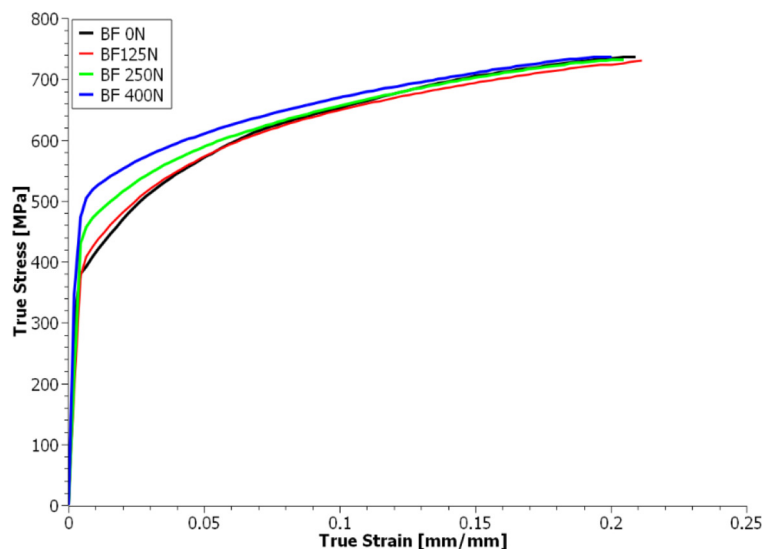


Figure 6. Stress-Strain curves of DP590-R sheet

to a more isotropic response. This reduction in anisotropy due to burnishing, evidenced by r -values approaching unity, contributes to a more uniform thinning distribution during deformation. In anisotropic conditions, localized thinning often develops along preferred orientations, accelerating crack initiation at critical sites such as hole edges. By promoting isotropy, burnishing mitigates directional dependence of plastic flow, leading to a more homogeneous strain distribution and delaying fracture onset under stretch-bending conditions. The stress-strain curves for as-received sheets and sheets burnished at 125 N, 250 N, 400 N, are compared in Figure 6 and Figure 7, for the DP590-R and TRIP440-Y, respectively.

Fracture force and punch displacement

Figure 8 shows the experimental vs. numerical results of the fracture displacement vs burnishing force (Figure 8a) and fracture force vs burnishing force (Figure 8b) for the DP590-R. The results are presented for each punch diameter (3, 6, 12 mm) identified as P3, P6 and P12, respectively. For each curve, the experimental fracture point and the simulation-based failure point (mesh instability criterion) are indicated. Fracture displacement generally decreases as burnishing force increases, while it increases with punch diameter. The most favorable combinations for delaying fracture for the DP590-R material are with the 6 mm punch diameter the unburnished condition and 12 mm

