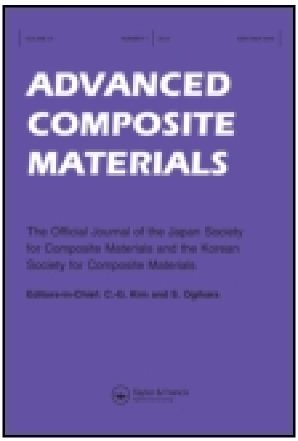

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Bending test for CFRP skin/foamed core sandwich plates

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Abstract—Bending tests of commercially available sandwich plates with three kinds of CFRP-fabric skins and two types of acrylic foam cores were conducted by means of a three-point bending. The effects of the span and the radius of the loading nose on the bending modulus and strength were examined systematically. Test data on bending rigidity did not coincide with those calculated from a modified beam theory where the effect of the shearing rigidity of the core was incorporated, although the tendency with respect to the span was the same. Based on these results, this paper proposes a desirable data reduction scheme by which methodology, the correlation between theory and experiment could be made clear.

Keywords: Sandwich; CFRP skin; foamed core; three-point bending; bending rigidity; data reduction scheme.

1. INTRODUCTION

Sandwich plates have a long history of application to structural elements, especially for aerospace use, and numerous research works have been conducted hitherto [1]; recently, a periodical journal for sandwich structures and materials was created [2]. In these researches, sandwiches of aluminum skin (face) and aluminum honeycomb core are of most interest and several standards [3] have been established to evaluate the mechanical properties of this kind of sandwich. Recently, CFRP skin/foamed core sandwiches have also been developed which are used in various fields, including medical application, mainly due to their X-ray transmittability while maintaining sufficient bending rigidity. In such a case of a relatively soft core, the existing test methods are not necessarily sufficient. For example, the deformation

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may not necessarily coincide with the classical beam theory due to the soft core and the failure pattern may be different from bending failure.

The bending rigidity (EI) can be measured experimentally by conducting a three- or four-point bending test of a sandwich coupon. In the case of the three-point bending,

$$EI_{\mathrm{exp}} = \frac{PL^3}{48\delta}, \tag{1}$$

holds, where P is the applied load, L is the span, and δ is the midspan deflection. This equation is based on an elementary beam theory.

On the other hand, if we know the elastic constants of the constitutive materials, that is, if we know Young's modulus of the skin, and the shearing modulus of the core (G_{c}), the bending rigidity can be calculated. There are several levels for this. The simplest one is: if we assume that the applied load (moment) is carried by only the face material and that the role of the core is only to keep the two skins at a constant distance, the bending rigidity can be calculated, under Euler–Bernoulli's hypothesis, as

$$EI_{\mathrm{c}} = \frac{E_{\mathrm{f}} b t_{\mathrm{f}} t_{\mathrm{c}}^2}{2}, \tag{2}$$

where E_{f} and t_{f} are Young's modulus and thickness of the face material, t_{c} is the thickness of the core, and b is the specimen width. This may be called a composite beam theory, although some simplification is done. This also corresponds to the case that the shearing modulus of the core is infinity while Young's modulus of the core is neglected.

In the case of a soft-core sandwich, shearing deformation of the core takes place. This leads to increase of the deflection and hence decrease of the bending rigidity. The total deflection, δ_{total}, is the sum of the deflection due to bending, δ_{b}, and the deflection due to the shearing deformation of the core, δ_{s}, that is,

$$\delta_{\mathrm{total}} = \delta_{\mathrm{b}} + \delta_{\mathrm{s}}. \tag{3}$$

In the case of a three-point bending, these values can be calculated by

$$\delta_{\mathrm{b}} = \frac{PL^3}{48EI_{\mathrm{c}}}, \tag{4}$$

$$\delta_{\mathrm{s}} = \frac{PL}{4t_{\mathrm{c}} b G_{\mathrm{c}}}, \tag{5}$$

where G_{c} is the shearing modulus of the core. In this case, the bending rigidity, EI_{m}, is, referring to equation (1),

$$EI_{\mathrm{m}} = \frac{PL^3}{48\delta_{\mathrm{total}}}. \tag{6}$$

From equations (3)–(6),

$$EI_{\mathrm{m}} = \frac{EI_{\mathrm{c}}}{1+\alpha}, \tag{7}$$

is derived where

$$\alpha = \frac{6E_{\mathrm{f}}t_{\mathrm{f}}t_{\mathrm{c}}}{L^2 G_{\mathrm{c}}}. \tag{8}$$

Equation (7) may be referred to as a modified beam theory where the shearing deformation of core is considered, and the skin is assumed to share the bending moment whereas the role of core is to carry the shearing force. If we neglect the shearing deformation of core, $\alpha = 0$ holds and equation (7) reduces to a composite beam theory, EI_{c}.

