Effect of anvil profile on cutting characteristics of polystyrene bar subjected to wedge indentation

Thepwachara RUCHIRABHA* and Shigeru NAGASAWA*
*Department of mechanical engineering, Nagaoka University of Technology
1603-1 Kamitomioka, Nagaoka, Niigata 940-2188, Japan
E-mail: snaga@mech.nagaokaut.ac.jp

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Abstract
This research was aiming to investigate cutting characteristics of Polystyrene (PS) 3 mm square bars subjected to indentation of a center bevel (blade angle of 42°) steel blade and stacked on an AL anvil (underlay). To reveal the effect of contact width of workpiece against the anvil on the sheared edge profile of workpiece, the contact width was varied from 1 mm to 40 mm during the cutting test. In this process, the cutting load response was investigated and the sheared edge profile of workpiece was observed by a microscope. To discuss the effect of the anvil width on the deformation behavior of workpiece, a finite element method (FEM) analysis with elasto-plastic plain strain model was developed using a crack generation rule. Through the experiment and FEM analysis, a proportion of wedge penetration flow and crack propagation of sheared workpiece was revealed with the contact width. The peak maximum cutting force, pre-crack position and the breaking position were characterized with the contact width. When the contact width was two times or much larger than the thickness of workpiece, the wedge penetration flow form was remarkably appeared, while the crack propagation based smooth flat form was appeared when the contact width was narrower than the thickness of workpiece. A bent-down or bent-up deformation of workpiece varied with the contact width and its sign change occurred when the contact width was about two times of the thickness of workpiece. When choosing the narrower contact width than the thickness of workpiece, an asymmetric crack propagation occurred due to the misalignment of blade position against the left and right side edges of anvil.

Keywords: Shear, Cutting, Elasto-plastic, FEM, Square bar, Anvil, Crack

1. Introduction

A cutting method with a wedge indentation into a sheet material on a counter plate is widely used in many packaging or printing industries for converting of paperboard, labels, laminated resin sheets, ductile metal film and other similar sheet materials (Hesse et al., 1963; Inaba, 1998). There are many kinds of engineering resins, such as polystyrene (PS), polycarbonate (PC), polypropylene (PP). When focusing on a manual precise cutting of molded parts, PS bars and PS ribbons are often used for making decorated plastic models. Furthermore, since the mechanical properties of PS are partially similar to that of natural nails (Baden., H. P., 1970), a cutting test of PS bars by using nail clipper seems to be useful for knowing the performance of nail clipper. A nipper or nail clipper is mechanically composed of a cutting wedge against a counter anvil (including a wedge). In this mechanism, several problems that affect the quality of surface smoothness and the cutting processability of materials are caused by the variation of blade tip condition such as crushing, abrasion of the wedge profile and friction against surface roughness during cutting works. The frictional adhesion between the blade and wedged workpiece and that of the workpiece and underlay affect the sheared profile of a resin sheet due to the sliding state based bent-up mode of the workpiece (Nagasawa et al., 2001). Also, a wedge based asymmetric combination of wedge and grooved counter plate under bending restrain affects the sheared profile of thick resin sheet (Nagasawa et al., 2011a). So far, the combination of a sharp wedge and an anvil which has a narrow width of its tip seems to affect sensitively the cutting profile of a PS bar or PS ribbon. When considering various nail clippers,
there are many geometrical parameters and cutting conditions such as the overlapped clearance between a wedge blade and counter anvil, an inclined angle of wedge and a 3 dimensional shape of cutting line. Although it was not so easy to prepare many kinds of real nail clippers by arranging some geometrical parameters, the authors (Ruchirabha et al. 2020) tried to compare the effect of wedge angle and the tip thickness (land) on the cutting characteristics of PS bar. However, due to hand-made restriction and 3 dimensional complicated structure, the effects of geometrical parameters on the cutting characteristics of nail clipper were not sufficiently revealed.

Ando et al.(1994) determine the friction characteristics of small contact surfaces, the friction coefficient between steel balls of 0.5 to 5 mm radii and block gauges were measured while varying the friction speed, normal load, sliding distance etc. This report showed that the friction coefficient under a low normal load increased with sliding distance.

To understand the deformation characteristics of a white-coated paperboard subjected to a center bevel blade indentation, several numerical and experimental researches have been reported in recent years. The cutting blade tip became trapezoidal (as a crushed form) when the cutting blade tip contacted to the counter plate, and the cutting tip shape affects the breaking characteristics and cutting load response of the paperboard (Nagasawa et al., 2004, 2002, 2001).

Nagasawa et al. (2011b) reported an FEM (finite element method) analysis of cutting deformation of Polycarbonate sheets subjected to wedge indentation. This report showed that when varying the friction coefficient of contact surface, the cutting load resistant was possibly changed and its dispersion was generated. Mitsomwang et al. (2016) used the elasto-plastic large deformation model based on an FEM Code for simulating the wedge indentation process of a lead alloy sheet until the final splitting stage when varying the apex angle of wedge blade. It revealed that the maximum cutting load response increased with the apex angle of blade and the simulated necking process matched to the experimental results. Chaijit et al. (2008) discussed the combination performance of underlay stiffness with respect to cutting characteristics of thin sheet against a wedge indentation, when changing the combination stiffness and yielding strength of the underlay. A combination of the edge shape of blade and the stiffness of underlay affected the load response and sheared profile of workpiece (Mitsomwang et al., 2012). Murayama et al. (2003, 2004) have reported the cutting mechanism of a 42° wedge indentation into a 4mm thickness aluminum sheet using a trapezoidal cutting blade imitated as a crushed tip. In that study, a couple of separation modes were estimated using the ratio of blade tip thickness \( w \) and the workpiece thickness \( t \). In case of \( w/t<0.23 \), the second mode necking occurred and the string-like burrs were generated beneath the blade tip. Chaijit et al. (2006) investigated the cutting processability of trapezoidal blade with a 4mm thickness aluminum sheet for \( w/t=0.23 \). Here, \( w \) was a tip thickness of blade and \( t \) was a thickness of workpiece. The separation limit of the aluminum sheet was confirmed to be \( w/t=0.28 \) and then, when \( w/t >0.3 \), the wedge surface was detached from the deformed aluminum sheet and the blade tip simply pushed the sheet in an upsetting mode without separation. Chaijit et al. (2009) used the finite element analysis for clarifying the sensitivity of crack propagation during wedge shearing process of an aluminum sheet when varying the tip thickness of cutting blade. The result showed that (i) the sensitivity of crack generation depends on the tip thickness, (ii) the necking behavior in the lower surface of aluminum sheet contributes to generate a crack at the lower wedged zone. It is sensitively faster than upper side.

As seen above, an FEM simulation is useful for knowing some parameters that are essential in cutting problems. In this research, a 2 dimensional (plain strain) FEM simulation of wedge indentation against an underlay of which the width was varied was considered. Here, the material property of PS bar was considered as an elasto-plastic model plus Cockroft-Latham damage model (MSC software, 2010c) based on a sort of plastic fracture energy.

In order to predict the cutting resistance and deformation behavior of a PS square bar, some peculiar mechanical properties of PS are necessary. PS materials can be distilled an oily liquid named styrol from the resin of Turkish sweet gum trees. The thermoplastic resin has generally the higher resistance of compressive deformation than that of tensile deformation (Lynwood, C. 2014). From the book of Miyari et al. (2014), PS materials are a brittle resin in a tensile mode, while that have ductile behavior in the compressive mode. PS materials are the most employed aromatic thermoplastic polymer due to the hardness, stiffness, and chemical stability over a wide pH range, and also that are a brilliantly transparent synthetic resin produced by polymerization of styrene. PS materials have a wide range of application such as a food packaging product and an insulator in buildings due to some advantages as the versatility, dimensional stability and low cost (Frediani et al. 2014).

Ruchirabha et al. (2019) designed a rotational-linked fixture of a nipper handle which was used for cutting a PS bar. In this development of fixture, three kinds of nippers were investigated. The first two nippers were the big nipper (the upper/lower asymmetric two-line wedge composed of upper apex angle \( \alpha=90–72° \), lower apex angle \( \alpha'=85–72° \)), the medium nipper (the upper/lower asymmetric two-line wedge composed of upper apex angle \( \alpha=49–55° \), lower apex angle \( \alpha'=49–45° \)), and the small nipper (the upper/lower asymmetric two-line wedge composed of upper apex angle \( \alpha=49–55° \), lower apex angle \( \alpha'=49–45° \)).
α=42°–50°) and the third was the small nipper (13° single apex, side wedge against a narrow-tip anvil). Through this development of fixture, it was clarified that when using the big and medium nippers, there were unstable cracks and dynamic large force drop (as a breaking down) during the cutting process after passing the necked stage, while the small nipper had the lowest peak force in the early stage and any complicated cracks and large force drop were not detected, owing that a small wedge angle reduced the lower necking and the frictional restriction of anvil contact was not negligible.

However, a wedge cut of PS material seems to be not yet discussed sufficiently when varying the width of underlay (anvil) for cutting a 2 dimensional bar or ribbon. In this paper, a symmetric center bevel (wedge) blade which had a tip thickness \( w = 10 \mu m \) and an apex angle \( \alpha = 42° \) was indented to a 3mm square polystyrene (PS) bar which was stacked on an underlay, and the deformation behavior of the PS specimen was analyzed experimentally and numerically in order to reveal the fundamental load response, deformation behavior of PS bar. Through discussion of the 2 dimensional anvil width variation, the effects of overlapped land of cutting tool (such as nail clipper or nipper) on the cutting characteristics of PS bar was revealed. Also, comparing the experiment and simulation at the peak maximum load point, the crack initiation and its propagation of PS bar were revealed with respect to the bent up state of workpiece and frictional restriction.