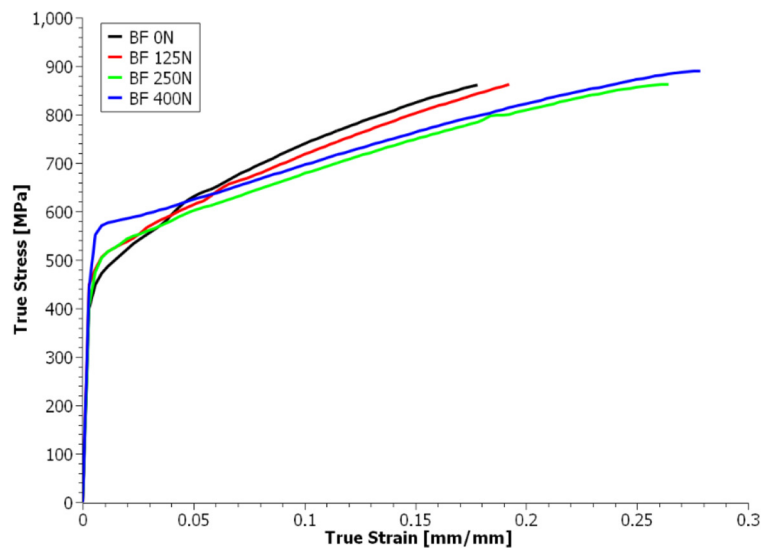


Figure 7. Stress-Strain curves of TRIP440-Y sheet

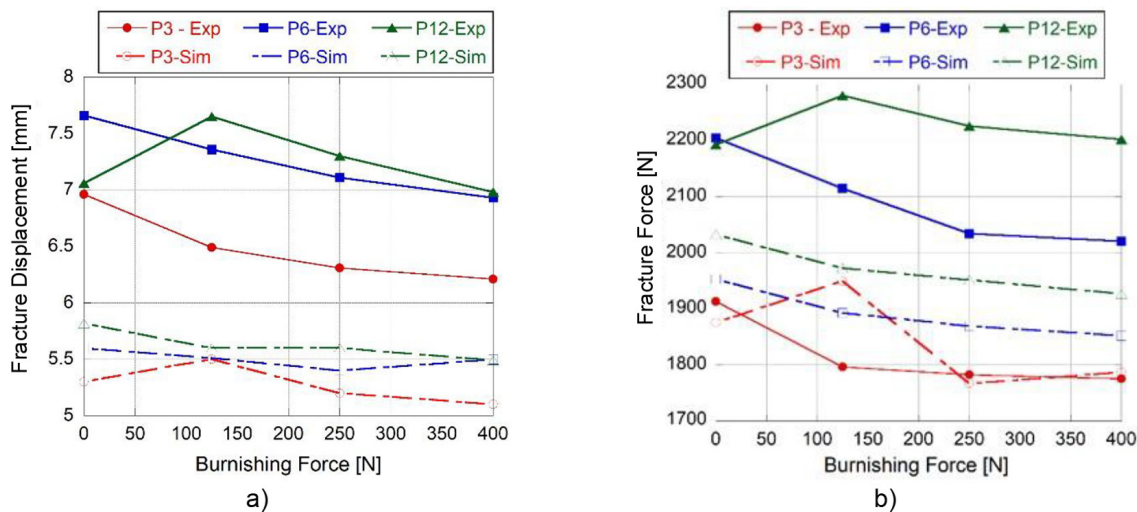


Figure 8. a) Fracture displacement vs. burnishing force; b) fracture force vs. burnishing force of a DP590-R steel

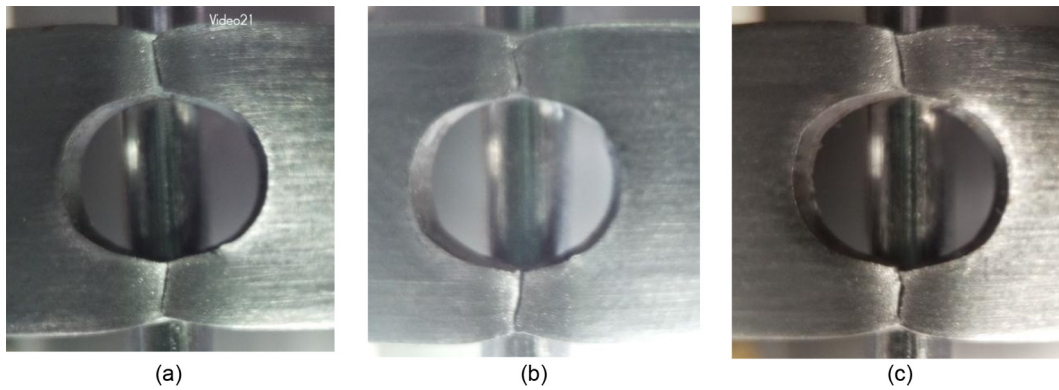


Figure 9. Fracture initiation in specimens burnished at forces of: (a) 125N, (b) 250N, (c) 400N

punch diameter with 125 N and 250 N of burnishing force, which achieve the largest displacement at fracture. Furthermore, it should be noted that the simulation underpredicts the experimental results in most cases. Figure 9 shows the fracture profiles with the three different burnishing forces where a severe necking condition is observed in the direction where the fractures take place.

Figure 10 shows the experimental vs. numerical results of the fracture displacement vs burnishing force (Figure 10a) and burnishing force vs fracture force (Figure 10b) for the TRIP440-Y. As for the previous material, the results are presented for each punch diameter (P3, P6 and P12) respectively. In a similar analysis it was found that for the P3 punch, sheet formability is enhanced with 125 and 250 N burnishing forces. Overall, fracture displacement increased with punch diameter, while the effect of burnishing was less consistent than in DP590-R.

DP590-R statistical analysis

The mechanical responses of fracture force and fracture displacement to the factors of burnishing force (A) and punch diameter (B) were analyzed. For both response variables, the normality assumption was not satisfied, as confirmed by Shapiro-Wilk tests (Fracture Force: $W=0.8257$, $p=0.0004$; Fracture Displacement: $W=0.9170$, $p=0.0334$). Given the primary objective of developing predictive models and the recognized robustness of regression to moderate non-normality, quadratic regression analysis was employed for both responses.

