EVALUATION OF A VACUUM-INDUCED CONCENTRATED-LOAD SANDWICH TESTER

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EVALUATION OF A VACUUM-INDUCED CONCENTRATED-LOAD SANDWICH TESTER[1]

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Summary

This report presents the results of work done to evaluate the performance of a testing device for use in determining the quality of panels of sandwich construction. The device was tried on several sandwich constructions made with both good and poor facing-to-core bonds. The performance of the tester on the various constructions is discussed, and advantages and disadvantages are pointed out. A theoretical derivation of stresses induced by the tester is included in the appendix, and the application of formulas for core shear stress are discussed. The tester appears to be a fairly reliable detection device on flat sandwich constructions having aluminum honeycomb, glass-cloth honeycomb, or balsa cores. However, when used on panels having cores of cotton honeycomb, cellular cellulose acetate, or waffle type, poor bonds could not be detected. The sensitivity of the tester is about equal to the sensitivity of flexure tests for determining poor bonds, but neither is as sensitive as the flatwise tension test.

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[2] Maintained at Madison, Wis., in cooperation with the University of Wisconsin.

Introduction

A preliminary evaluation of a vacuum-induced concentrated-load sandwich tester was presented in the first progress report.[3] The purpose of the work reported here was to evaluate more completely the performance of the tester by means of values obtained from its use, and to correlate the results with flexure and tension tests on sandwich panels of several typical constructions.

Fabrication of Test Panels

Eleven sandwich constructions composed of some of the typical combinations of aluminum alloy and of glass-cloth-reinforced plastic facings on aluminum honeycomb, end-grain balsa, glass-cloth honeycomb, cellular cellulose acetate, cotton honeycomb, and waffle-type cores were fabricated for the test work. All panels were flat and approximately 24 inches wide and 36 inches long.

Adhesives, resins, and fabricating techniques were typical of those normally employed by the aircraft industry in making similar sandwich constructions. Two panels, composing a matched set, were made for each construction. One panel of each set had normal high-quality bonds between both facings and the core, whereas the second panel of each set had a normal bond on one side and a relatively weak bond between the facing (carrying the panel number) and the core on the opposite side. An attempt was made in each set to confine the tensile strength of the poor bond to between 25 and 50 percent of that of the normal bond; however, with some constructions, this was found to be impracticable. The fabrication details of each of the 11 sets of panels are given in appendix I.

Description and Operation of the Sandwich Tester

The tester consists of a dish-shaped casting, approximately 10 inches in diameter, with a rubber gasket or washer around the outside rim. Figure 1 shows an external view of the device. The gasket is used to form a pressure seal between the tester and a sandwich panel. An internal view of the tester

[3]Preliminary Evaluation of a Vacuum-Induced Concentrated-Load Sandwich Tester. Forest Products Laboratory Report No. 1832-A. June 1952.

(fig. 2) shows a central rubber-covered steel foot that is pressed against the panel as the dish-shaped cavity is evacuated. Foot sizes of different diameters from 1 to 2 inches in steps of 1/4 inch are supplied so that various ratios of compression and shear can be applied. The foot is fastened by means of a convenient snap-on fitting to a threaded bolt that extends through the casting. A vacuum gage is attached to the casting to measure the applied load.

In use, the tester is operated by placing it on a sandwich panel, adjusting the position of the foot by turning the threaded bolt until the panel contacts both the foot and the rubber gasket, and drawing a partial vacuum on the casting until failure occurs or until some desired proof load, determined by the setting on the poppet valve, is reached.

Exploratory trials of the tester showed that the rubber gasket may be deformed so much by the deflection of the panel that the casting makes contact with the sandwich. If this occurs, continued evacuation merely places a small additional uniform load on the sandwich with little further deflection. In order to indicate rim contact, a buzzer was introduced in a circuit between the casting and sandwich facing. If a nonconductive facing material was on the sandwich being tested, thin strips of metal foil were placed on the surface and used in the circuit.

Test Procedure

The locations of the test areas and test specimens are shown in figure 3.

Tests Nos. 1 and 3 with the tester were made with the instrument located on the numbered side of the panel, whereas test No. 2 was made on the opposite side. The poor bond in each set was located under the numbered side of the panel. In all cases the tester was attached to the bottom side of the panel (as set up for test), leaving the top side visible for observations during the test and for deflection measurements as shown in figure 4. A mercury manometer was used to measure the partial vacuum applied during test. A 2-inch-diameter foot was used for all tests except those on sets 5 and 6, on which it was found necessary to use a 1-inch-diameter foot to cause the panels to fail.