2. Experimental analysis condition
2.1 Experimental method and specimens

A 3-mm-square polystyrene (PS) specimen, TAMIYA Item 70130-360, which had a longitudinal length of \( L = 40 \text{mm} \), a lateral width of \( b = 3 \text{mm} \) and a thickness (height) of \( t = 3 \text{mm} \) as shown in Fig. 1, was used for the cutting test. All specimens were sufficiently cleaned by ethanol before cutting. Figure 2 shows a schematic of experiment apparatus and configurations of specimen. The cutting test was carried out with 5 times for each condition. On the experimental apparatus (a compressive testing equipment), the upper crosshead had a cutting blade mounted on a load cell with the maximum load \( 10 \text{kN} \). The attitude of the cutting blade was vertical and across the specimen. A specimen of PS square bar was stacked on the underlay for performing the cutting test. Here, five kinds of underlay were prepared. The first type was a 0.4-mm-thickness square AL plate (Hikari AZ551), which had a lateral length (in the y-axis direction) of \( L_{\text{AL plate}} = 80 \text{mm} \) and a width (in the x-axis direction) of \( b_{\text{AL plate}} = 80 \text{mm} \). This was a full size plate which was fixed on the lower crosshead, as shown in Fig. 2(a).

In this case, the contact width of specimen against the underlay (AL plate) \( b \) was 40 mm. The second or third type was a square bar of AL (AH3031) anvil that had the height (thickness) \( t_{\text{U}} = 30 \text{mm} \) in the z-axis direction, a lateral length
(in the y-axis direction) of $L_{\text{AL anvil}} = 30$ mm and a width (in the x-axis direction) of $b_{\text{AL anvil}} = 2, 3, 6, 10$ and 15 mm, respectively, as shown in Fig. 2(b). Here, the contact width of specimen against the underlay (AL anvil) $b$ was equal to $b_{\text{AL anvil}}$. The AL plate had a surface roughness of $R_a=9.86 \mu m$ ($9.04 \sim 10.56 \mu m$) and its hardness was 75 (73-76) VHN, while the AL anvil blocks of $b=2, 3$ and 6 mm had a surface roughness of $R_a=8.7 \mu m$ ($8.1 \sim 9.8 \mu m$). The AL anvil blocks of $b=10$ and 15 mm had a surface roughness of $R_a=9.1$ ($7.7 \sim 10.3 \mu m$) and 8.4 ($7.3 \sim 9.8 \mu m$), respectively. The hardness of anvil block was 73 (72-74) VHN.

The wedge blade made of SK5 high carbon steel, had a lateral length of $L_{\text{blade}}=70$ mm, a height of $H=23.5$ mm, a thickness of $S=0.71$ mm, and an initial tip thickness of $w=10 \mu m$ in average as shown in Fig. 3. The bevel face of the wedge blade had a surface roughness of $R_a=7.06 \mu m$ ($6.6 \sim 7.8 \mu m$), and its apex angle $\alpha = 42^\circ$. The upper crosshead moved downward with a velocity of $V=0.1 \text{ mm/s}$.

![Fig. 3 Specification of wedge blade and blade position against workpiece. (a) General view of blade; (b) Relation of blade displacement and indentation depth with specimen.](image)

2.2 Mechanical properties of materials

The tensile stress-strain relationship of a 3-mm-square PS bar was examined in the longitudinal direction of bar with the feed velocity $V=5 \text{ mm/s}$. Here, the bar specimen, the longitudinal length of which was chosen as 110 mm, was prepared and the both end sides of 30 mm were clamped by upper and lower holders, while the central part of 50 mm length was evaluated as an elongated zone. The mechanical properties of the 3 mm square PS specimen were shown in Table 1. Figure 4 shows a representative stress-strain diagram on the experimental tensile testing of a PS specimen. This measuring condition of PS square bar was reported by Ruchirabha et al. (2019). Here, $\varepsilon_B$ was described with the logarithmic strain and $\sigma$ was calculated as $\sigma=F_{\text{INPL}}/(1+\varepsilon)/A$ when putting $A$ as a nominal section area $=3x3=9 \text{ mm}^2$, $F_{\text{INPL}}$ as a tensile force N, and $\varepsilon$ as the nominal strain that equal to $(l_f-l_i)/l_i$ where $l_f$ = final length of specimen and $l_i$ = initial length of specimen. When considering an FEM model, the breaking point $(\sigma_B, \varepsilon_B)$ was initially expanded up to $\sigma=\sigma_B$ (for $\varepsilon_B>1.1$) without any cracking. Secondly, a damage model based on a plastic fracture energy was considered so as to fit the experimental breakage (See the section 3).

![Fig. 4 Representative stress-strain diagram on the tensile testing of PS specimen.](image)
Table 1 In-plane tensile mechanical properties of polystyrene (PS) in longitudinal direction (the strain rate: 0.1 s\(^{-1}\) with the span of 50 mm).

| Symbol | Thickness (Height) \(t_s\) /mm | Young's modulus \(E\) /GPa | Yield strength \(\sigma_Y\) /MPa | Tensile strength \(\sigma_B\) /MPa | Breaking true strain \(\varepsilon_B\) |
|--------|-------------------------------|--------------------------|-----------------------------|-----------------------------|------------------------|
| Average (Max.-Min.) | 3.0 | 1.88 (1.80–1.97) | 30.18 (28.05–35.73) | 41.65 (39.83–43.60) | 0.32 (0.28–0.34) |

Table 2 Out of plane compressive mechanical properties of polystyrene (PS) in the cutting direction (the \(y\) direction). The strain rate: 0.33 s\(^{-1}\).

| Symbol | Thickness \(t\) /mm | Young's modulus \(E_C\) /GPa | Yield strength \(\sigma_{Y_C}\) /MPa | Breaking strain \(\varepsilon_B\) |
|--------|-------------------|---------------------------|-----------------------------|------------------------|
| Compressive mode | 3.0 | 5.04 | 85.63 | |

Table 3 In-plane tensile mechanical properties of 0.4mm thickness plate of A5051 in longitudinal direction (the strain rate: 0.001s\(^{-1}\) with the span of 80 mm).

| Tensile mode | Young’s modulus \(E\) /GPa | Yield strength \(\sigma_Y\) /MPa | Tensile strength \(\sigma_B\) /MPa | Breaking strain \(\varepsilon_B\) |
|--------------|-----------------|-----------------------------|-----------------------------|------------------------|
| A5051 Counter plate | 42.01 (37.75–44.61) | 88.44 (86.47–91.45) | 89.54 (87.73–92.56) | 0.013 (0.011–0.014) |

The compressive test of 3mm square polystyrene bars was measured for \(\varepsilon \approx 0.65\) with \(V=1\)mm/s. Here, the \(\varepsilon\) is the true strain in the thickness direction (TD). The compressive Young’s modulus \(E_C\) in the \(y\) direction (cutting direction) and the yield strength \(\sigma_{Y_C}\) in the \(y\) direction (cutting direction) were shown in Table 2. It was found that the Young’s modulus and Yield strength of the compressive test were clearly higher than that from the tensile testing. Here, it was noted that this compressive \(\sigma_{Y_C}\) included a sort of frictional restriction.

The in-plane tensile mechanical properties of AL plate (in the \(x\)-axis direction of cutting test apparatus) were shown in Table 3 (based on JIS K 7125). The applied pushing force \(F\) was measured by the load cell. The blade displacement of the cutting blade to the specimen \(c\) was measured as the upper crosshead displacement, and \(f=\frac{F}{\text{lateral length of specimen (3mm)}\ N/mm}\) was the line force applied in vertical to the cutting blade. Figure 3(b) shows the relationship between the blade displacement \(c\) and the indentation depth \(d\), and other sizes. This relation of \(d\) and \(c\) was varied with the size of underlay, and explained from a preliminary experiment, as shown in Eq. (1). Namely, \(c\) was composed of \(d\) plus a sort of spring effect derived from elastic behavior of underlay and connected parts (Nagasawa et al., 2010). Here, \(k_y\) is the equivalent spring constant of the experimental apparatus. In this work, when choosing the contact width (in the \(x\)-axis direction) of \(b=2, 3, 6, 10, 15\) and 40mm, the spring constant \(k_y\) was measured as 239.23, 218.82, 265.96, 745.16 and 724.64, respectively.

\[
d/t_s = c/t_s - f/k_y \tag{1}
\]

2.3 Friction coefficient

To ensure non-lubricated contact between the specimen and the wedge blade, all the tool surfaces were washed by ethanol before carrying the indentation test. The three kinds of friction coefficient \(\mu_C, \mu_U\) and \(\mu_P\) were experimentally measured by the horizontal method based on JIS-P8147. The \(\mu_C\) between the cutting blade and the PS square bar, the \(\mu_U\) between the PS square bar and the underlay and the \(\mu_P\) between the underlay and the lower base steel block were shown in Table 4. Here, since it was difficult to use the surface of the lower base in direct due to its big size, a counter plate SUS630 was alternatively examined with the horizontal method under an applied pressure of 4.9 kPa.
Table 4  Friction coefficients of underlay (at 4.9 kPa) Average (Maximum-Minimum measured).

| Contact target       | Steel blade | AL plate or anvil | Lower base of steel block |
|----------------------|-------------|-------------------|---------------------------|
| PS square bar AL     | 0.31 (0.31-0.33) | 0.77 (0.52-0.86) | 0.61 (0.49-0.64) |

2.4 Observation of shear edge parameters

From sectional views of experimental sheared profile, the primary inclined angle $\beta$, the secondary inclined angle $\beta'$, the $1^{\text{st}}$ height $t_1$, the $2^{\text{nd}}$ height $t_2$, the fracture zone height $t_3$ and the elevation (bottom-up) height $t_4$ were observed using a microscope camera as shown in Fig. 5. Over here, the bent-down angle $\theta_B$ was evaluated as a gradient of distance $4t_S$ on the upper surface of the workpiece, as shown in Fig. 6.

