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Abstract
The energy absorbing ability of a crashworthy structure for a railway’s rolling stock composed of welded aluminum alloys was evaluated numerically using finite element analysis (FEA). In the numerical simulation, two different material models were employed to characterize the base aluminum alloys and welding materials: one was a damage-mechanics model and the other a conventional plastic-mechanics model. The energy absorbing abilities of two different types of crashworthy structures were predicted using the FE simulations, and the numerical predictions were compared with experimental results obtained from quasi-static compression tests using mockups of these two crashworthy structures. The local phenomena (buckling and fractures) observed in the mockup tests, were also predicted numerically. The local fractures were accurately reproduced in the FE simulation employing the damage-mechanics model, while the buckling behaviors were predicted with substantial accuracy in both simulations. Comparison of the experimental results and the numerical predictions also revealed that the FE simulation applying the damage-mechanics model had an advantage in accurately predicting the energy absorption. The relationship between the local phenomena and the structural energy absorption is discussed.

Key words: Railway, Crashworthiness, Damage-Mechanics Model, Mockup Test and Finite Element Analysis (FEA)

1. Introduction
The possibility of railway train collisions is always a concern, and the crashworthiness of rolling stock must be guaranteed to protect passengers and crews from suffering serious injuries *(1)*. Train vehicles are required to absorb the collision energy substantially while preserving the integrity of the passengers’ area and driver’s cab in collisions. From this background, a specific structure for absorbing the collision energy, which is called a crashworthy structure, is allocated at the front and rear end portions of a rolling stock *(2)*.

When designing a crashworthy structure, a collision simulation applying finite element analysis (FEA) plays an important role. In the previous research, the plastic deformation and buckling behavior have mainly been analyzed. Marunaka et al. have evaluated the plastic buckling behavior of columns and beams composing the crashworthy structure using
FEA and a mockup test, and they designed a structure to absorb a sufficient amount of the collision energy while limiting peak value of the collapsing load \(^{(3)}\). Kawasaki et al. have conducted mockup test and FE simulation to evaluate plastic buckling behavior and energy-absorbing properties of the crashworthy structure allocating aluminum devices specified for energy absorption beneath the floor \(^{(4)}\). In both studies, an elasto-plastic mechanics model was employed as constitutive equations of the materials composing the structure and equivalent plastic strain was applied for the materials fracture criterion.

Fracture strain of metallic materials is known to be dependent on stress tri-axiality \(^{(5)}\). When the crashworthy structure absorbs collision energy by its plastic deformation, the stress tri-axiality at the welded region and the stress concentration area tends to be higher than that at the region unaffected by stress concentration, and hence local fractures can occur in these regions. Since fracture initiation and propagation degrade the structural stiffness and strength, the fracture region must be limited to as small an area as possible for efficient energy absorption. From this viewpoint, accurately predicting the local fractures and properly evaluating the effect of the fractures on energy absorption are substantially important for designing a crashworthy structure \(^{(6)}\).

In this research, numerical simulations using FEA were conducted to evaluate the energy-absorbing properties of two different types of crashworthy structures obtained from quasi-static compression tests using the mockups of the structures that have been reported in the previous research \(^{(4),(6)}\). Both structures consisted of welded aluminum alloys: one was composed of stiffened frame structure and thin plates \(^{(6)}\) and the other of thin plates and energy-absorbing devices made of extruded aluminum alloy \(^{(4)}\). In the numerical simulation, as constitutive equations of the aluminum alloys and the welding materials, two different material models were employed: a damage-mechanics model and a conventional plastic-mechanics model based on \(J_2\) flow theory. By comparing the numerical results with the experimental data with respect to initiation of fractures and energy-absorbing ability, the effect of the fractures on the energy-absorbing properties was evaluated.

2. Quasi-static Compression Test Using Full-scale Mockup

This section describes conformation of the crashworthy structures and the experimental results of the mockup test. Since the details of the mockup tests have already been indicated in previous papers \(^{(4),(6)}\), essential points of the test are described here.