The developed quadratic models demonstrated an exceptional fit to the experimental data for both fracture force and fracture displacement. The goodness-of-fit statistics, presented in Table 4, indicate outstanding predictive capability. The high coefficients of determination more than ($R^2 > 0.9890$) confirm that the models explain nearly

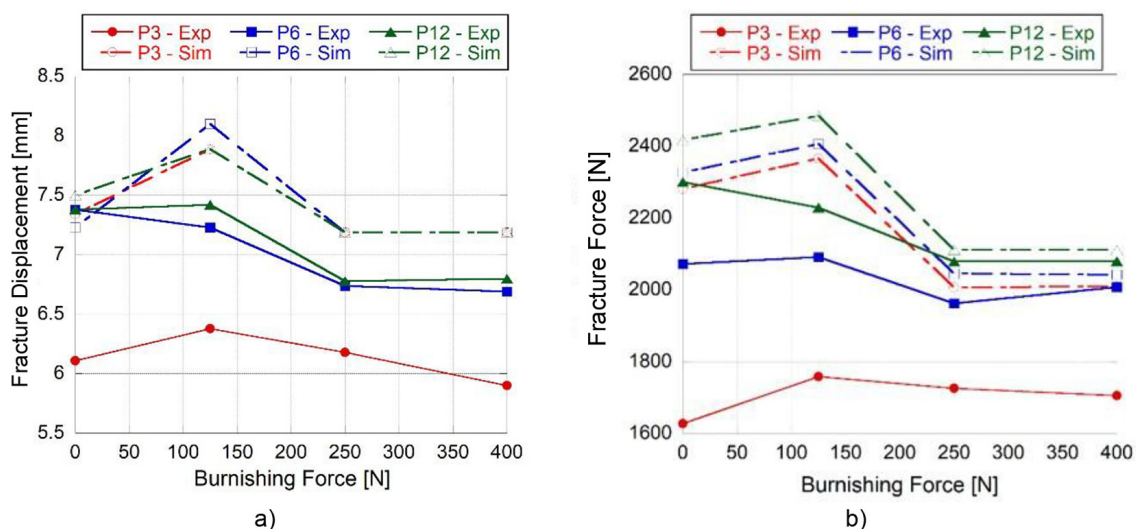


Figure 10. (a) Fracture displacement vs. burnishing force; (b) fracture force vs. burnishing force of a TRIP440-Y steel

Table 4. Fit statistics for the quadratic regression model of DP590-R

Fracture Force			
Standard deviation	19.37	R-Squared (R ²)	0.989
Mean	2018.20	Adjusted R ²	0.9863
Coefficient of variation	0.96	Predicted R ²	0.9822
		Adequate precision	541.26
Fracture Displacement			
Standard deviation	0.0135	R-Squared (R ²)	0.999
Mean	6.9263	Adjusted R ²	0.999
Coefficient of variation	0.2	Predicted R ²	0.998
		Adequate precision	2658.72

all the variability in the data. The exceptionally high Adequate Precision values ($\gg 4$) signify a strong signal and confirm the models' suitability for navigating the design space.

The final quadratic regression model in terms of coded factors is presented below:

$$\begin{aligned} \text{Fracture force} = & 2113.54 - \\ & - 16.38 \cdot A + 207.54 \cdot B + \\ & 6.83 \cdot A^2 + 4.39 \cdot A \cdot B - \\ & - 102.17 \cdot B^2 \end{aligned} \quad (3)$$

$$\begin{aligned} \text{Fracture displacement} = & 7.246 - \\ & - 0.187 \cdot A + 0.499 \cdot B + 0.034 \cdot A^2 - \\ & - 0.066 \cdot A \cdot B - 0.354 \cdot B^2 \end{aligned} \quad (4)$$

where: A – burnishing force, B – punch diameter.

To statistically validate the differences between the experimental treatment groups, Kruskal-Wallis tests were performed. The results confirmed statistically significant differences for both fracture force ($H=24.99$, $p=0.0016$) and fracture displacement ($H=25.69$, $p=0.0012$). The large effect sizes ($\eta^2 > 0.94$) indicate a substantial influence of the process parameters on both mechanical responses. Post-hoc analyses using Dunn's test with Holm correction were conducted to identify specific pairwise differences between treatment groups. The results are summarized in Table 5.