![Fig. 5 Sectional views of sheared profile model. (a) Cutting off using a large contact width of specimen against the underlay $b$ of underlay, as $b \geq$ 10mm; (b) Cutting off using a narrow contact width of specimen against the underlay $b$ of underlay, as $b \leq$ 6mm.](image)

![Fig. 6 Bent-down angle model during a cutting process.](image)

3. FEM simulation

A general purpose finite element code, MSC.MARC 2015.0.0, was employed for simulating the indentation of wedge blade into the PS square bar stacked on an AL underlay (counter plate and anvils). An elasto-plastic non-linear finite element analysis using the updated Lagrange procedure and a large finite strain model was considered. The two-dimensional symmetric model was constructed in a half-length of the workpiece and the underlay by using the four-node plain strain quadrilateral element type, as shown in Fig. 7. The workpiece and the underlay were assumed to be deformable, while the wedge blade and the lower base block were assumed to be rigid. Regarding the blade position, the indentation depth $d$ was considered here for comparing with the experimental results. A half of length of the workpiece $L_S/2 = 20$mm and a half of width of the counter plate underlay $b_{\text{AL plate}}/2 = 40$mm were used for the first model. Other anvil models had the same length of the workpiece $L_S/2 = 20$mm and a half of anvil width $b_{\text{AL anvil}}/2 = 0.5, 1, 1.5, 2.5, 3, 3.5, 5, 10, 15$ and $17.5$mm.

![Fig. 7 Half symmetric mesh models with respect to initial profile of workpiece and specific underlay. (a) The underlay was considered as a counter plate; (b) The underlay was considered as 6mm width anvil.](image)
Since the model was based on the 2 dimensional plane strain, the third directional (in the longitudinal direction of the wedge blade) deformation was fixed. In order to obtain accurate simulation results, the divided elements of the workpiece and the underlay were constructed with fine meshes at the central zone of 4 mm and 8 mm, respectively. The minimum side length of elements with the workpiece and underlay were initially 20 and 50 μm, respectively. Some large (long in the lateral) elements were used in the specified range (over 4mm in the specimen, over 8mm (light blue color in Fig. 7) in the full size aluminum counter plate) far from the wedge blade. Those fine and large subdivided areas (two groups of meshes) were attached with each other by using the glue contact function. During the wedge blade indentation, a few of elements are largely deformed beneath the blade tip. Consequently, the calculation tended to be fault due to crushing of some elements. Therefore, the center-side elements of the workpiece were automatically re-generated in the deformed region using the automatic re-meshing function with ADVANCING FRONT QUAD (MSC software, 2010a) in order to overcome the crushing of any elements.

The coulomb tan^{-1} friction model (MSC software, 2010b) with the relative-slipping velocity threshold = 0.01 was assumed for each contact interface. Coefficients of friction were assumed in Table 4. The material properties of workpiece (PS materials) was assumed to be isotropic elasto-plastic as shown in Table 1 and Fig. 4. Since the experimental cutting velocity of blade was fixed as \( V = 0.1 \text{ mm·s}^{-1} \) and there were experimentally a peak maximum load point plus a cracked separation of sheared zone, the FEM simulation model of cutting resistance was considered as a plastic deformation plus a damage (plastic failure) model.

The material properties of the underlay were assumed to be isotropic-elastic as shown in Table 3. The plastic behavior of specimen was evaluated from a uni-axial tensile test (Nagasawa et al., 2011a).

In order to discuss the effect of width of counter plate underlay on the sheared profile of wedged workpiece, the material properties and geometrical conditions for simulation was shown in Table 5. The length of specimen \( L_s \) and a width of underlay \( b_{AL\text{ plate}} \) was 40 and 80 mm, in case of AL counter plate. As for anvil cases, the width of anvil underlay \( b_{AL\text{ anvil}} \) was varied from 1 to 35 mm (less than \( L_s \)). So far, the contact width of specimen against the underlay \( b \) was considered for simulation in a range of 1–40mm as shown in Table 6. In the following, the contact width of PS specimen (workpiece) against the underlay \( b \) was mainly used for comparing the effect of anvil width on the cutting profile of workpiece. The effect of variation of \( b \) on the experimental cutting load response was discussed in the section 3.1 and 3.2. As for the effect of anvil width on the cutting response, the experimental comparison of \( b/2= 1.0, 1.5, 3, 5, 7.5 \) and 40mm was investigated, firstly. All the values of \( b \) described in Table 6, were discussed in the section 3.3 and 3.4.

In order to investigate the deformation and sheared profile of the simulated wedged workpiece, the profile parameters defined in Fig. 5 were evaluated from the simulation. As for the simulated \( \theta_b \), the measurement method was similar to Fig. 6 but it was measured at the lower-side of workpiece.

### Table 5  Material properties and geometrical conditions for simulation in case of AL counter plate.

| Material type | PS workpiece | AL underlay |
|---------------|--------------|-------------|
| Young’s modulus \( E_s, E_u/GPa \) | Elasto-plastic without linear work hardening | Elastic |
| Thickness \( t_s, t_u/mm \) | \( E_s = 1.88 \) | \( E_u = 42.01 \) |
| A half length of target | \( L_s/2 = 20 \) | \( b_{AL\text{ plate}}/2=40 \) |
| Bevel apex angle /°, Tip thickness /μm | Friction coefficient: | \( \mu_c = 0.31, \mu_a = 0.77 \) and \( \mu_b = 0.61 \) |
| | \( \alpha = 42, w = 10 \) |

### Table 6  Relationship between the width of underlay (\( b_{AL\text{ plate}} \) or \( b_{AL\text{ anvil}} \)) and the initial contact width of PS specimen against the underlay \( b \) used in simulation.

| A width of underlay \( b_{AL} / \text{mm} \) | Initial contact width of PS specimen against the underlay \( b / \text{mm} \) |
|------------------------------------------|---------------------------------|
| \( b_{AL\text{ anvil}} = 1, 2, 3, 5, 6, 7, 10, 20, 30, 35 \) | \( b_{AL\text{ anvil}} \) |
| \( b_{AL\text{ plate}} = 80 \) | \( L_s=40 \) |
In order to investigate the fundamental failure and sensitivity of crack propagation on sheared surfaces during the wedge indentation process in the simulation, a fracture criterion and a certain critical fracture value were considered. In this study, the Cockroft-Latham damage model was used as the fracture criterion. The general Cockroft-Latham criterion equation was considered as Eq. (2). Stress and strain fields at a small region in the cracked zone were calculated and compared with the fracture threshold factor \( C \). Namely, any element had broken when the value of right-hand side of Eq. (2) reached 1.0 \( (I_C = 1) \) (MSC software, 2010c).

\[
I_C = \frac{1}{\varepsilon_f} \int \frac{\sigma_{max}}{\bar{\sigma}} \sigma d\varepsilon
\]  

Here, \( \varepsilon_f \) was the equivalent fracture strain, \( \sigma_{max} \) was the maximum principal tensile stress, \( \bar{\sigma} \) was the equivalent stress, \( \bar{\varepsilon} \) was the equivalent strain and \( C \) was the critical fracture threshold. Since the field values \( \sigma_{max} \) and \( \bar{\sigma} \) were gotten from the simulation, the value of \( C \) was examined to detect the occurrence of fracture (crack), compared with the experiment.

4. Results and Discussion

4.1 Results of experiment

4.1.1 Cutting load response and deformation of specimen

After setting a PS square bar on the AL counter plate that had a contact width of PS specimen against the underlay \( b = 40 \text{mm} \), and/or mounted on the AL anvil block that had a contact width \( b = 2, 3, 6, 10 \) and \( 15 \text{mm} \), a \( 42^\circ \) wedge blade was indented to the PS specimen until the workpiece was completely cut off. Figure 8(a) shows the experimental relationship between the cutting line force \( f = (F/b_S) \text{N/mm} \) and the indentation depth of wedge blade \( d/t_S \) when choosing six kinds of underlays: an AL counter plate \( (b = 40 \text{mm}) \), AL anvil blocks of \( b = 2, 3, 6, 10 \) and \( 15 \text{mm} \) contact width. All the parameters described in Fig. 8(a) were shown in Table 7.