2.1 Crashworthy Structure

The energy absorbing capacities of the crashworthy structures at the front and rear portions of an intermediate vehicle were evaluated. The gray hatchings in Fig. 1 show the positions of the crashworthy structures evaluated in the compression test.

Both mockups consist of aluminum alloys, and the application parts of the aluminum alloys are listed in Table 1. Metal-inert-gas arc welding and friction-stir welding are used to join the aluminum components. Figure 2 shows a schematic diagram of the full-scale mockups of the crashworthy structures. As indicated in Fig. 2 (a), the mockup of STRUCTURE A consists of two areas: a crushable zone and a survival zone, which represents a part of the passengers’ area. Deformation of the components is allowable in the crushable zone but not in the survival zone. In the crushable zone of STRUCTURE A, doorframes are placed and a thick component is allocated on each corner of the doorframe. Therefore, the corners of the doorframes are remarkably stiffened compared with the roof, floor, and body panel composed of thin plates. As shown in Fig. 2 (b), a whole part of STRUCTURE B is a crushable zone, and this structure is composed of thin plates. A specific component made of an extruded aluminum alloy for energy absorption, which is called an energy-absorbing block, is placed below the floor of STRUCTURE B. For STRUCTURE B, beam components of a 4-mm-thick horseshoe-shaped cross-section are
allocated on the inner side of the body panel, and beam components of a 6-mm-thick dogleg-shaped cross-section are placed on the lower side of the body panel.

The coordinate system indicated in Fig. 2 was applied; \(x\), \(y\), and \(z\) directions correspond to the width, vertical, and longitudinal directions of the mockups, respectively.

![Crashworthy structure](image)

**Fig. 1 Schematic of positions of crashworthy structures for railway vehicle.**

Table 1 List of application part of aluminum alloys.

<table>
<thead>
<tr>
<th>Designation</th>
<th>A5083P-O</th>
<th>A6N01S-T5</th>
<th>A7N01S-T5</th>
<th>A6063S-T5</th>
</tr>
</thead>
<tbody>
<tr>
<td>Application</td>
<td>Body panel</td>
<td>Roof panel, Floor</td>
<td>Pillar, Doorframe</td>
<td>Energy-absorbing block</td>
</tr>
</tbody>
</table>

![Intermediate vehicles](image)

(a) STRUCTURE A

![Intermediate vehicles](image)

(b) STRUCTURE B

**Fig. 2 Overview and schematic of mockups of two kinds of crashworthy structures.**

### 2.2 Testing Procedure

A quasi-static compression test was conducted with a hydraulically controlled testing machine. Experimental conditions are indicated in Table 2, and a side view of the experimental setup is shown in Fig. 3. A full-face collision was tested by compressing the mockup from the one side against the longitudinal direction with a steel wall as shown in Fig. 3 while perfectly constraining the displacement at the opposite side. The steel walls are rigid enough to be regarded as rigid walls in the FE simulation.

We measured compression force with a load cell and displacement of the steel wall with a displacement gauge. Deformations of some components in the crushable and survival zones were evaluated with 5-mm-long strain gauges attached to the face of the structure. To satisfy the quasi-static condition, the crosshead speed was kept to between 1.0 mm/min and 10 mm/min.

**Table 2 Experimental conditions of quasi-static compression test.**

<table>
<thead>
<tr>
<th>Crosshead speed</th>
<th>Control</th>
<th>Maximum force</th>
<th>Stroke</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.0–10 mm/min</td>
<td>Hydraulic</td>
<td>50 MN</td>
<td>600 mm</td>
</tr>
</tbody>
</table>
2.3 Results

For STRUCTURE A, buckling deformations concentrated into the roof and floor components and into the plate in the upper side of the doorframe whose stiffness was relatively low. On the other hand, for STRUCTURE B, buckling deformation occurred in the whole area of the body panel and the progressive buckling deformation occurred in the energy-absorbing blocks. These buckling behaviors are indicated in 4.1 with numerical predictions of the FE simulations.