For fracture force, the response surface indicates that the burnishing process parameters exert a considerable influence on the material's mechanical response. The highest fracture forces are observed at larger punch diameters (12 mm) and a burnishing force of 125 N. It is observed that the burnishing force has no major influence on fracture resistance. For fracture displacement, the surface reveals that lower burnishing forces

(125 N) combined with larger punch diameters (12 mm) lead to the most pronounced fracture displacement values. This behavior suggests that a milder burnishing force allows greater plastic deformation and surface compliance, while higher forces may restrict deformation due to increased surface hardening as shown in Figure 11.

TRIP 440-Y statistical analysis

The mechanical responses of burnishing force (A) and punch diameter (B) were analyzed with Shapiro-Wilk. The fracture force data significantly deviated from a normal distribution ($W=0.8843$, $p=0.0059$), while the fracture displacement data did not ($W=0.954$, $p=0.269$). Given the primary objective of developing predictive models and the robustness of regression analysis, quadratic models were developed for both responses. The quadratic regression models demonstrated an excellent fit to the experimental data. The goodness-of-fit statistics, presented in Table 6, confirm high predictive capability for both fracture force and fracture displacement. The high coefficients of determination ($R^2 \geq 0.9597$) and adequate precision values ($\gg 4$) signify strong models suitable for navigating the design space. The final quadratic regression model in terms of coded factors is presented below:

$$\begin{aligned} \text{Fracture force} = & 2034.52 - \\ & 42.16 \cdot A + 198.00 \cdot B + \\ & + 41.80 \cdot A^2 - 13.85 \cdot A \cdot B - \\ & - 122.10 \cdot B^2 \end{aligned} \quad (5)$$

$$\begin{aligned} \text{Fracture displacement} = & \\ & 6.906 - 0.2438 \cdot A + \\ & + 0.425 \cdot B + 0.1079 \cdot A^2 - \\ & - 0.054 \cdot A \cdot B - 0.3762 \cdot B^2 \end{aligned} \quad (6)$$