In equations (7) and (8), it becomes necessary to measure the shearing modulus of the core, G_{c}. But as will be described later, it is rather difficult to measure G_{c} precisely. In addition, even if a precise G_{c} value is obtained from the core itself, there is no guarantee that the mechanical properties of the core are kept unvaried in the sandwich plate. For example, if some amount of resin is impregnated in some portion of core during the co-cure process, the core will become harder.

There is a sophisticated way of calculating G_{c} by combining the results of a 3-point and a 4-point bending tests of a sandwich coupon [4]. The final result is

$$G_{\mathrm{c}} = \frac{P_3 L_3 t_{\mathrm{c}} \left\{ \dfrac{8L_3^2}{11L_4^2} - 1 \right\}}{2\delta_3 b (t_{\mathrm{f}} + t_{\mathrm{c}})^2 \left(\dfrac{16 P_3 L_3^3 \delta_4}{11 P_4 L_4^3 \delta_3} - 1 \right)}, \tag{9}$$

where the subscripts 3 and 4 denote, respectively, the values for 3- and 4-point bending. However, as far as our experiment is concerned, the scatter of the data by this method was fairly large; it is almost impossible to get a reliable G_{c} value using this method.

So far we have reviewed several methods to calculate the bending rigidity of a sandwich beam from the mechanical properties of the constituent materials. These theories will later be compared with experimental values.

Going back to the present paper, several kinds of commercially available sandwich plates of CFRP-fabric skin and acrylic foam core were evaluated by means of a three-point bending to make sure of their mechanical behaviors. The effects of the span and the radius of curvature of the loading nose on the bending modulus and strength were examined systematically.

During the comparison of the present data with the above existing theories, we found there exist some discrepancies between the two. Then we tried to establish a methodology to correlate the theories and the experiments; this is a primary subject of this paper.

2. EXPERIMENTAL PROCEDURE

2.1. Test specimen

In the present study, three kinds of 4-harness satin woven carbon cloths of T300, T700s or M40 with 12000 filaments in a tow were used to make each prepreg sheet for skin. An epoxy resin was used for the matrix of skin and the nominal volume fraction of fiber was 50%. The core material was acrylic foam the inflation ratio of which was mainly 10-times, whereas 15-times inflation core was also used for T700s skin sandwich. These raw materials of skin prepreg and core material were co-cured at 80°C by a hot press. Geometries of the test specimen are shown in Table 1 where h, t_f, and t_c are the total thickness, the thickness of the skin and the thickness of core, respectively. From these panels, test coupons of width $b = 15$ mm with various length were prepared. The number of test samples was 4–5 for each case.

2.2. Properties of constituent materials

Figure 1 demonstrates the stress–strain curves of each skin CFRP material and Table 2 summarizes Young's modulus and the tensile strength. Although tensile

Table 1.
Geometries of test specimens

Skin	T300	T700s	M40
h (mm)	14.4	13.6	13.5
t_f (mm)	1.0	0.6	0.6
t_c (mm)	12.4	12.4	12.3

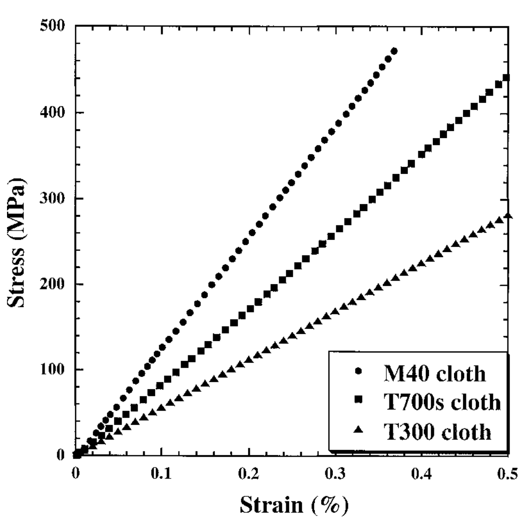

Figure 1. Representative tensile stress–strain curves of the skin materials.