In addition to the buckling deformation described above, local fractures initiated at the welded regions and the stress concentration areas in STRUCTURE A. Initiation of the local fractures were evaluated with visual observation, and an example of the local fractures is shown in Fig. 4 (a). In Fig. 4 (a), positions of the local fractures are indicated as fracture points. Variation of the $y$-direction strain in the vicinity of the fracture point measured by a strain gauge is indicated in Fig. 4 (b). In Fig. 4 (b), an instantaneous drop of the strain indicates initiation of the fracture. In STRUCTURE A, as the buckling deformations became larger, local fractures occurred in the roof and floor components, while distinct fracture was not observed on the face of STRUCTURE B.

Force-displacement curves obtained from the mockup tests are shown in Fig. 5. In Fig. 5, forces are normalized with each maximum value and displacement is that of the steel wall against $z$ direction. As indicated in Fig. 5 (a), for STRUCTURE A, the force reached a maximum at a displacement of 72 mm and monotonically decreased beyond this displacement. Buckling of the floor and roof in the crushable zone initiated at that displacement and these buckling deformations became larger as the displacement increased. As indicated in Fig. 4, the fracture at the welded region in lower side of the doorframe initiated at a displacement of 78 mm, which was similar to the displacement corresponding to the maximum force. Since the doorframe could not retain a frame structure due to the fractures, it is considered that the fracture initiation led to degradation of the strength of the doorframe, which in turn seems to have led to growth of the buckling deformations in the roof and floor. It is also considered that the fracture regions became larger as these buckling deformations grew, and hence the fractures and the buckling deformations presumably correlate. Since the buckling deformations dominate decrease of the compressive force, initiation of the local fractures presumably has a measurable effect on the decreasing behavior of the compressive force, i.e. the structural energy absorption.

As indicated in Fig. 5 (b), there are some peak values of the compressive force at displacements of 11 mm, 35 mm, and 145 mm. These peak values indicate initiation of collapse of the energy-absorbing blocks and the side beam (5). From a displacement of 145 mm to that of 400 mm, there is a cyclic fluctuation of the force and this cyclic behavior
exhibits the progressive buckling deformation of the energy-absorbing blocks. For STRUCTURE B, at a displacement of 450 mm, the force was reduced to elongate the stroke of the testing machine, and following that, the compressive force was reloaded until a displacement of 500 mm. At this displacement, the force was reduced to finish the test.

(a) Overview of local fractures
(b) Variation of y-direction strain in the vicinity of fracture point

Fig. 4 Local fractures at welded region.

(a) STRUCTURE A
(b) STRUCTURE B

Fig. 5 Force-displacement curve obtained from quasi-static compression test.
3. FE Simulation of Collapse of Crashworthy Structures

As indicated in the experimental results, different results with respect to initiation of the local fractures and the energy-absorbing properties were obtained for each structure. Especially for STRUCTURE A, the fractures were presumed to have a measurable effect on the energy absorption. To predict initiation of the local fracture and clarify the relationship between the fractures and the energy-absorbing properties, we conducted numerical simulations applying two different material models. In these models, the difference is the fracture criterion. This section describes the material models and the modeling method of the FE simulation.

3.1 Constitutive Equation for Aluminum Alloys and Welding Materials

The crashworthy structures were composed of aluminum alloys, and hence the local fractures occurring in the mockup test were ductile fractures. When a ductile fracture occurs, fracture strain is dependent on the stress tri-axiality, which is the ratio of equivalent stress and mean stress \( \frac{\sigma}{\sigma_h} \). In this research, to evaluate the collapse of the crashworthy structures considering variation of the fracture strain, we employed the Gurson-Tvergaard-Needleman model (GTN model), which is one of the damage-mechanics models, as constitutive equations of the aluminum alloys and the welding materials.