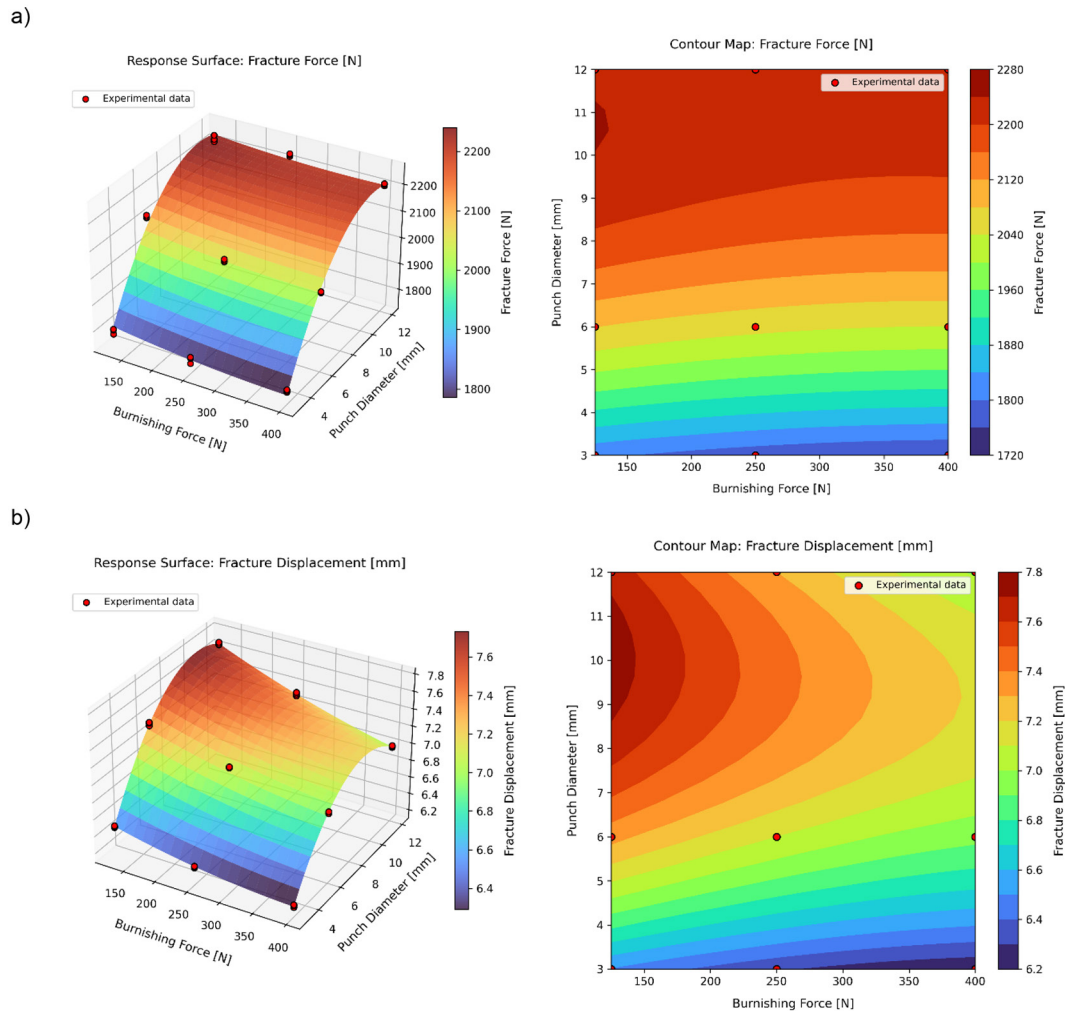


Figure 11. Response of the quadratic regression equation for DP590-R: a) fracture force, b) fracture displacement

Table 5. Pairwise comparisons using Dunn’s test with Holm correction for DP590-R

Group comparison	Adjusted p-value	Significant
Fracture force		
250N-12mm vs 250N-3mm	0.0456	Yes
250N-12mm vs 400N-3mm	0.0185	Yes
Remaining comparisons	> 0.05	No
Fracture displacement		
125N-12mm vs 250N-3mm	0.0416	Yes
125N-12mm vs 400N-3mm	0.0076	Yes
125N-6mm vs 400N-3mm	0.0442	Yes
Remaining comparisons	> 0.05	No

where: A – burnishing force, B – punch diameter.

Kruskal-Wallis tests confirmed statistically significant differences between treatment groups for both fracture force ($H = 25.0794$, $p = 0.0015$) and fracture displacement ($H = 254.26$, $p = 0.0021$). The large effect sizes ($\eta^2 > 0.90$)

indicate a substantial influence of the process parameters on both mechanical responses. Post-hoc analyses using Dunn’s test with Holm correction identified specific pairwise differences, summarized in Table 7.

The results reveal a consistent trend: treatments involving a low burnishing force (125

Table 6. Fit statistics for the quadratic regression model of TRIP440-Y

Fracture Force			
Standard deviation	23.25	R-Squared (R^2)	0.9833
Mean	1954.21	Adjusted R^2	0.9793
Coefficient of variation	1.19	Predicted R^2	0.9733
		Adequate precision	436.83
Fracture Displacement			
Standard deviation	0.0915	R-Squared (R^2)	0.9597
Mean	6.6381	Adjusted R^2	0.9501
Coefficient of variation	1.38	Predicted R^2	0.9340
		Adequate precision	377.06

Table 7. Pairwise comparisons using Dunn’s test with Holm correction for TRIP 440-Y

Group comparison	Adjusted p-value	Significant
Fracture force		
125N-12mm vs 250N-3mm	0.0418	Yes
125N-12mm vs 400N-3mm	0.0077	Yes
Remaining comparisons	> 0.05	No
Fracture displacement		
125N-12mm vs 250N-3mm	0.0485	Yes
125N-12mm vs 400N-3mm	0.0076	Yes
125N-6mm vs 400N-3mm	0.0417	Yes
Remaining comparisons	> 0.05	No

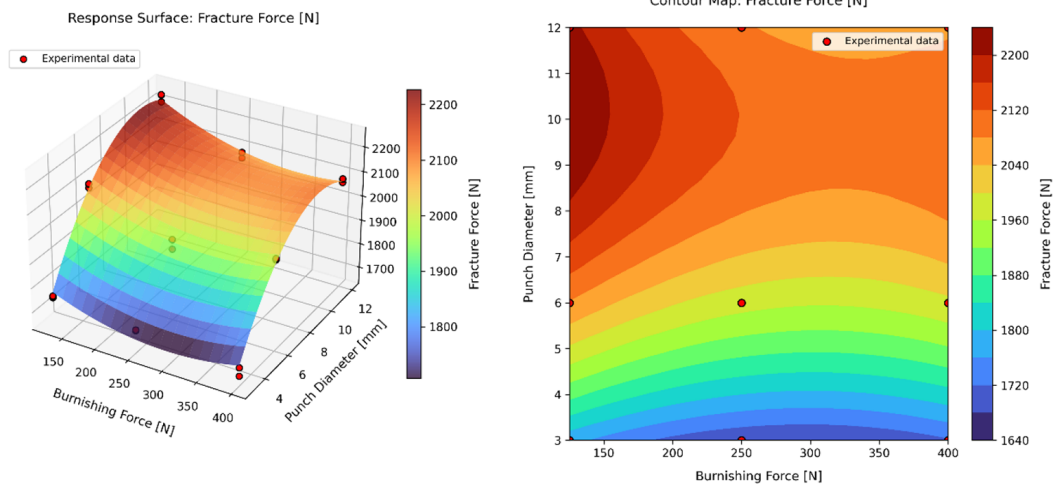
N) combined with larger punch diameters (12 mm) produce significantly different mechanical responses compared to treatments with higher forces (250 N, 400 N) and a small punch diameter (3 mm). This pattern highlights the critical interaction between low force and large tool diameter in defining the mechanical behavior of TRIP 440-Y steel. The response surfaces for both variables are shown in Figure 12.