It was found that the rubber gasket supplied on the tester allowed for only a 0.1-inch deflection of the panel being tested. As this was insufficient for some of the more flexible panels, a wider gasket, permitting a 0.2-inch deflection before making rim contact (which must be avoided) was substituted. The new gasket, being somewhat stiffer and apparently having a higher coefficient of friction, did not slide uniformly during loading unless the surface of the panel was lubricated with water; therefore each test area was wiped with a wet cloth before testing.

The flexure specimens (three in each direction) and the section for tension tests were cut from the panel after completing the tester evaluations. In two flexure tests of each group of three, the numbered facing was up, and in the third it was on the lower side. Flexure specimens were of varying lengths, depending on the thickness and construction. In all cases two-point loading (on the quarter points) was employed.

Eight, and in some cases 16, individual tension tests normal to the panel surface were made from each section reserved for the evaluation of tensile strength. Cubes of aluminum were bonded to the facings with an epoxy-resin adhesive cured at 200° F. under pressure of 15 pounds per square inch for 2 hours. The specimen was tested in a universal-joint arrangement.[4]

Test Results

In recording the performance of the tester, the deflections (opposite the foot) at the center of the circular test area were plotted directly against the partial vacuum. Typical load-deflection curves for two widely different sandwich panels are shown in figure 5, A and 5, B.

By using the partial vacuum at failure of the panel, a calculated maximum shear stress in the core at failure was determined for each test by the following formula:

$$\tau_2{}^{5} = \frac{13.9q}{c + f}$$ for the 2-inch-diameter foot.

$$\tau_1{}^{5} = \frac{28.6q}{c + f}$$ for the 1-inch-diameter foot.

where τ = core shear stress based upon assumption of uniform load under foot. (p. s. i.)

q = partial vacuum. (p. s. i.)

c = core thickness. (in.)

f = facing thickness. (in.)

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[4]See apparatus in figures 6, 7, and 8 of Forest Products Laboratory Report No. 1556. Methods for Conducting Mechanical Tests of Sandwich Construction at Normal Temperatures.

[5]This formula is obtained from formula (52) appendix II, by assuming no core shear deformation ($\alpha = \infty$) and thus causing a slight error (less than 2 percent for extreme constructions).

The shear values for each test, the maximum core shear stress in each flexure test, and the average of the tensile values for each panel are presented in table 1 for the 11 sets of panels tested. For purposes of comparison figure 6 shows a graph of tester shear-stress values plotted against the lowest individual flexural shear stress. (The lowest value of flexural shear stress was chosen on the assumption that the area tested by the tester would be no stronger and, also, that failure in the weakest direction would precipitate complete failure.)

Discussion of Test Results

A certain degree of correlation can be observed between calculated shear values obtained with the tester and those obtained by flexure tests by observing the position of the points plotted in figure 6. The dashed line in this figure represents the average of all the points, as determined by least squares, and has a slope of 1.8, which is approximately 80 percent greater than the theoretical values for a uniformly loaded foot. This large difference between estimates in tests by the tester and by flexure is due in part to the use of the lowest flexure value as the abscissas of each point. Then, too, one would expect lower strengths from the flexure test because the specimen is subjected to a constant shear force between load points and outer supports and would presumably fail at the weakest point in one or the other of these intervals. With the tester high shear forces occur only in the region near the rim of the foot.

With some cores, such as cellular cellulose acetate, waffle type, and cotton-cloth honeycomb, it was difficult to determine the failure load with the tester because of the extreme deflection with no abrupt indication of failure. Figure 5, B shows a typical load-deflection curve of this type for a panel having a cotton-cloth honeycomb core. The residual deflection shown indicates that the stresses imposed by the tester had exceeded the proportional limit of the sandwich, although there was no evidence of failure other than a slight cracking sound that persisted throughout most of the test. This characteristic was also typical of the construction having a glass-cloth honeycomb core. A similar test on a sandwich having a cellular cellulose acetate core produced a similar load-deflection curve and a residual deflection, but with no audible evidences of failure.

By again referring to table 1, it may be seen, from a comparison of the strength values of normal and poorly bonded panels, that a reduction in the quality of the bond generally caused a greater reduction in tensile values than for either tester or flexural shear values. This fact is presented graphically in figure 7, which has average panel strength values for well-bonded panels plotted against those for poorly bonded panels. The position of the points and lines fitted by least-squares methods to each type of test with reference to the line representing equal strength of well-bonded and poorly bonded panels, shows that the tester usually rated well-bonded panels as being just slightly better (approximately 10 percent) than poorly bonded panels; that the flexure test usually rated the well-bonded panels as being approximately 17 percent better than the poorly bonded panels, and that the tensile test generally gave well-bonded panels 56 percent greater strength than poorly bonded panels. (The line representing tensile values does not appear to fit the points because the least-squares method, in effect, weights the three highest values.) The relation among tester, flexure, and tensile values is also shown on a percentage basis in table 1 for each set of panels.