The GTN model’s yielding condition \(^{(7),(8)}\) is of the form

\[
\Phi = \left( \frac{\sigma_{eq}}{\sigma_M} \right)^2 + 2q_1 f^* \cosh \left( \frac{3q_1 \sigma_h}{2\sigma_M} \right) - 1 - (q_1 f^*)^2 = 0
\]

where \( \sigma_{eq} \) and \( \sigma_h \) are equivalent and mean stresses of the material containing a void, respectively, and \( \sigma_M \) is equivalent stress of the matrix. \( q_1 \) is a constant introduced by Tvergaard \(^{(9)}\). \( f^* \) is a function of the volume fraction of void \( (f) \), and it is of the following form:

\[
f^* = \begin{cases} 
 f & \text{for } f \leq f_c \\
 f_c + \frac{f^*_U - f_c}{f_f - f_c} (f - f_c) & \text{for } f > f_c
\end{cases}
\]

where \( f_c \) and \( f_f \) are the void volume fractions at which the void coalescence starts and the final fracture occurs, respectively, and \( f^*_U \) is \( 1/q_1 \). For a material without voids, \( f=0 \), Eq. (1) is exactly the same as the conventional Von-Mises yielding condition.

Variation of \( f \) with respect to time, \( \dot{f} \), is expressed as the sum of voids nucleation and growth:

\[
\dot{f} = (\dot{f})_{\text{growth}} + (\dot{f})_{\text{nucleation}}
\]

The growth rate of voids, \( (\dot{f})_{\text{growth}} \), follows

\[
(\dot{f})_{\text{growth}} = (1 - f) \varepsilon^p_{\text{pl}}
\]

where \( \varepsilon^p_{\text{pl}} \) is plastic volumetric strain. Assuming that initiation of the voids is dominated by the plastic strain, the nucleation of voids, \( (\dot{f})_{\text{nucleation}} \), is expressed as \(^{(10)}\):

\[
(\dot{f})_{\text{nucleation}} = \frac{f_N}{S_N \sqrt{2\pi}} \exp \left[ -\frac{1}{2} \left( \frac{\varepsilon^p_{\text{pl}} - \varepsilon^p_{\text{M}}}{S_N} \right) \right] \varepsilon^p_{\text{pl}}
\]
where \( \varepsilon^p_M \) is the effective plastic strain of the matrix. In Eq. (5), \( f_N \), \( \varepsilon_N \), and \( S_N \) are respectively the volume fraction of particles causing void nucleation, mean value of equivalent strain for void nucleation, and standard deviation of \( \varepsilon_N \).

The stress-strain relationship of the matrix of the aluminum alloys and welding materials is expressed as

\[
\varepsilon = \begin{cases} 
\frac{\sigma}{E}, & \text{for } \sigma \leq \sigma_Y \\
\frac{\sigma}{E} + \left( \frac{\sigma - \sigma_Y}{K} \right)^N, & \text{for } \sigma > \sigma_Y 
\end{cases}
\]

(6)

where \( E \) and \( \sigma_Y \) are Young's modulus and yielding stress respectively, and \( K \) and \( N \) are material constants in the plastic region. The values of \( E \), \( \sigma_Y \), \( N \), and \( K \) for each aluminum alloy are listed in Table 3. These values were obtained from static tensile tests. In this research, strain-rate dependency of mechanical properties of the materials was not introduced into the simulation. Since the compression test was conducted at quasi-static speed, it was presumed that the strain-rate dependency of aluminum alloys did not become measurable in the tests.

For the yielding stresses of the welding materials except for A6063-T5, we applied literature data\(^{(11),(12)}\) as 148 MPa for A5083P-O, 118 MPa for A6N01-T5 and 241 MPa for A7N01-T5, respectively. For the welding material of A6063-T5, we used the same value of yielding stress as the base material for the following reason: the application part of A6063-T5 was limited to the energy-absorbing blocks in which the collision energy was mainly absorbed with plastic buckling deformation of the parent material, and therefore the yielding stress of the welding material was assumed to affect the energy absorption negligibly.

In a conventional plastic-mechanics model, the Von-Mises yielding condition is employed and the effect of the accumulation of damage in the material is not included. Equation (6) with the values indicated in Table 3 was also applied as the stress-strain relationship in the plastic-mechanics model. Equivalent plastic strain was employed as the fracture criterion of this model. The fracture strain for the base materials was selected as 1.0 after referring to the previous research\(^{(6)}\). For the welded regions other than A6063-T5, 0.30 for A5083P-O, 0.13 for A6N01-T5 and 0.11 for A7N01-T5 were selected respectively referring to the literature data\(^{(11),(12)}\). Similar to the yielding stress, the fracture strain for the welding region of A6063-T5 was set to the same value as that of the base material.