Thickness reduction analysis

The thickness distribution around the hole edge was experimentally and numerically evaluated. Figure 13 illustrates the initial and deformed configurations, highlighting the regions of maximum thinning. Measurements were taken at four angular positions (0° , 90° , 180° , and 270°), corresponding to the rolling and transverse directions. In the simulations, thickness was recorded at the punch displacement corresponding to fracture initiation, as identified from the force–displacement curves. Experimentally, the final thickness distribution was obtained at the same angular

positions along the hole perimeter. Figure 14 shows the thickness distributions obtained with punch diameters of 3, 6, and 12 mm, respectively. In all cases, fracture occurred in the angular sector of maximum thinning (typically located at 90° and 270°). The 3 mm punch diameter produced the most severe local thinning, which explains the lower fracture displacements. Conversely, the 12 mm punch diameter resulted in a more gradual thickness reduction, enabling higher displacement before fracture. Burnishing effects were not uniform: high burnishing forces tended to accentuate strain localization, whereas moderate burnishing (125 N) promoted a slightly more homogeneous thickness profile, in agreement with the trend toward isotropy observed in *r-values*. Similarly, Figure 15 shows the numerical-experimental analysis now for TRIP440-Y steel, of the thickness distribution for specimens with the different burnishing conditions for punches of 3, 6 and 12 mm diameter, respectively. It is observed that a low burnishing force leads to less severe thickness reduction at 90° and 270° than the high burnishing forces.

a)



b)

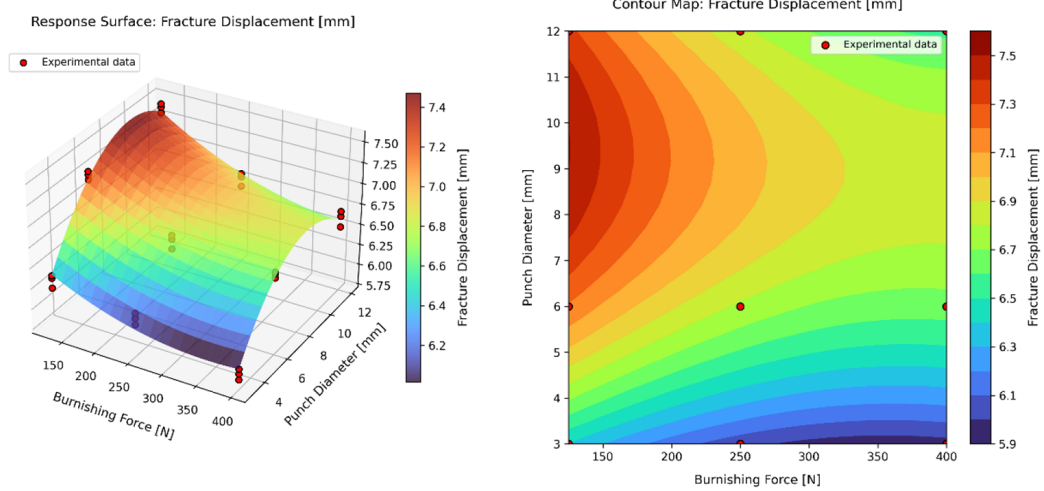


Figure 12. Response of the quadratic regression equation for TRIP440-Y:
a) fracture force, b) fracture displacement