Table 2.
Mechanical properties of CFRP skins

Skin	T300	T700s	M40
Young's modulus E_f (GPa)	56.5	89.5	131
Tensile strength (MPa)	597	1645	541

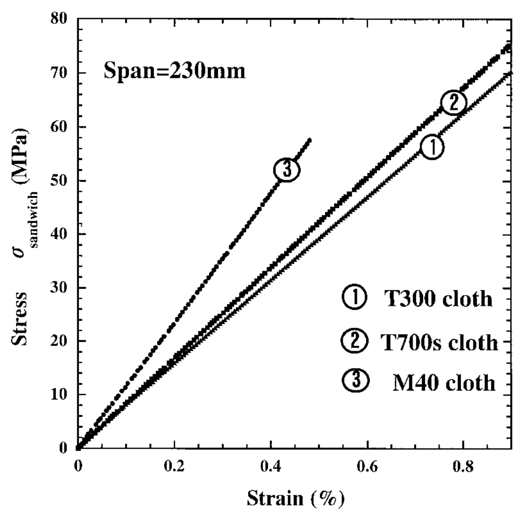

Figure 2. Representative tensile stress–strain curves of the sandwich coupons.

tests in the direction of 45° were also done to obtain the shearing modulus of the skin, data are omitted here. Mechanical test data of core materials are also discarded in this paper.

The tensile stress–strain curves of sandwich plates were almost linear as demonstrated in Fig. 2 where $\sigma_{sandwich}$ indicates the applied load divided by the total cross sectional area of the sandwich coupon.

2.3. Three-point bending

To examine the effect of the span on the bending rigidity and strength, the span was changed from 115 mm to 500 mm for T300 skin sandwich and for T700s and M40 skin, the span was 150, 230, or 500 mm. The radius of curvature of the loading nose (R) and the supporting nose (r) were fixed to $R = 6$ mm and $r = 3$ mm, respectively.

The effects of the radius of curvature of the loading nose on the bending modulus and strength were also examined against T300 skin sandwich only. In this case, the span was fixed to either $L = 230$ mm or $L = 500$ mm and the radius of curvature of the loading nose was varied to $R = 6$, 12, 24, 50, and 100 mm.

In all cases, the bending strength was calculated by

$$\sigma_{max} = \frac{3P_{max}L}{2bt^2}, \tag{10}$$

in accordance with the elementary beam theory.

3. RESULTS AND DISCUSSION

3.1. Effect of span

In this chapter, only results of three point bending will be described. Figure 3 shows the relation between the span, L, and the equivalent bending rigidity. Lines calculated from modified beam theory are also drawn, where E_f represents the experimental values shown in Table 2. For G_c, a catalog value of 27 MPa [5] was used. According to this figure, experimental values are larger than the theoretical values. This point will be discussed later.

Figure 4 is the bending strength vs. span for T300 skin sandwich. The bending strength was almost constant when the span L was larger than 300 mm whereas it decreased with decrease in the span in the region of $L < 300$ mm. This result indirectly shows that for a short-span beam such as $L < 300$ mm, the failure pattern might be different from the bending failure. This point will be discussed in the next article.

3.2. Effect of radius of curvature of the loading nose

Bending tests were conducted also by varying the radius of curvature of the loading nose (indentor), where the span was either (a) $L = 230$ mm or (b) $L = 500$ mm.

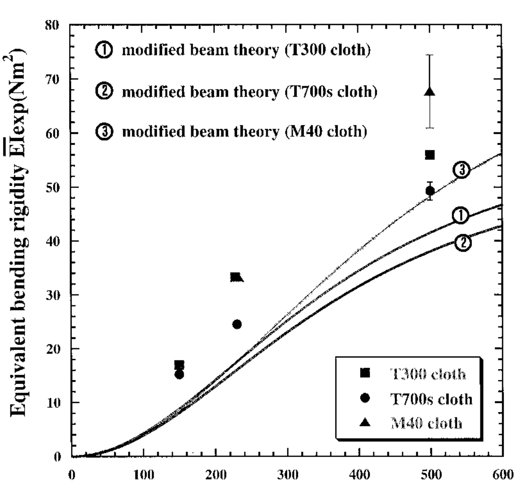

Figure 3. Equivalent bending rigidity *vs.* span.