Thus it appears that the tester is a less sensitive means of detecting subnormal bonds in sandwich panels than is a tension test. Tester and flexure-test values were affected somewhat similarly by reduced quality; therefore, the sensitivity of the tester appears to compare fairly well with the sensitivity of flexure tests.

A review of the tester values on poorly bonded panels reveals that it makes little or no difference in the values obtained whether the poor bond is on the side under the tester or on the opposite side. The type of failure, however, appears to be slightly different, there being a tendency to produce bond failures over a wider area when the poor bond is on the side opposite the tester.

A study of the variation in tester shear values in comparable groups of three tests shows that the degree of variation depends largely on the uniformity of the core material. The greatest variation in tester values was found to be in the well-bonded panel of set 5, which had a 1/2-inch-thick end-grain balsa core. In general, the most uniform groups of tester values (where abrupt failures in the core were observed) were recorded for aluminum honeycomb cores. However, even with this relatively small variation in individual values, it would be difficult to select a proof load at such a level that panels having slightly submarginal bonds could be failed, and therefore rejected, without danger of damage to well-bonded panels. As an example, it would be impossible to set the tester at a proof load that would cause consistent failure in the poorly bonded panels of set 1 without failure when applied to the well-bonded companion panels, because of the overlap in the two groups of values, although the average tensile strength of the poorly bonded panel is only about one-half that of the well-bonded panels. It would, however, be possible to differentiate between these two panels by proof-loading to a calculated shear stress of 255 pounds per square inch, which would have caused only one failure (sufficient for rejection) in the weaker panel.

With a core material relatively weak in shear and having a wide variation in quality (such as end-grain balsa), the dependability of the tester in locating poorly bonded material would be questionable. As an example, in set 4, although the average tensile strength of the bonds in the subnormal panel was only 69 percent of normal, all three tester values on the poorly bonded panel were higher than the highest value obtained from the three tests on the well-bonded panel. Likewise, in set 5, the excessive variation in the tester values on the normal panel produced one low value that was considerably (30 percent) lower than the lowest value from tests on the poorly bonded panel. (Tensile strength 82 percent of normal.)

In use on production panels many more tests would be made per panel, thus greatly expanding the range of ultimate tester values and increasing the chance of finding areas of poor-quality bonds. The reliability of the tester would therefore depend largely on the magnitude of the proof load applied by the tester, which must be controlled so that areas that will not withstand the minimum allowable shear stress used in the design of the part will fail under the foot of the instrument.

Without calculating stresses under the tester foot, it should be possible to use the tester by correlating values directly with flexure tests. As an example, let it be assumed that the design of a flat sandwich panel loaded in bending (such as a floor) requires a minimum shear value of 150 pounds per square inch in the LR plane, after allowance for an applicable factor of safety. Let the floor be designed with a core having a shear strength of slightly above 150 pounds per square inch. Matched flexure-test values (in the LT and LR direction) and tester values are then obtained on a production part (or a panel made under similar conditions) that yield the assumed results in the following tabulation. (It is assumed that from a few exploratory tests a foot of sufficient size to eliminate core compression failures had been selected.)

Partial vacuum recorded on tester at failure	Flexure shear	
	LT	LR
Inches Hg	P. s. i.	P. s. i.
24.1	295	213
19.4	208	180
23.4	296	172
20.8	240	207
20.5	297	198
18.8	240	197
Average 21.2	263	194

If reasonable matching and distribution are assumed, then the tester would be adjusted so that it would apply a maximum vacuum of about 16 inches on mercury ($21.2 \times \frac{150}{194}$), which corresponds to a shear value in the LR plane of 150 pounds per square inch (based on a straight-line relation between tester and shear stresses), and should produce failure on any spot in the panel having a shear strength of less than the required minimum.

Conclusions

Poor bonds between facings and core in flat panels having cores of aluminum-foil honeycomb, glass-cloth honeycomb, or balsa can be detected by proper use of the tester, and it makes little or no difference on which side the poor bond occurs.

Use of the tester on sandwich constructions having certain cores, such as cotton honeycomb, waffle type, and cellular cellulose acetate, that do not fail suddenly but continue to carry large loads after small local failures, may result in inability to detect failures, even of poorly bonded panels. Poor bonds in such panels therefore would be difficult to detect by use of the tester.