Differences in the welding methods and their conditions affect the yielding stress and the fracture strain to some extent. However, we did not introduce these effects into the FE simulations for the following reason: even if we neglect such variation of the mechanical properties, the two material models described above can be compared with respect to evaluation of the energy-absorbing properties of the crashworthy structure.

<table>
<thead>
<tr>
<th>Table 3 List of mechanical properties of aluminum alloys.</th>
</tr>
</thead>
<tbody>
<tr>
<td>E (GPa)</td>
</tr>
<tr>
<td>-------</td>
</tr>
<tr>
<td>( \sigma_Y ) (MPa)</td>
</tr>
<tr>
<td>( N )</td>
</tr>
<tr>
<td>( K ) (MPa)</td>
</tr>
</tbody>
</table>
3.2 Identification of Material Constants

Though the GTN model is useful for dealing with ductile fractures, it includes some material constants to be identified, and these constants strongly influence ductile fractures (13). To identify the material constants, Noguchi et al. (14) have proposed an inverse analysis method, but they did not deal with the fracture initiation. In this research, these parameters were determined so that the stress-strain curve obtained from the FE simulation matched the one obtained from the tensile tests. Since it was quite difficult to identify all of the material constants simultaneously and the values of $\varepsilon_N$, $f_N$, $f_C$, and $f_F$ were expected to depend strongly on the materials, we assumed that $q_1$ was independent of the materials and selected $q_1=1.5$, which was determined by Tvergaard (19), as a central value. Identified values of the material constants are listed in Table 4.

![Figure 6](image)

**Fig. 6** Comparison of stress-strain curves between numerical prediction and experimental data.

<table>
<thead>
<tr>
<th>Table 4 List of mechanical constants of GTN model.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
</tr>
<tr>
<td>A5083-P-O</td>
</tr>
<tr>
<td>A6N01-T5</td>
</tr>
<tr>
<td>A7N01-T5</td>
</tr>
<tr>
<td>A6063-T5</td>
</tr>
</tbody>
</table>

3.3 Modeling Method of FE Simulation

The FE simulation models are indicated in Fig. 7, and the numerical conditions for the simulations are listed in Table 5. Since the mockup was in a plane-symmetrical conformation against the longitudinal axis, the simulation was of a 1/2 model, as shown in Fig. 7. Commercially available FE code, LS-DYNA version 971®, was used.

The simulation models were discretized using shell and solid elements. The shell elements were applied to the areas composed of thin plates, and the solid elements to the areas composed of thick and rigid components such as corners of the doorframe of STRUCTURE A. Similar to the previous research, element size was selected as 10 mm to reproduce the plastic buckling behavior occurring progressively in the energy-absorbing...
blocks. Since deformation was expected to concentrate into the welded regions where the yielding stress was lower than the parent materials, the welded regions were modeled with finer mesh than regions of the parent aluminum alloys. In addition, stress concentration areas were also discretized with a finer mesh than those in the regions unaffected by the stress concentration.

![Fig. 7 FE simulation models.](image)

<table>
<thead>
<tr>
<th>Element size</th>
<th>Number of elements</th>
<th>Number of nodes</th>
</tr>
</thead>
<tbody>
<tr>
<td>Normal</td>
<td>STRUCTURE A</td>
<td>STRUCTURE B</td>
</tr>
<tr>
<td>10 mm</td>
<td>$3.9 \times 10^5$</td>
<td>$3.0 \times 10^5$</td>
</tr>
<tr>
<td>Fine</td>
<td>$3.7 \times 10^5$</td>
<td>$2.4 \times 10^5$</td>
</tr>
</tbody>
</table>

4. Discussion

In this section, we compare the numerical results with the experimental data with respect to initiation of the local fractures and the energy-absorbing properties to validate prediction accuracies of the simulations. Finally, the effect of the local fractures on the energy absorption is discussed.