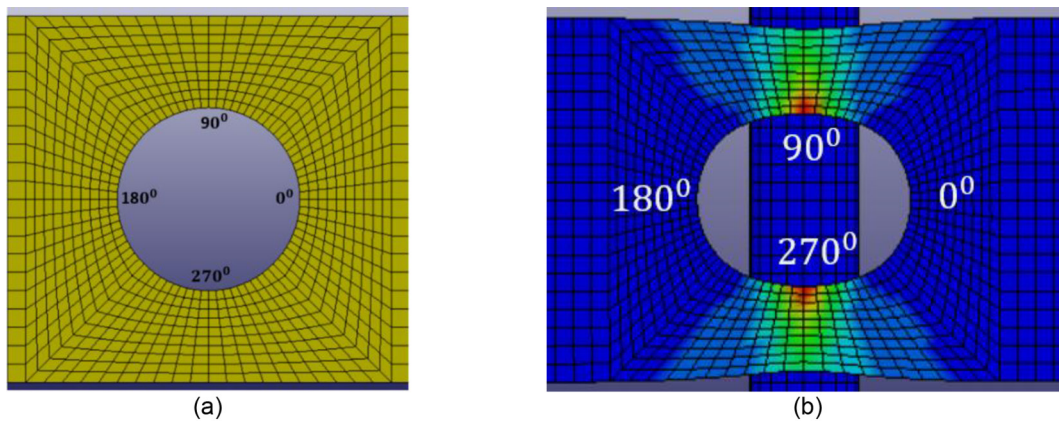


Figure 13. (a) Initial configuration of the specimen; (b) final configuration of the deformed specimen

From the presented results, slide burnishing modifies the thickness distribution along the hole perimeter by influencing the uniformity of plastic flow. In both steels, fracture consistently initiated at the angular sector of maximum

thinning (90°–270°). While excessive burnishing forces tended to accentuate local strain localization, moderate burnishing (≈ 125 N) promoted a more homogeneous thickness profile, consistent with the transition toward isotropy

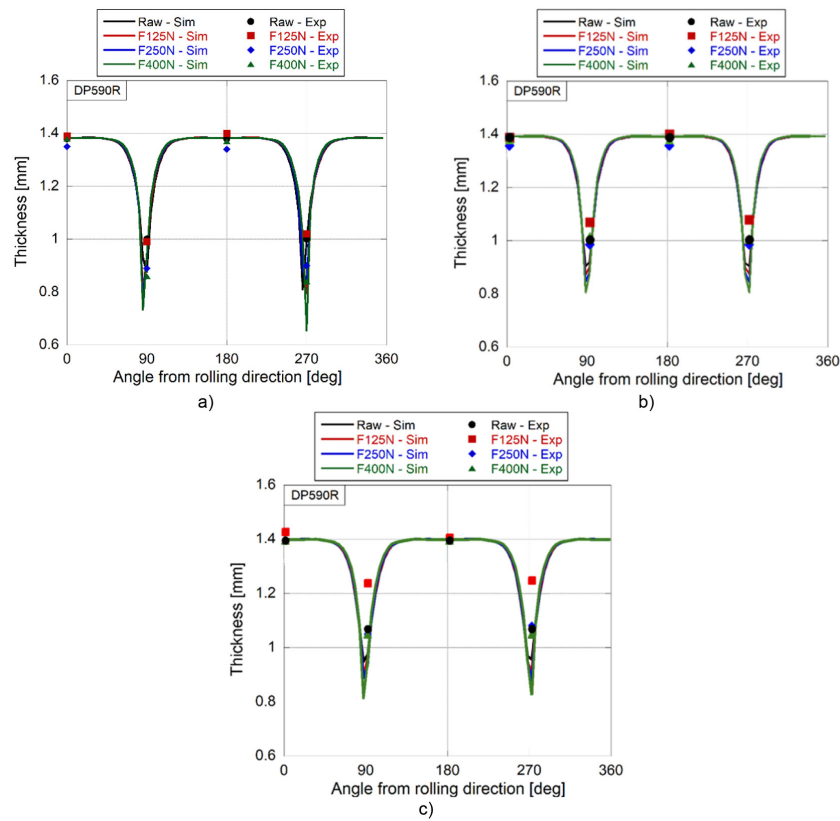


Figure 14. Thickness distribution vs. angle with respect to the rolling direction for DP590-R material for: a) 3 mm, b) 6 mm, and c) 12 mm punch diameter