The peak maximum line force \( f_{max} \), the pre-crack position \( d_{pre-crack}/t_S \) and the breaking down position \( d_{break}/t_S \) were picked up from Fig. 8(a) and its results were plotted in Fig. 8(b) with respect to the contact width \( b \). The penetrated, pushed and necked stages are explained later when watching Fig. 8(b).

\[
d_{pre-crack}/t_S = d_{spc}/t_S - D_{pc} \exp\left(-\frac{b}{\Lambda_{pc}}\right) \quad \text{with} \quad d_{spc}/t_S= 0.85, \quad D_{pc}=1.01 \text{mm}, \quad \Lambda_{pc}=4.55 \text{mm} \]  
\[
d_{break}/t_S = d_{sbr}/t_S - D_{br} \exp\left(-\frac{b}{\Lambda_{br}}\right) \quad \text{with} \quad d_{sbr}/t_S= 0.92, \quad D_{br}=0.32 \text{mm}, \quad \Lambda_{br}=4.65 \text{mm} \]  
\[
f_{max} = f_{smx} - D_{fmx} \exp\left(-\frac{b}{\Lambda_{fmx}}\right) \quad \text{with} \quad f_{smx}=117.08 \text{N/mm}, \quad D_{fmx}=106.07 \text{N/mm}, \quad \Lambda_{fmx}=3.07 \text{mm} \]  

Since change tendencies of \( d_{pre-crack} \) and \( d_{break} \) were non-linearly increasing with \( b \) for \( 3 \leq b < 5 \text{mm} \), while they were appeared to be saturated for \( b > 15 \text{mm} \), the approximations of Eq. (3) and Eq. (4) were estimated. Here, constant terms \( d_{spc}/t_S, d_{sbr}/t_S \) were assumed to be the average of 2 data at \( b = 10 \) and \( 15 \text{mm} \), The RMSE (the root mean square error) of Eq. (3) and Eq. (4) were 7.9% and 9.0% with the constant term, respectively. Similarly, Eq. (5) was derived with the peak maximum force. The RMSE of Eq. (5) was 2.0% with the constant term.

\[
d_{pre-crack}/t_S = d_{spc}/t_S - D_{pc} \exp\left(-\frac{b}{\Lambda_{pc}}\right) \quad \text{with} \quad d_{spc}/t_S= 0.85, \quad D_{pc}=1.01 \text{mm}, \quad \Lambda_{pc}=4.55 \text{mm} \]  
\[
d_{break}/t_S = d_{sbr}/t_S - D_{br} \exp\left(-\frac{b}{\Lambda_{br}}\right) \quad \text{with} \quad d_{sbr}/t_S= 0.92, \quad D_{br}=0.32 \text{mm}, \quad \Lambda_{br}=4.65 \text{mm} \]  
\[
f_{max} = f_{smx} - D_{fmx} \exp\left(-\frac{b}{\Lambda_{fmx}}\right) \quad \text{with} \quad f_{smx}=117.08 \text{N/mm}, \quad D_{fmx}=106.07 \text{N/mm}, \quad \Lambda_{fmx}=3.07 \text{mm} \]
Table 7  Classification of cutting load response \( f_d/t_S \) in Fig. 8(a).

| Classification | Stage (1-6) | Description |
|----------------|-------------|-------------|
| Common         | \( S_{1-all} \) | The pushed stage. |
| Anvil 2 mm     | \( S_{2-2} \) | The penetrated stage. |
|                | \( S_{3-2} \) | The necked stage and pre-crack |
|                | \( S_{4-2} \) | \( S_{5-2} \) |
|                | \( S_{5-2} \) | The crack propagated stage |
| Anvil 3 mm     | \( S_{2-3} \) | The penetrated stage. |
|                | \( S_{3-3} \) | The necked stage and pre-crack |
|                | \( S_{4-3} \) | \( S_{5-3} \) |
|                | \( S_{5-3} \) | The crack propagated stage |
| Anvil 6 mm     | \( S_{2-6} \) | The penetrated stage. |
|                | \( S_{3-6} \) | The necked stage and pre-crack |
|                | \( S_{4-6} \) | \( S_{5-6} \) |
|                | \( S_{5-6} \) | The crack propagated stage |
| Anvil 10 mm    | \( S_{2-10} \) | The penetrated stage |
|                | \( S_{3-10} \) | The necked (lift up) stage |
|                | \( S_{4-10} \) | The pre-crack occurred. |
|                | \( S_{5-10} \) | The crack propagated stage. |
|                | \( S_{2-15} \) | The penetrated stage |
|                | \( S_{3-15} \) | The necked (lift up) stage |
|                | \( S_{4-15} \) | The pre-crack occurred. |
|                | \( S_{5-15} \) | The crack propagated stage. |
|                | \( S_{2-40} \) | The penetrated stage |
|                | \( S_{3-40} \) | The necked (lift up) stage |
|                | \( S_{4-40} \) | The pre-crack occurred. |
| AL plate       | \( S_{2-40} \) | The necked (lift up) stage |
|                | \( S_{3-40} \) | The crack propagated stage. |
|                | \( S_{4-40} \) | The pre-crack occurred. |
|                | \( S_{5-40} \) | The crack propagated stage. |
| Common         | \( S_{6-all} \) | Force drop and cutting off. |

Seeing Fig. 8(a) and (b), it was found that the cutting load response remarkably depended on the contact width \( b \). The maximum peak of force was almost saturated for \( b > 10 \text{ mm} \). The pre-crack position \( d_{\text{pre-crack}}/t_S \) and breaking down position \( d_{\text{break}}/t_S \) basically increased for \( b < 15 \text{ mm} \), although the case of \( b=40 \text{ mm} \) had a little reduced level. These differences of \( d_{\text{pre-crack}}/t_S \) and \( d_{\text{break}}/t_S \) between \( b=15 \text{ mm} \) and \( b=40 \text{ mm} \) seemed to be caused from the frictional resistance, surface roughness of underlay or the contact length of workpiece against the length of underlay. From Fig. 8(b), it was found that \( d_{\text{pre-crack}}/t_S \) was larger than 1.5 for \( b \leq 6 \text{ mm} \) while it was less than 1.09 for \( b \geq 10 \text{ mm} \). As a tendency, as shown in Eq. (4) and Eq. (5), the pre-crack position (depth) \( d_{\text{pre-crack}}/t_S \) and breaking down position (depth) \( d_{\text{break}}/t_S \) increased with the contact width of PS specimen against the underlay \( b \).

Seeing the case of \( b=2 \text{ mm} \), although \( d_{\text{pre-crack}}/t_S \) was relatively larger than the case of \( b=3-6 \text{ mm} \), this difference was characterized with the necking behavior and the force dropping response. When the force drop after passing the maximum peak load was watched, the position of force drop in the case of \( b=2 \text{ mm} \) was close and a little smaller than that of \( b=3 \text{ mm} \). Therefore, the occurrence of necking was stable and similar when \( b=2-3 \text{ mm} \). However, the crack propagation with the contact width \( b \) seems to be remarkably changed in this condition (\( b=2-3 \text{ mm} \)).
Figure 9 shows the intermediate deformation states of PS specimen in each stage referred from Fig. 8(a) and (b). In this figure, four stages (a)-(d) as the penetrate, pre-crack, crack propagation, and breaking down stages were observed by a video camera. In the crack propagated stage, a certain small necking or lifting up of the bottom surface were detected. In the early stage as Fig. 9(a), $0.3 < \frac{d}{t_S} < 0.5$ was recognized as the penetrated stage and $\frac{d}{t_S} < 0.2$ was the pushed stage (Murayama et al., 2003, 2004) of thickness plastic body before necking (Hill, R., 1953), while the pushed stage was a shallow-wedged deformation against a semi-infinite plastic body (Grunzweig, et al., 1954). Although the peak maximum force point ($0.3 < \frac{d_{\text{peak}}}{t_S} < 0.4$) seemed to reach the necked stage, the depth position of pre-crack occurrence ($\frac{d}{t_S} = 0.3~0.62$) tended to be deeper than $\frac{d_{\text{peak}}}{t_S}$. Figure 10 shows the relationship between the bent-down angle $\theta_B$ and the indentation depth of wedge blade $d/t_S$. Here, the average of 5 samples were plotted for each case of $b$.

![Diagram of deformation states](image)

(a) Penetrated stage  (b) Pre-crack occurred  (c) Crack propagated  (d) Breaking down stage

Figure 10 Relationship between bent-down angle against indentation depth of wedge blade $d/t_S$. Average of five cracking and/or simples were plotted for each case.

The following features were detected from Fig. 8(a), 8(b), 9 and Fig. 10.

(1) Compared with the counter plate underlay, the depth position of pre-crack occurrence in the cases of $b=2$, 3 or 6mm was 50%, 30% and 15% shallower, respectively. The depth position of pre-crack occurrence in the cases of $b=10$, 15mm was almost the same as that of the counter plate.
(2) Compared with the counter plate underlay, the breaking down position at \( b = 3 \) mm and 6\( \text{mm} \) was 11% and 14% shallower, respectively. However, in case of \( b = 2 \) mm the breaking down position was 9% deeper. There seems to be a transition mode of crack propagation between \( b = 2 \) mm and \( b = 3 \) mm.

(3) In the early stage \( d/t < 0.2 \), the load response was almost similar tendency as the pushed stage. In the range of 0.2 \(< d/t < 0.4 \), the cutting line force monotonically increased as the penetrated stage with \( b \).