4.1 Comparison of Deformation Mode

Figure 8 shows deformation modes of each crashworthy structure obtained from the FE simulations and the mockup tests. In Fig. 8 (a), buckling deformations at the plate component in upper side of the doorframe, the roof, and the floor are observed in the simulation and experimental results for STRUCTURE A. In Fig. 8 (b), buckling deformation of the whole part of the body panel and progressive buckling deformation of the energy-absorbing block are observed in the simulation and experimental results for STRUCTURE B. From these results, we learn that the difference in the fracture criterion of the material models has little influence on the deformation modes of the whole structure and the components and that both simulations can reproduce the experimental results with substantial accuracy.

In contrast to the deformation mode, the difference in the fracture criterion seems to have a large effect on the prediction accuracy of initiation of the local fractures. In the next section, we compare the simulation results with the experimental data with respect to the local fractures at the welded regions and the stress concentration areas, and we discuss the prediction accuracies.
Fig. 8 Comparison of global deformations in crushable zones among predictions of two FE simulations and experimental results.
4.2 Comparison of Fracture Initiation

As shown in 2.3, although the local fractures occurred at the welded regions of STRUCTURE A, a distinct fracture was not observed on the face of STRUCTURE B. Since the local fractures are considered to have a measurable influence on the energy-absorbing properties, we compare the simulation results with the experimental data with respect to the fractures occurring at the welded region in lower side of the doorframe and at the plate component in upper side of the doorframe. The sizes of these fracture regions were the largest in the mockup test of STRUCTURE A.

The overview in the vicinity of the fracture points is indicated in Fig. 9. As shown in Fig. 9, the simulation using the GTN model can accurately reproduce the positions of the fracture points, while the simulation of the Von-Mises yielding model cannot predict some fracture points. However, the simulation of the GTN model cannot predict the crack growth beginning from the fractures, and as a result, the fracture regions are underestimated.

<table>
<thead>
<tr>
<th>Experimental</th>
<th>Simulation</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>GTN model</td>
</tr>
<tr>
<td>Fracture points</td>
<td>Fracture points</td>
</tr>
<tr>
<td>Strain gauge</td>
<td>Fracture point</td>
</tr>
</tbody>
</table>

(a) Fractures occurring at welded region in lower side of doorframe.

<table>
<thead>
<tr>
<th>Experimental</th>
<th>Simulation</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>GTN model</td>
</tr>
<tr>
<td>Fracture point</td>
<td>Fracture point</td>
</tr>
</tbody>
</table>

(b) Fractures occurring at parent aluminum alloy in upper side of doorframe.

Fig. 9 Comparison of local fractures in crushable zone of STRUCTURE A among predictions of FE simulations and experimental result.

Figure 10 shows the relationship between \( y \)-direction strain in the vicinity of fracture point depicted in Fig. 9 (a) and the displacement. From Fig. 10, we learn that the simulation using the GTN model can predict instantaneous variation of the strain that exhibits initiation of the fracture. In the simulation of the Von-Mises yielding model, the strain increases monotonically since initiation of the fracture is not reproduced as shown in Fig. 9 (a).
However, even in the simulation of the GTN model, the displacement corresponding to the fracture initiation is different from the experimental data: 90 mm in the simulation and 78 mm in the experimental. This is because the simulation cannot accumulate all of the deformations and fractures in exact detail.

From the results indicated in Figs. 9 and 10, the prediction accuracy of initiation of the fractures was found to depend on the material models. At the fracture points, such as the welded regions and the stress concentration areas, stress tri-axiality becomes higher than the regions unaffected by stress concentration, and as a result, the fracture strain becomes lower. The GTN model, as indicated in Eq. (1), includes the term related to the stress tri-axiality, and this model can express degradation of fracture strain caused by increase of stress tri-axiality (5). In contrast, dependency of fracture strain on stress tri-axiality is not included in the Von-Mises yielding model. For this reason, we believe that the simulation using the GTN model has shown an advantage in predicting initiation of the local fractures.

Although the simulation of the Von-Mises yielding model might be able to reproduce the fracture initiation depicted in Figs. 9 and 10 by reducing the fracture strain, reducing the fracture strain can lead to unexpected fractures occurring at the regions with relatively low stress tri-axiality where a fracture did not initiate in the mockup test. Therefore, reducing the fracture strain is not appropriate for predicting the fracture points when designing a crashworthy structure.