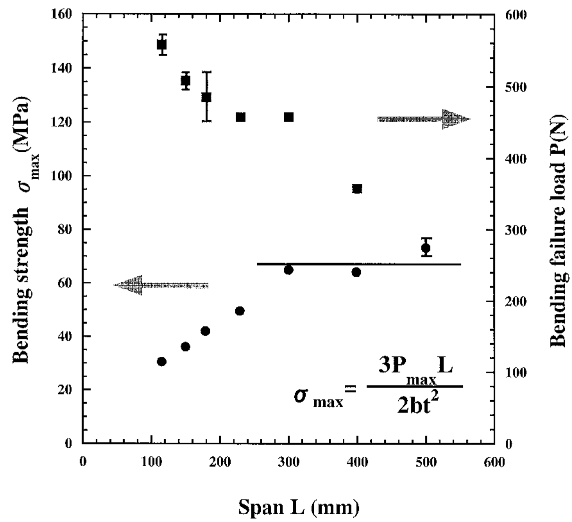

Figure 4. Span *vs.* bending strength (T300).

This test was conducted with the T300 skin sandwich only. Dots in Fig. 5 are the results; square symbols will be discussed later. According to Fig. 5, the failure load increased slightly with increase in the radius of the indentor. This suggests that a relatively large loading nose is desirable for the bending test of sandwich beams.

As was shown in Fig. 4, a bending failure might not have occurred for short-span coupons. To clarify this point, a kind of indentation test was conducted where the sandwich coupon was pressed on a rigid and flat plate using the same loading nose as those used for the bending test. Figure 6 is the schematic view of the indentation test and the square symbols of Fig. 5 are the results.

One notable point is that, in the case of short span, the failure load by means of 3-point bending was almost the same as that of the indentation test (see Fig. 5a). This indicates that for $L = 230$ mm, some other failure pattern such as crush of core took place prior to a bending failure. On the other hand, the bending failure is likely to have occurred for $L = 500$ mm because the bending failure load was smaller than that of the indentation test (Fig. 5b). Thus, it was again confirmed that a bending test of short span is not desirable.

3.3. The effect of core

Figure 7 shows the effect of core materials on the equivalent bending rigidity of T700s-skin sandwich. As was expected, the rigidity decreased when the core of 15-times inflation ratio was used, although quantitative discussion is rather difficult at present.

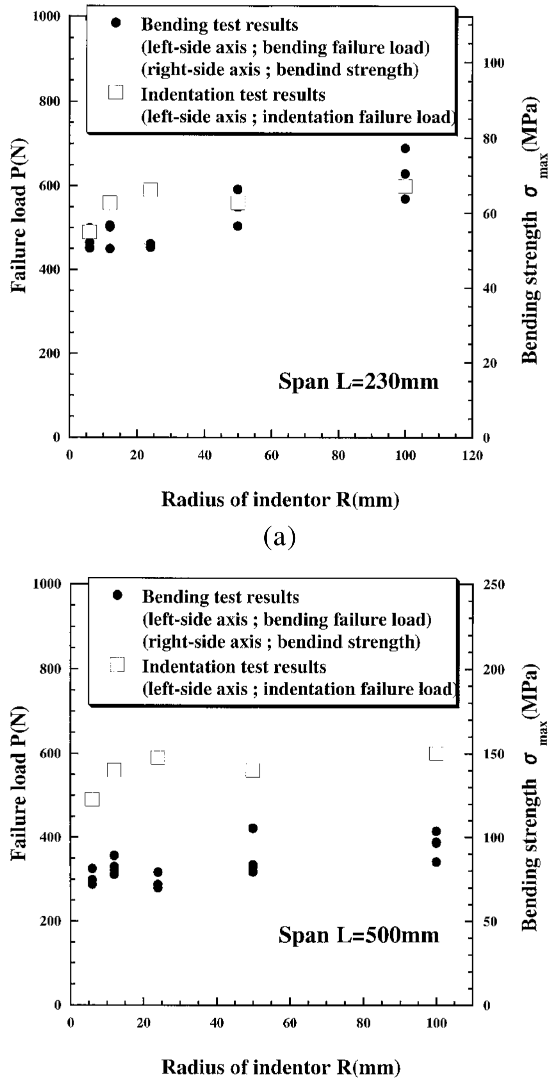

Figure 5. Radius of indentor *vs*. failure load. (a) $L = 230$ mm, (b) $L = 500$ mm.

3.4. Methodology of correlating theory and experiment

Going back to Fig. 3, the experimental values were larger than those predicted by means of the modified beam theory. This difference might be due to an inaccurate G_c value used in the theory, because it is quite difficult to measure G_c in a precise manner, as was described earlier.