(4) The reducing gradient of \( - \partial \Delta d/t \partial (d/t) \) was measured for a duration of \( \Delta d/t \) =0.1 after passing through the \( d/t \) of \( f_{\text{max}} \); \( d/t \approx 0.28-0.38 \) (\( b = 2 \) mm), 0.35-0.45 (\( b = 3 \) mm), 0.45-0.55 (\( b = 6 \) mm), 0.55-0.65 (\( b = 10, 15 \) and 40mm). The reducing gradient was gradually decreased for \( b < 15 \text{mm} \) as shown in Fig. 11. It seems to be caused from a variation of frictional resistance with the anvil contact width. In the case of \( b = 40 \text{mm} \), although the gradient was similar to that of \( b = 10-15 \text{mm} \), the gradient was a bit larger. It seems to be caused from an overhang length of workpiece against the underlay. Obviously, the peak maximum load response of \( b = 40 \text{mm} \) was remarkably different from others in Fig. 8(a).

(5) When using the initial contact width of PS specimen against the anvil \( b = 2 \) mm and 3 \( \text{mm} \), the workpiece started to be bent down at \( d/t \approx 0.15 \) and the pre-crack generated at \( d/t \approx 0.3 \). The bent-down deformation wasn’t symmetric due to a small deviation of blade position against the both side edges of anvil, or due to a small asymmetric crack propagation as shown in Fig. 9. This small anvil width \( b \) seemed to make a pre-crack easy to occur at the wedged zone and to propagate the crack. After propagating the crack (\( d/t > 0.5 \)), the maximum bent-down angle \( \theta_B \) reached \( 24.0^\circ(19.4^\circ-26.3^\circ) \) at \( d/t \approx 0.74 \) and \( 16.3^\circ(13.2^\circ-16.3^\circ) \) at \( d/t \approx 0.62 \), when choosing \( b = 2 \) mm and 3 \( \text{mm} \), respectively, as shown in Fig. 10. Here, the lift-up distance at the center position was 0.22\( \text{mm} \) and 0.11\( \text{mm} \), when choosing \( b = 2 \) mm and 3 \( \text{mm} \), respectively (see \( d/t \approx 0.55-0.65, b = 2-3 \text{mm} \) in Fig. 9).

In order to pursue the force drop phenomena in case of \( b = 2 \text{mm} \) in Fig. 8, additional video photographs at \( d/t \approx 0.53, 0.65 \) and 0.89 were shown in Fig. 12. It was found that the crack was largely propagated until \( d/t \approx 0.53 \), and then the propagation stopped due to the detaching of cracked surfaces from the blade. Such a crack length (detaching from a blade) appeared to be determined by energy dissipation and the decrease of stress level. Until the second starting of crack propagation, the blade moved downward. Seeing Fig. 12 and the load response of \( b = 2 \text{mm} \) in Fig. 8(a), the blade tip touched the bottom of notched (cracked) surface at \( d/t \approx 0.65-0.82 \). Finally, the specimen was cut off without crack propagation at \( d/t \approx 0.89 \) as shown in Fig. 9(d).

![Fig. 12 Details of deformation states of PS specimen at d/t=0.53, 0.65 and 0.82 when b=2mm. (a) Stop of crack propagation; (b) Blade movement downward without touching and (c) Second kissing of blade tip against bottom surface of notched specimen.](image-url)

(6) When using the 6 mm width anvil, the workpiece started to be bent down at \( d/t \approx 0.3 \) and the pre-crack generated at \( d/t \approx 0.4 \). After propagating the crack (\( d/t > 0.65 \)), the bent-down angle \( \theta_B \) reached and saturated to \( 3.5^\circ(2.5^\circ-4.7^\circ) \) as shown in Fig. 10. and the lift-up clearance was 0.03 \( \text{mm} \).

(7) When using \( b = 10 \text{mm} \) and 15 mm, the bent-down angle \( \theta_B \) reached \( 2.0^\circ(1.8^\circ-2.4^\circ) \) at \( d/t \approx 0.43 \) when \( b = 10 \text{mm} \) and \( 1.2^\circ(0.9^\circ-1.4^\circ) \) at \( d/t \approx 0.56 \) when \( b = 15 \text{mm} \) as shown in Fig. 10. From Fig. 9, the pre-crack generated at \( d/t \approx 0.85 \), both. After propagating of the crack (\( d/t > 0.89 \) when \( b = 10 \text{mm} \) and \( d/t > 0.92 \) when \( b = 15 \text{mm} \)), the bent-down angle \( \theta_B \) slightly decreased and reached saturated angles of \( 1.8^\circ(1.6^\circ-2.0^\circ) \) and \( 0.4^\circ(0.3^\circ-0.5^\circ) \) for \( b = 10 \text{mm} \) and 15 mm, respectively, as shown in Fig. 10. Here, the lift-up (necking) distance of 0.024\( \text{mm} \) and 0.018\( \text{mm} \) before breaking off (\( d/t \approx 0.90 \) and 0.93) was detected from video photographs at \( b = 10 \text{mm}, 15 \text{mm} \), respectively.

Obviously, pressure suppression of wider anvil reduces the bent-down angle \( \theta_B \) and the lift-up(necking) distance, compared to the narrower anvil due to the frictional restraint of anvil (underlay) and the wedge pressure.
(8) In the case of AL plate \((b=40\text{mm})\), the bent-up angle was negative (namely, bent-up mode) and saturated -1.1° at \(d/t_b\approx0.76\), and the pre-crack occurred at the same position. Here, the lift-up disappeared due to the negative bent-down.

4.1.2 Sheared profile of specimen on experiment

After cutting PS specimens, the sheared profile of the specimens was observed by a microscope camera. Figure 13 shows representative side views of sheared zone of PS specimens using the four kinds of underlays. Although Fig. 5 introduced geometrical parameters on the right side sheared profile, since asymmetric features were detected from real sectional views, corresponded geometrical parameters on the left side were introduced here. The profile parameters of the left side sheared edge were measured from photographs as \(t_1\) (mm), \(t_2\) (mm), \(h_1\) (mm), \(h_2\) (mm), \(\theta\) (°), \(\theta'\) (°) and \(\theta''\) (°). That of the right sheared edge were defined as \(t_1\) (mm), \(t_2\) (mm), \(t_3\) (mm), \(t_4\) (mm), \(\beta\) (°), \(\beta'\) (°) and \(\beta''\) (°). The penetrated stage depth showed a wear inclined shape. It was composed of two kinds of height zones \(t_1, t_2\) on the right side and that of \(h_1, h_2\) on the left side. The two kinds of left side height zone corresponded wear inclined angles \(\theta\) and \(\theta'\), while that of right side height zone had wear inclined angles \(\beta\) and \(\beta'\), respectively. Figure 14 shows relationship between the profile parameters of Fig. 13 and the contact width \(b\). Here, the bent-down angle \(\theta_b\) was evaluated at the maximum angle for \(b<15\text{mm}\), and at the saturated state for \(b=40\text{mm}\).

Seeing Fig. 13(a), in the case of AL plate underlay, the wear inclined zone was characterized with the inclined angle \(\beta<\beta', \theta<\theta'\) and its height range of \(t_1+t_2\approx h_1+h_2\approx 0.77t_b\). In the lower zone, the fracture height \(h_3\approx t_3\approx 0.23t_b\) was small but its surface roughness was large. Also, the force drop was large at the final breaking point. The whole shape seemed to be plastically deformed as the penetrated stage. Namely, the plastic wedging of sheared zone was deeply processed against a large underlay. When the contact width \(b\) was relatively large \((b=40\text{mm})\), the shear trace of left side and right side were almost symmetry. Due to the small height \((0.23t_b)\) of fracture zone, the inclined angles \(\theta''\) and \(\beta''\) were almost zero.

![Sheared profile of PS specimens](image)

**Fig. 13** Representative sheared profiles of PS specimens that were cut off by a 42° wedge blade when using (a) AL plate, (b) 6mm width AL anvil block and (c) 3mm width AL anvil block as underlay.

In the case of 6mm, 10mm and 15mm width anvil, the shear trace of specimen seems to be the similar with each other. Fig. 13(b) shows an example of shear trace at \(b=6\text{mm}\). Since the pre-crack occurred at the earlier than the case of AL plate underlay due to a downward bending of the workpiece (\(\theta_b=3.5°, 1.7°\) and 0.4° in case of use the width of anvil at 6mm, 10 mm and 15 mm, respectively.), the penetrated stage shortly settled down when the width of anvil was decreased. But as the penetration was yet large, the inclined angle was kept as \(\beta<\beta', \theta<\theta'\). As the result, the wear height range of \(t_1+t_2\approx h_1+h_2\) was about 0.48\(t_b\), 0.72\(t_b\) and 0.74\(t_b\) in case of use the width anvil at 6mm, 10mm and 15mm, respectively (shallower than the case of (a), but deeper than the case of (c)), while the fracture height \(h_3\approx t_3\) relatively increased owing that the second wear height \(t_2\approx h_2\) decreased when compared with Fig. 13(a).

In cases of \(b=6\text{mm}\), 10mm and 15mm, the wear height of \(t_1+t_2\approx h_1+h_2\) were increased with the contact width of PS specimens against the anvil \(b\), as shown in Fig. 13(a). Here, on the left fracture zone \((h_3)\), the inclined angle \(\theta''\) was almost zero when use the width anvil at 6mm, while a small inclined angle of \(\beta''\) was detected on the right fracture zone \((t_3)\) as shown in Fig. 13(b).