As described above, the simulation using the GTN model can predict initiation of the local fractures more accurately than that of the Von-Mises model. For the energy-absorbing properties, similar prediction may be obtained from these two simulations when the effect of the local fractures is negligible, and the prediction accuracy is presumed to be different when the effect is measurable. In the next section, we validate the prediction accuracies of these two simulations with respect to the energy absorption, and we discuss the influence of the local fractures on the structural energy absorption.

![Comparison of variation of y-direction strain in the vicinity of fracture point depicted in Fig. 9 (a) among predictions of FE simulations and experimental results.](image)

**4.3 Evaluation of Energy-absorbing Properties**

The force-displacement curves obtained from the FE simulation and the mockup tests are shown in Fig. 11, and relationships between the absorbed energy and the displacement are indicated in Fig. 12. The absorbed energy was calculated by integrating the force with respect to the displacement.

As depicted in Figs. 11 (a) and 12 (a), for STRUCTURE A, the simulation using the GTN model agrees better with the experimental result. From Fig. 11 (a), the difference in the prediction accuracies of the force becomes larger in a displacement beyond 90 mm. The
Displacement of 90 mm is coincident with the displacement at which the local fracture occurred in the simulation using the GTN model. From this viewpoint, we can say that the difference in the prediction accuracies of the local fractures led to the difference in those of the energy-absorbing properties. However, even in the simulation of the GTN model, the absorbed energy results in 10% overestimation at a displacement of 160 mm, and this is considered to be due to underestimation of the fracture regions.

As indicated in Figs. 11 (b) and 12 (b), although the simulation of the GTN model agrees better with the experimental data for STRUCTURE B, the difference in the prediction accuracies of the force and the absorbed energy is small and both simulations predict substantially accurately.

From these results, we can conclude that the fractures occurring in STRUCTURE A have a measurable influence on the energy-absorbing properties, and it is considered that a distinct fracture occurred neither on the face nor in the inner side of STRUCTURE B.

Fig. 11 Comparison of force-displacement curves between predictions of numerical simulations and experimental results.
From the above, the simulation using the GTN model was found to have an advantage in prediction accuracy of the energy-absorbing properties for both crashworthy structures. However, when the effect of the fractures is not measurable, the simulation employing the Von-Mises yielding model can predict the experimental results with substantial accuracy. In addition to that, applying the Von-Mises yielding model leads to shortening of the calculation time. Therefore, we can conclude that combining the simulation of the GTN model and that of the Von-Mises yielding model is very effective for designing a crashworthy structure. First, the simulation using the GTN model is conducted to predict the fracture points accurately and to modify the structural composition so that the fracture areas decrease. Following that, the simulation of the Von-Mises yielding model is conducted to modify the structure so that the energy absorption increases. In this way, it becomes possible to design a crashworthy structure whose energy-absorbing ability is improved.
5. Conclusion

This study numerically evaluated the energy-absorbing properties and initiation of the fracture occurring at the components and welded regions in two types of crashworthy structures by using FE simulation. Numerical results were compared with the experimental results obtained from mockup tests. In the FE simulation, two different material models (GTN and Von-Mises) were applied, and the prediction accuracies of the FE simulations were assessed. As a result, the following conclusions were obtained.

(1) The buckling behaviors of the crashworthy structures are predicted substantially accurately regardless of any difference in the material models.

(2) Initiation of the fractures in the welded regions and stress concentration areas are accurately reproduced in the FE simulation employing the GTN model since dependency of fracture strain on stress state is incorporated in the GTN model.

(3) For STRUCTURE A, the local fractures occurred at the welded regions and the stress concentration areas and these fractures had a measurable effect on the structural energy absorption. The simulation using the GTN model was found to more accurately predict the energy-absorbing properties.

(4) Both FE simulations substantially accurately predict the energy-absorbing properties for STRUCTURE B, of which a distinct fracture was not observed on the face. From this result, it is considered that a distinct fracture occurred neither on the face nor in the inner side of STRUCTURE B.

References