Our alternative idea is as follows. Choosing an arbitrary span, for example, $L = 230$ mm, the shearing modulus, G_c, was inversely calculated from equations (2) and (4) so that the calculated bending rigidity meets with the experimental value.

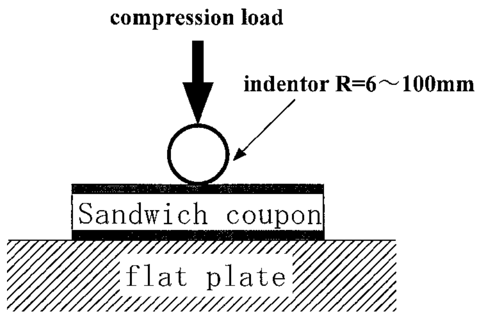

Figure 6. Effect of core materials on the equivalent bending rigidity.

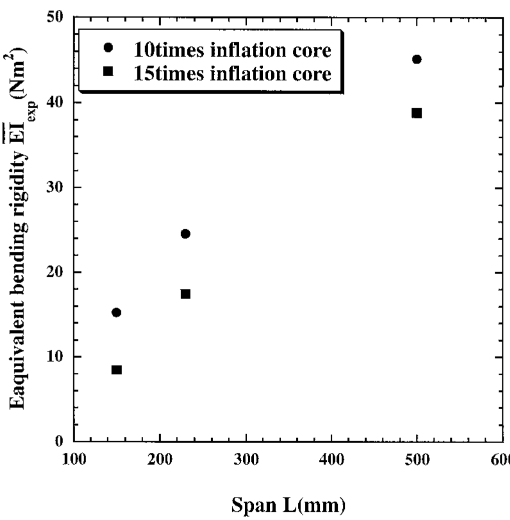

Figure 7. Effect of core materials on the equivalent bending rigidity.

In other words, one point collocation was done. Figure 8 shows the case of T700s where the G_c value was assumed to be 51 MPa. The theoretical values agreed very well with the experimental values. That is, if we once conduct a bending test at an arbitrary span length, we can estimate the flexural rigidity at any span length by the aid of the modified beam theory of equations (2) and (4); this is a new methodology derived here.

This logic holds to the cases of T300 or M40 face material, too. However, G_c values necessary to match the theory to experiment were different from each other ($G_c = 83$ MPa for T300, $G_c = 65$ MPa for M40); nevertheless, the same core material was used in the experiments.

In all cases, the inversely calculated G_c values were larger than the catalog value. Because the present sandwich is co-cured, some amount of excess resin in the skin penetrated into the core, which might increase the average G_c. According to our observation, however, the depth of resin penetration into the core was less than 1 mm; this is not enough to explain the increase of G_c. Another possible reason

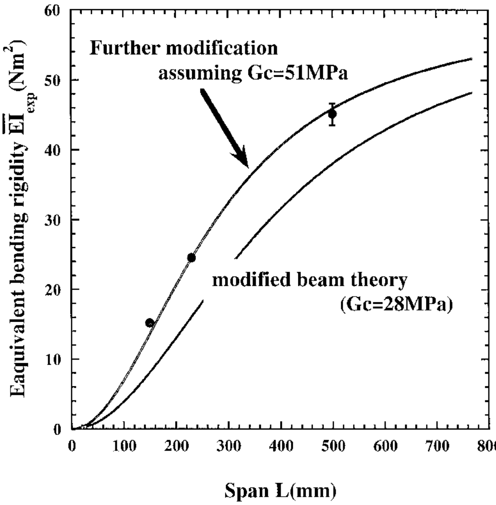

Figure 8. Modification of modified beam theory (T700s).

is that the behavior of core in the sandwich plate might be different from that of individual core, due to some geometrical restriction such as Poisson's effect. But this is still no more than a guess.

As for the difference of inversely calculated G_c among three types of skin, everything was left for future discussion.

4. CONCLUSIONS

In the present paper, we conducted a series of bending tests of CFRP skin/foamed core sandwich beams. Effects of span and radius of curvature of the loading nose on the bending rigidity and bending strength were evaluated systematically and data were accumulated. The effect of core material on the bending rigidity was also demonstrated. Because it was rather difficult to measure the shearing modulus of core itself, there were some discrepancies of bending rigidity between experiment and theory. To correlate experiment and theory, we proposed a methodology of data reduction, which was successfully applied to the present case although some points were left unclear.

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