Seeing the cases of \(b=2\text{mm}\) and 3mm in Fig. 9(c), (d) and Fig. 13(c), the bent-down angle \(\theta_b\) of the workpiece increased and the pre-crack remarkably occurred at the earlier. Therefore, the plastic penetration stage was not promoted. As the result, the wear height of \(t_1+t_2\approx h_1+h_2\) were about 0.19\(t_b\) (at \(b=2\text{mm}\)), 0.25\(t_b\) (at \(b=3\text{mm}\), respectively. And the fracture height \(h_3\approx t_3\) increased up to 0.85\(t_b\) (at \(b=2\text{mm}\)) and 0.7\(t_b\) (at \(b=3\text{mm}\), respectively. Namely, the crack propagation mainly worked for generating the sheared surface and there was not almost any plastic deformation as the penetration stage. As the result, the wedged profile was in a liner shape: \(\theta''\approx\theta\) and \(\beta''\approx\beta\). At \(b=2\text{ and }3\text{mm}\) in Fig. 13(c) and (d), the inclined angles \(\theta'', \beta''\) of the upper half zones \((h_3, t_3)\) of specimen were about 0° both, while \(\theta''\) was almost
equal to $\beta''$ which had an inclined angle 24.5° at the lower half zones ($h_3$, $t_3$). Namely, the left side cracked trace had a sunken-broken line. This asymmetric inclination can be explained from the picture at $d/t_S=0.65$ ($b=2\text{mm}$) and at $d/t_S=0.55$ ($b=3\text{mm}$) in Fig. 9(c). At this stage, the left side of the wedge surface strongly contacted with the wedged surface of PS specimen, while the right side of wedge surface detached from the cracked surface of PS specimen. This phenomenon can be understood as a sort of side-wedge indentation that penetrates to the left side. At the lower half zone ($d/t_S=0.78$ at $b=2\text{mm}$ and 0.65 at $b=3\text{mm}$), the attitude of wedge knife against the crack was inclined.

Synthetically, when increasing the initial contact width $b$, the penetration stage as the inclined shape increased, while the bent-down angle $\theta_B$ decreased. The right-left cutting traces of Fig. 13(b) and (c) were fairly asymmetric (the crack direction inclined to the right side with 3.25°), compared to that of Fig. 13(a). These asymmetric traces seemed to be caused from a small deviation (misalignment) of wedge blade against the anvil block. This asymmetric cracking was unstable and sensitive for the misalignment of the wedge against the anvil.

![Fig. 13(a) and Fig. 14(a)](image)

Fig. 14 Dependency of sheared profile of PS specimen on the initial contact width of PS specimen against the underlay $b$. (a) Total height of wear zone on the left side ($h_1 + h_2$) and that on the right side ($t_1 + t_2$), fracture height ($h_3$ and $t_3$) and burr elevation height ($h_4$ and $t_4$), (b) Primary and secondary inclined angles of wear zone $\theta$, $\theta'$, $\beta$, $\beta'$ and tertiary inclined angle of breakage zone $\theta''$ and $\beta''$, (c) Saturated bent-down angle $\theta_B$.

Seeing Fig. 13(a) and Fig. 14(a), when increasing the contact width $b$, the elevation height of burr ($h_4$ and $t_4$) appeared to increase. Namely, a sort of necking was observed when $b>6\text{mm}$. When $b=2\text{mm}$, the necking was too small owing that the specimen was bent down with $\theta_B = 23.9^\circ$ by wedging, as shown in Fig. 9(d) ($d/t_S=0.89$) and Fig. 14(c).

### 4.2 Results of simulation

#### 4.2.1 Finding fracture criterion and crack propagation model

Since the work body is defined as a two dimensional plain strain model, it is assumed to be a sheet which has a thickness of 3mm, and its width is infinitive, a very long width is assumed. It is not any square bar. So, the simulation is based on a workpiece of 3mm thickness. On the simulation, the mechanical properties of PS were assumed to be an elasto-plastic behavior based on Fig. 4 and Table 1 until the equivalent strain $\varepsilon$ reaching a breaking strain of $\varepsilon_B = 0.32$.

In the cutting process, since the equivalent strain seems to be extremely enlarged in a local sheared zone, to extrapolate the stress-strain relationship is generally necessary in order to avoid any undefined stress-strain state. Therefore, in this work, a perfect plastic model: $\bar{\sigma} = \sigma_0$ for $\varepsilon > \varepsilon_0$ was virtually considered. Seeing Fig. 13(a), the left and right side sheared profiles of specimen were almost symmetry, a half symmetric cutting was simulated using $L_s/2=20\text{mm}$ and $b/2=20\text{mm}$ in Fig. 7(a) in order to confirm the fracture criterion. In a simulation process, each element of deformable body was
updated at every incremental step and was deleted (removed) when the evaluation amount of $I_c$ reached 1.0, using Eq. (2). This was named as the element deleting method. In order to determine the value of $C$ in Eq. (2), the maximum principal stress $\sigma_{\text{max}}$ and the equivalent yield stress $\bar{\sigma}$ were searched in each element in the deformable body subjected to a wedge indentation, until reaching the critical position expressed by Eq. (2). Namely, the integration of $I_c$ was calculated in the preliminary simulation without any cracks, until the indentation depth reached a pre-cracked position of $d/t_s = 0.76$ in the experiment, as shown in Fig. 9(b). From the preliminary non-cracked simulation, the target values $\sigma_{\text{max}}$ and $\bar{\sigma}$ were detected at a circled notched position in Fig. 15(a) and (b), respectively. The integration value of $I_C$ was approximated as $\int_0^{\bar{\varepsilon}} \sigma_{\text{max}} \, d\bar{\varepsilon} \approx \sigma_{\text{max}} \bar{\varepsilon}$. Here, the equivalent strain $\bar{\varepsilon}$ was detected at $d/t_s = 0.76$ in Fig. 15(c). So far, the critical fracture value $C$ was estimated as $(35.57/32.62) \times 0.31 = 0.33$.

4.2.2 Verification of simulation model.

Using the stress-strain model as mentioned at the section 2.2, and the breakage function of Eq. (2) under $C=0.33$, the simulated cutting load response at $b=40$mm (against the counter plate underlay) fairly fitted with an experimental result as shown in Fig. 16. By adding Eq. (2) under $C=0.33$ to the stress-strain model, the total behavior of mechanical properties of workpiece seems to be close the experimental behavior of Fig. 6, and the breakage behavior of PS bar was well explained. In the following, the elasto-plastic model plus the breakage function Eq. (2) under $C=0.33$ was basically investigated.

Figure 17 shows numerical contour band diagrams of the equivalent strain and deformation states of specimen subjected to a wedge indentation. Figure 18(a) shows the maximum bent-up angle during a numerical cutting process, while Fig. 18(b) shows an example of experimental bent-up state (negative as bent-down) at $d/t_s=0.79$. Here, the standard deviation of $\theta_B$ was 0.08 from the experiment and its average was -0.9° with 5 samples.

![Fig. 15 Wedged bottom zone of workpiece at d/t_s = 0.76 and contour band diagrams of three quantities using the non-cracked model. (a) The maximum principal tensile stress, (b) The equivalent stress and (c) The equivalent strain.](image)

![Fig. 16 Comparison of simulated load response of wedge indentation when considering damage model or non-damage model with experimental process. Initial contact width of workpiece was b= 40 mm against Al counter plate as underlay.](image)
Fig. 17 Simulated deformation states of workpiece subjected to a wedge indentation when choosing an initial contact width of \( b=40 \text{mm} \). (a) Penetrated stage \((d/t_S=0.2)\), (b) Pre-crack occurred at \( d/t_S=0.77 \), (c) Crack propagated stage at \( d/t_S=0.78 \) and (d) Breaking down at \( d/t_S=0.82 \).

Fig. 18 The saturated bent-up state of specimen subjected to a wedge indentation at \( d/t_S = 0.71-0.82 \). (a) Simulation result had \( \theta_B = -2.1^\circ \) at \( d/t_S = 0.81 \), (b) Experimental case of bent-up state had \( \theta_B = -1.1^\circ \) at \( d/t_S = 0.79 \).

The pre-cracked state Fig. 17(c) \((d/t_S=0.77)\) and the break down state Fig. 17(e) \((d/t_S=0.82)\) in the simulation were similar to the experimental results shown in Fig. 9(b) \((d/t_S=0.76)\) and (d) \((d/t_S=0.81)\) in case of \( b=40 \text{mm} \). As for the saturated bent-up state of specimen during the wedging process, the saturated bent-up angle (negative as bent-down) occurred at \( d/t_S = 0.71-0.89 \) and \( d/t_S = 0.76-0.81 \) for simulation and experimental, respectively, while the average of difference of saturated bent-up angle between experiment and simulation was \( 1^\circ (0.9^\circ-1.1^\circ) \).

Seeing Fig. 16, the gradient \( \partial f/\partial (d/t_S) \) measured from the pushed stage to the penetrated stage \((d/t_S=0.00-0.21)\) were 358.5 and 352.0 N/mm in the simulation and the experiment of \( b=40 \text{mm} \), respectively. They fairly matched with each other. So far, it was revealed that mechanical properties of workpiece as the modified model (Fig. 4 plus \( C=0.33 \) in Eq. (2)) was usable for investigating the wedging characteristics of 3mm squared workpiece.