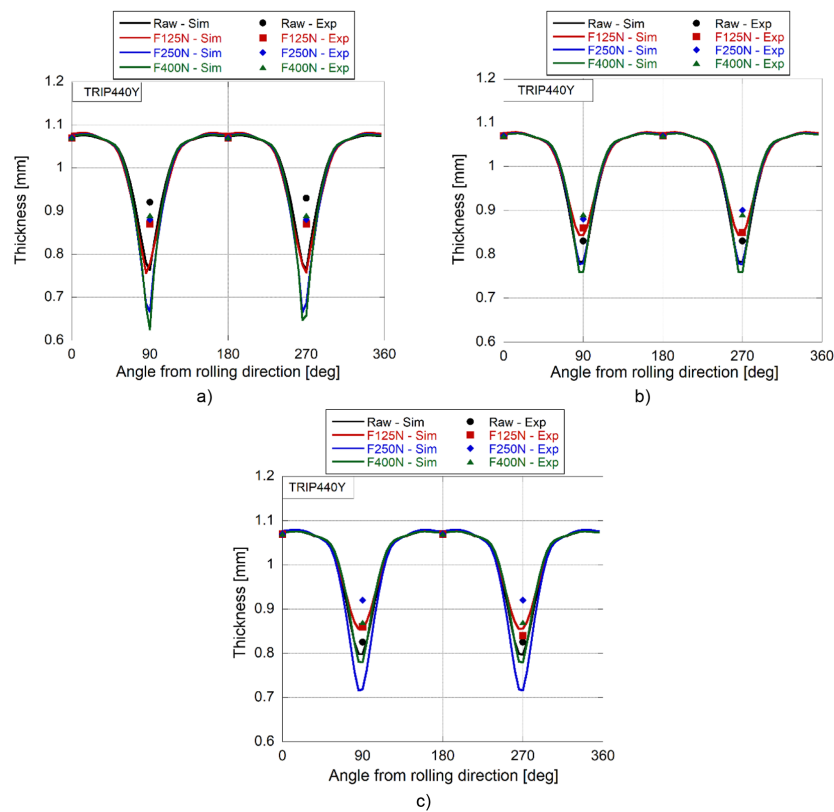


Figure 15. Thickness distribution vs. angle with respect to the rolling direction for TRIP440-Y material for: a) 3 mm, b) 6 mm, and c) 12 mm punch diameter

indicated by *r-values*. This effect is beneficial because it reduces the severity of localized thinning, delays crack initiation, and improves the correlation between experimental and numerical results. Therefore, burnishing does not universally increase fracture displacement, but when properly controlled, it contributes positively by mitigating anisotropy-driven thickness gradients and enhancing edge fracture resistance.

CONCLUSIONS

Based on the experimental and numerical results, it was found that slide burnishing modified the mechanical response of both AHSS grades by increasing the yield strength while producing only minor changes in ultimate tensile strength. For DP590-R, the yield strength increased from 369 MPa to 480 MPa (+30%), whereas for TRIP440-Y it increased from 438 MPa to 552 MPa (+26%). In both materials, the strain-hardening exponent decreased with increasing burnishing force, indicating a reduction in uniform deformation capacity. The effect of slide burnishing on stretch-bending performance was strongly dependent on the steel grade and punch diameter. For DP590-R, the highest fracture displacement was obtained with a 12 mm punch diameter and a burnishing force of 125 N, resulting in an improvement of approximately 10.5% compared with the unburnished condition. For TRIP440-Y, the maximum improvement was more limited, reaching approximately 4.4% for the 3 mm punch diameter with 125 N burnishing force.

Statistical analyses confirmed that punch diameter was the dominant parameter controlling fracture behavior in both steels. The best mechanical performance was consistently associated with the largest punch diameter (12 mm), while excessive burnishing forces (250–400 N) generally reduced fracture displacement due to increased strain localization and reduced strain-hardening capability.

Burnishing promoted a progressive reduction in material anisotropy, as evidenced by the convergence of the Lankford coefficients toward unity. For DP590-R, the *r-value* evolved from 0.93 to 1.01, while for TRIP440-Y it changed from 0.84 to 0.99. This isotropization effect contributed to a more uniform thickness distribution around the hole perimeter and reduced the severity of localized thinning during stretch-bending. Experimental measurements and LS-DYNA

simulations showed good agreement in predicting fracture location and thickness reduction profiles. In both steels, fracture consistently initiated in the region of maximum thinning, typically between 90° and 270° relative to the rolling direction. Moderate burnishing forces (125 N) produced the most homogeneous thickness distributions, whereas higher forces intensified local thinning. These results demonstrate that slide burnishing can enhance edge fracture resistance in AHSS sheets when applied under optimized conditions, particularly in combination with larger punch diameters.

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