The wedge indentation (cutting) resistance of workpiece is fundamentally estimated using the sliding line field theory of a shallow indentation of wedge (Grunzweig, et al., 1954, Nagasawa, et al. 2019), and the relationship between the line force \( f \) N/mm and the normalized indentation depth \( d/t_S \) is expressed as Eq. (6). Here, the coefficient \( D \) (mm) is generally determined from the apex angle of wedge \( \alpha \) and the friction coefficient \( \mu \) on the contact surface. In this work, assuming that the friction coefficient of workpiece and a steel blade was \( \mu = 0.31 \) and the apex angle was \( \alpha = 42^\circ \), the coefficient \( D \) was numerically estimated as \( 2.568 t_S = 7.7 \text{ mm} \).

\[
f = D(\alpha, \mu) \sigma_Y (d/t_S) \tag{6}
\]

Since the experimental gradient \( \partial f/\partial (d/t_S) \) was 352.0 N·mm\(^{-1}\), the yield stress \( \sigma_Y \) was estimated as 352.0/(2.568t_S)=45.7 MPa. According to Table 1 for the in-plane tensile test and Table 2 for the out-of-plane compression test, the yield stress was 30.18 and 85.63 MPa, respectively. It was found that the estimated wedge yield stress of 45.7 MPa was in the intermediate range of 30.18-85.64 MPa. As for the simulation, Fig. 4 and Table 1 were considered and included a sort of work hardening. In such the case, the initial yield stress \( \sigma_Y = 30.18 \text{ MPa} \) well covered the experimental gradient \( \partial f/\partial (d/t_S) \) as shown in Fig. 16. Therefore, the difference between the sliding line field based estimation 45.7 MPa and \( \sigma_Y = 30.18 \text{ MPa} \) seemed to be caused from the work hardening effect.
4.2.3 Effects of contact width of workpiece against the underlay on wedged profile during cutting simulation

In the section of 4.2.1 and 4.2.2, an FEM simulation model of workpiece subjected to a wedge indentation was developed and confirmed with respect to the similarity to the experimental features in case of the contact width of workpiece against the underlay $b=40$mm. In order to reveal the effects of $b$ on the wedged profile, the simulation model was investigated here when widely changing $b=1$-30mm.

Figure 19 and 20 illustrate simulated deformation states of workpiece and contour band diagrams of equivalent strain during a 42° wedge indentation against two kinds of anvil underlay ($b=1$ and 6 mm) until reaching a large force drop or a separation of wedged zone. When varying the initial contact width of workpiece against the underlay $b$ from 1mm up to 30mm, the deformed profiles and shear traces were empirically classified in three types.

Fig. 19 Simulated deformation states of 3mm thickness workpiece subjected to a 42° wedge indentation when using an initial contact width of $b=1$ mm. (a) $d/t_S=0.18$, the edge of anvil started to penetrate into lower surface of workpiece, (b) $d/t_S=0.3$, a necking occurred at the lower center zone, (c) $d/t_S=0.36$, a pre-crack (as the first small crack) occurred at the wedged zone, (d) $d/t_S=0.69$, the upper crack propagated largely but not completed while the edge of anvil penetrated and the lower necking zone increased, (e) $d/t_S=0.72$, the separation of wedged zone almost completed.

Fig. 20 Simulated deformation states of 3mm thickness workpiece subjected to a 42° wedge indentation when using an initial contact width of $b=6$ mm. (a) $d/t_S=0.19$, a lift-up started, (b) $d/t_S=0.26$, the edge of anvil started to penetrate into workpiece, (c) $d/t_S=0.69$, a pre-crack occurred at the wedged zone, (d) $d/t_S=0.86$, the upper crack propagated largely but not completed, (e) $d/t_S=0.92$, the separation of wedged zone almost completed.
The group 1 was in a range from \( b = 1 \text{mm} \) to \( 3 \text{mm} \). Figure 19 showed the case of \( b = 1 \text{mm} \). Wedged workpieces were bent-down largely and the sheared trace of them was smoothly straight as shown in Fig. 13(c) and (d). The group 2 was in a range from \( b = 5 \text{mm} \) to \( 7 \text{mm} \). Figure 20 showed the case of \( b = 6 \text{mm} \). Wedged workpieces were a little bent-down and the hollow shape of \( \beta' > 0 \) and \( t_1 + t_2 > t_3 \) was similar to Fig. 13(b). The group 3 was in a range from \( b = 7 \text{mm} \) to \( 40 \text{mm} \). From Fig. 17(\( b = 40 \text{mm} \)), workpieces were slightly bent-up (namely, negative bent-down), while the sheared trace was hollow and similar to that of group 2. That was similar to Fig. 13(a).

Seeing the group 1-2, although the lower surface of workpiece was remarkably deformed as shown in Fig. 19 and 20, such a large penetration of edge of anvil was not observed in the experimental results. This mismatching seems to be caused from the difference of material properties at the surface layer and the anisotropy of workpiece. From the level of contour band of equivalent strain in Fig. 19 and 20, the narrow underlay (\( b = 1 \text{mm} \)) had the less equivalent strain (<0.46) than that (<0.60 at a narrow zone) of the wider underlay (\( b = 6 \text{mm} \)). Furthermore, in case of Fig. 17 (\( b = 40 \text{mm} \)), the level of equivalent strain apt to be larger than that of \( b = 6 \text{mm} \). Namely, the level of equivalent strain 0.6 at \( b = 30 \text{mm} \) had the wider sectional area than that of \( b = 6 \text{mm} \). This tendency of equivalent strain distribution revealed that the workpiece based on a narrow underlay generated the smoother sheared edge trace (as the less plastic strain) than that of a wide underlay.

![Fig. 21](image)

Fig. 21 Dependency of sheared profile of workpiece on the initial contact width of workpiece against underlay \( b \). (a) Total wear height \( t_1 + t_2 \), fracture height \( t_3 \) and elevation height (necked) \( t_4 \), (b) primary and secondary inclined angles of \( \beta, \beta' \), tertiary inclined angle \( \beta'' \), (c) bent-down angle \( \theta_B \) measured from the FEM simulation.

Simulated parameters of sheared trace as the total height of wear zone \( (t_1 + t_2) \), the fracture height \( (t_3) \), the elevation height \( (t_4) \), the primary and secondary inclined angles of wear zone \( \beta, \beta' \), the tertiary inclined angle of breakage zone \( \beta'' \) and the bent-down angle \( \theta_B \) of right side of workpiece were shown in Fig. 21, when varying the initial contact width of workpiece against underlay from \( b = 1 \text{mm} \) up to \( 40 \text{mm} \). The bent-down angle \( \theta_B \) decreased when choosing \( b < 6 \text{mm} \). This tendency was similar to the experiment shown in Fig. 14(c). Variation tendency of \( t_1 + t_2, t_3, t_4, \beta, \beta' \) were relatively similar to that of the experiment shown in Fig. 14(a) and (b). Although the tertiary inclined angle \( \beta'' \) was fairly different for \( b < 3 \text{mm} \), compared with the experiment of Fig. 14(b), this was appeared to be caused from asymmetric breakage observed in Fig. 13(c) and (d).
4.2.4 Effects of contact width of workpiece against the underlay $b$ on cutting load response in simulation

In the section 4.2.3, the sheared trace was investigated through the simulation of workpiece subjected to a wedge indentation when changing the contact width $b=1$ to 40mm. In the simulation, the cutting load response with the indentation depth of blade was also measured. The simulated results of cutting load when changing the contact width $b$ was shown in Fig. 22(a) and (b). Figure 22(b) shows the peak maximum cutting force $f_{\text{max}}$, the pre-cracked indentation depth $d_{\text{pre-crack}}/t_S$, and the breaking indentation depth $d_{\text{break}}/t_S$ with respect to $b$.

Seeing Fig. 22(b), when the contact width $b$ increased, the peak maximum line force ($f_{\text{max}}$) increased and saturated for $b>6$mm($=2b_3$). This result was well matched to the experimental $f_{\text{max}}$ shown in Fig. 8(b). The $d_{\text{pre-crack}}/t_S$ was smaller than 0.4 for $b<3$mm=$t_S$, while it was larger than 0.7 for $b>10$mm=3.3$t_S$. This tendency was similar to the experiment in Fig. 8(b) although a middle zone of 5<$b$<7mm has different tendency from the experiment at $b=6$mm. This mismatching of $d_{\text{pre-crack}}/t_S$ seemed to be related to the mismatching of load response. Seeing cases of 1<$b$<7mm in Fig. 8(a) and Fig. 22(a), the load response in the experiment after passing the peak maximum was remarkably different (reduced) from that of the simulation. This difference seems to be caused from the dynamic behavior of crack propagation, and the simulation model of Eq. (2) could not cover this dynamic behavior.

Seeing the $d_{\text{break}}/t_S$, it did not decrease for $b<3$mm, while it gradually decreased for $b>10$mm, this tendency was relatively similar to the experiment shown in Fig. 8(b). The latter of decreasing for $b>10$mm seemed to be affected by the negative bent-down (bent-up). Since the bent-up of workpiece accelerates a necking at the lower layer of wedged zone of workpiece and decreases the contact length against the anvil underlay, the breakage position seemed to decrease.

Figure 23 shows a full contact length of workpiece against the anvil/counter plate underlay $b_C$ at the peak maximum load point when changing the contact width $b$. It was revealed that $b_C$ was about 7mm for 7<$b$<40mm, while $b_C \approx b$ for $b<7$mm($=2.3t_S$). Since the tool condition of $b=7$mm corresponded to the sign changing of bent-down angle $\theta_B$ as shown in Fig. 21(c), the effect of $b$ on $b_C$ was explained from the changing of bent-down/up of workpiece. As the maximum length of $b_C$ was about 7mm in this simulated case ($\alpha=42^\circ$, $t_S=3$mm), a tool condition of $b>7$mm ($=2.3t_S$) is necessary for making a bent-up cutting. Seeing Fig. 23(b) with 21(c), the result found that when the contact length of workpiece against underlay $b_C$ was increased the bent-down angle $\theta_B$ seems to be decreased. Figure 24 revealed that the transient contact length $b_C$ remarkably changed before reaching the peak maximum load point ($d/t_S < 0.5$) when $b$ was longer than 10mm ($b > 3.3$ $t_S$), while $b_C$ was stably apt to decrease for $d/t_S < 0.5$, $b < 10$mm.

![Fig. 22 Simulated load response. (a) Simulated cutting load response of 3mm thickness workpiece subjected to a wedge indentation when varying the contact width $b=1$ to 40mm and (b) Dependency of maximum line force, pre-crack position and break down position on the contact width of 3mm thickness workpiece subjected to a wedge indentation simulation.](image-url)
5. Conclusions

In order to reveal the effect of anvil shape on the cutting load response and sheared edge trace of a 3-mm-squared PS bar, an indentation test of a 42° center bevel blade across to the PS bar was experimentally carried out under an indentation velocity of $V=0.1$ mm/s. A wide counter face plate and several kinds of anvil plates, made of aluminum alloy, were considered as the underlay against the wedge indentation. Furthermore, an elasto-plastic 2 dimensional (plane strain) FEM model was developed and the numerical behavior of cutting deformation of 3mm thickness workpiece (modelled from the PS bar) was investigated when changing the contact width of workpiece against the underlay $b$. The results were as follows:

1) When changing $b = 2$-40mm in a cutting process of PS bar, the load response and sheared edge trace were characterized with $b$. Focusing on the peak maximum load, video motion of wedging process, and geometrical parameters of sheared profile of specimen, a proportion of wedge penetration flow and crack propagation was revealed with $b$.

2) A wedge penetration flow as sheared profile of workpiece was remarkably apparent when $b/t_S > 2$, while a smooth flat edge was a major part of sheared profile when $b/t_S < 1$. The former was based on the plastic flow deformation as a wedge indentation, and the latter seemed to be generated by an early pre-cracking and its dynamic crack propagation due to a slow indentation velocity of wedge blade.

3) When $b/t_S < 2$, a certain large bent-down of workpiece and a lift-up of central zone occurred. It made easily a crack initiated and accelerated the crack propagation. When $b/t_S > 2.3$, the bent-down of workpiece became negative (namely, bent-up) and the wedged profile was settled down in a wedge penetrated flow form, also the peak maximum force became saturated. These relations were approximated with Eq. (3), (4) and (5).
4) When $b/t_S < 1$, the sheared profiles of the left and right side edges of workpiece were asymmetric, flat but inclined shape in one way, due to a small misalignment of the blade tip position against the both side edges of anvil tool.

5) A plain strain elasto-plastic FEM simulation model was developed using an in-plane tensile test data and a crack generation rule expressed by Eq. (2) at the factor $C=0.33$. The simulated peak maximum force $f_{\text{max}}$ well fitted to that of the experimental result, and the simulated initial gradient $(\partial f/\partial (d/t_S))$ was valid with the experiment. The simulated pre-crack position was fairly larger than that of the experiment for $b/t_S < 0.3$. The simulated breaking position was similar to that of the experiment.

6) From the mismatching of pre-crack position between the simulation and the experiment when $b/t_S < 5$, the simulated load response was fairly different from that of experiment for $d/t_S > 0.3$. Especially the experimental-dynamic crack propagation was not covered by the proposed simulation model (Eq. (2) and $C=0.33$) when $b/t_S < 1$ and $V=0.1 \text{ mm} \cdot \text{s}^{-1}$.

7) The simulated sheared profile characterized by geometric parameters such as the wear heights, the fracture height, the elevation height, the inclined angles. These simulated parameters with $b$ were similar to that of the experiment.

8) When $b/t_S < 2$, the simulated load response was fairly different from the experimental load response, especially for the peak maximum force point and further deeper stage of blade indentation ($d/t_S > 0.3$).

9) Synthetically seeing, a 42 degrees wedge cutting of 3-mm-height PS specimen against a narrow width anvil ($b/t_S = 0.3-1.0$) had the better condition for making a smooth edge trace. The sheared edge trace of specimen in this condition appeared to have a long smooth zone ($t_1$), a small fracture zone ($t_2$) and a small fractured zone ($t_3$).

## Nomenclature

| Symbol | Definition |
|--------|------------|
| $\alpha$ | Upper blade apex angle $42^\circ$ |
| $L_S$ | Longitudinal length (in the $x$-axis) of polystyrene bar $40 / \text{mm}$ |
| $t_S$ | Thickness (Height, in the $z$-axis) of polystyrene bar $3 / \text{mm}$ |
| $b_S$ | Lateral width (in the $y$-axis) of polystyrene bar $3 / \text{mm}$ |
| $L_{\text{AL plate}}$ | Lateral length (in the $y$-axis) of AL plate $80 / \text{mm}$ |
| $L_{\text{AL anvil}}$ | Lateral length (in the $y$-axis) of AL anvil $30 / \text{mm}$ |
| $b_{\text{AL plate}}$ | Width (in the $x$-axis direction) of AL plate $80 / \text{mm}$ |
| $b_{\text{AL anvil}}$ | Width (in the $x$-axis direction) of AL anvil $2, 3, 6, 10, 15 / \text{mm}$ |
| $b$ | (Initial) contact width of PS specimen (or workpiece) against the counter plate or anvil underlay (in the $x$-axis) |
| $b_C$ | Contact length of workpiece against the counter plate or anvil underlay (in the $x$-axis) for $d/t_S = 0-1.0$ |
| $b_{CP}$ | Contact length of workpiece against the underlay at the peak maximum force $= b_C$ at the peak maximum force |
| $t_0$ | Thickness of AL underlay $0.4 / \text{mm}$ |
| $R_s$ | Surface roughness / $\mu \text{m}$ |
| $L_{\text{blade}}$ | Length (in the $y$-axis) of wedge blade $70 / \text{mm}$ |
| $H, S$ | Height of blade $23.5 / \text{mm}$, Thickness of blade $0.71 / \text{mm}$ |
| $V$ | Cutting/feed velocity of cutting tool $/ \text{mm} \cdot \text{s}^{-1}$ |
| $E$ | Young’s modulus of material $/ \text{MPa}$ |
| $\nu$ | Poisson’s ratio / $-$ |
| $\varepsilon$ | Logarithmic strain $= \ln(1+\varepsilon)$, here $\varepsilon$ is the nominal strain / $-$ |
| $l_i$ and $l_f$ | Initial length of specimen and final length of specimen / $\text{mm}$ |
| $c$ | Indentation depth of wedge blade into workpiece / $\text{mm}$ |
| $\sigma_y$ and $\sigma_B$ | True yield stress and true breaking stress of sheets $/ \text{MPa}$ |
| $\varepsilon_Y$ and $\varepsilon_B$ | Logarithmic yield strain and logarithmic breaking strain of sheets /$-$ |
| $F_{\text{INPL}}$ | Tensile force applied to in-plane tensile testing of specimen |
| $F$ | Cutting load resistance measured by load cell $/ \text{N}$ |
| $f$ | Cutting line force $(f = F/b_S) / \text{N} \cdot \text{mm}^{-1}$ |
| $d$ | Indentation depth of wedge blade w/o elastic gap of cutting apparatus / $\text{mm}$ |
| $t_s$ | Thickness of specimen (PS specimen) / $\text{mm}$ |
| $\mu$ | Friction coefficient between contacting interface / $-$ |
| Symbol | Definition |
|--------|------------|
| $\beta$, $\beta'$ and $\beta''$ | The primary, secondary and tertiary inclined angle on the right side / ° |
| $t_1, t_2$ | The 1st and 2nd wear height on the right side / mm |
| $t_3$ | Height of fracture zone on the right side / mm |
| $t_4$ | Elevation (bottom-up) height on the right side / mm |
| $\theta_b$ | Bent down angle of workpiece / ° |
| $C$ | The critical fracture threshold factor / - |
| $\bar{\varepsilon}_f$ | The equivalent fracture strain / - |
| $\sigma_{\max}$ | The maximum principal tensile stress / MPa |
| $\bar{\sigma}$ | The equivalent stress / MPa |
| $\bar{\varepsilon}$ | The equivalent strain / - |
| $\theta$, $\theta'$ and $\theta''$ | The primary, secondary and tertiary inclined angle on the left side / ° |
| $h_1, h_2$ | The 1st and 2nd wear height on the left side / mm |
| $h_3$ | Height of fracture zone on the left side / mm |
| $h_4$ | Elevation (bottom-up) height on the left side / mm |

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