The instrument is approximately as effective in detecting poorly bonded constructions as is the flexure test (designed to cause core shear failure), but is not nearly so sensitive to poor bonds as the tension test (flatwise).

APPENDIX I

Detailed Description of Sandwich Panel Fabrication

Set No. 1

Facings: 0.012-inch, 24 ST clad aluminum, etched in a sulfuric acid-sodium dichromate bath.

Core: (75) 1/2-inch aluminum honeycomb, of 0.002-inch, 3 SH foil formed to 3/16-inch cell size; 5.4 pounds per cubic foot. Foil ribbons parallel to 24-inch dimension.

Normal bonds: Adhesive (33), a high-temperature-setting formulation of thermosetting and thermoplastic resins. 5 grams per square foot on facings (brush spread). 15 grams per square foot on each side of core (roller spread).
Poor bond: 3 to 4 grams per square foot on facing, none on core.
Open assembly: 24 hours.
Precure: Facings and core, 150° F. for 60 minutes.
Assembly technique: One sheet of chipboard and cellophane on both sides as cauls.
Cure: Hot press, 300° F., 60 minutes, pressure of 25 pounds per square inch. Press opened temporarily after 1/2 to 1 minute, cooled under pressure in press.

Set No. 2

Same as set No. 1 except facings were of 0.020-inch thickness.

Set No. 3

Same as set No. 1 except facings were of 0.032-inch thickness.

Set No. 4

Facings: 0.012-inch, 24 ST clad aluminum, etched in a sulfuric acid-sodium dichromate bath.
Core: 1/4-inch end-grain balsa, 9 to 11 pounds per cubic foot.
Normal bonds: Adhesive (34), a high-temperature-setting, two-component adhesive with a thermosetting liquid resin and a thermoplastic powder. 9 grams per square foot plus powder on facings and core.
Poor bond: 9 grams of liquid per square foot on facing, no powder; none on core.
Open assembly: 36 hours.
Assembly technique: One sheet of chipboard and cellophane on both sides as cauls.
Cure: Hot press, 260° F., 60 minutes, pressure of 100 pounds per square inch. Press opened temporarily after 1/2 to 1 minute, removed hot.

Set No. 5

Same as set No. 4 except facings were of 0.020-inch thickness, and core of 1/2-inch thickness.

Set No. 6

Same as set No. 4 except facings were of 0.032-inch thickness, and core of 1/2-inch thickness.

Set No. 7

Facings: 0.020-inch, 24 ST clad aluminum, etched in a sulfuric acid-sodium dichromate bath.
Core: 1/2-inch cotton-cloth honeycomb, 7/16-inch cell size, nominal density, 4 pounds per cubic foot. Ribbons parallel to 36-inch dimension.
Normal bonds: Adhesive (33), a high-temperature-setting formulation of thermosetting and thermoplastic resins. 5 grams per square foot on facings (brush spread). 15 grams per square foot on each side of core (roller spread).
Poor bond: 5 grams per square foot on facing, 1 to 2 grams per square foot on each side of core.
Open assembly: 24 hours and precured at 150° F. for 60 minutes.
Assembly technique: One sheet of chipboard and cellophane on both sides as cauls.
Cure: Hot press, 300° F., 45 minutes, pressure of 30 pounds per square inch. Press opened temporarily after 1/2 to 1 minute, cooled under pressure in press.

Set No. 8

Facings: 0.032-inch, 24 ST clad aluminum, etched in a sulfuric acid-sodium dichromate bath.
Core: 1 inch PNL - Forest Products Laboratory corrugated-paper honeycomb, 30-pound kraft paper, 10 percent resin, sodium silicate bonded, 2.7 pounds per cubic foot. Ribbons parallel to 36-inch dimension.
Normal bonds: Adhesive (33), a high-temperature-setting formulation of thermosetting and thermoplastic resins. 5 grams per square foot on each facing (brush spread). 15 grams per square foot on each side of core (roller spread).
Poor bond: 5 grams per square foot on facing, 1 to 2 grams per square foot on core.
Open assembly: 24 hours and precured at 150° F. for 60 minutes.
Assembly technique: One sheet of chipboard and cellophane on both sides as cauls.
Cure: Hot press, 300° F., 45 minutes, pressure of 10 pounds per square inch. Press opened temporarily after 1/2 to 1 minute, cooled under pressure in press.

Set No. 9

Facings: 0.012-inch 24 ST clad aluminum, etched in a sulfuric acid-sodium dichromate bath.

Core: 1/4-inch cellular cellulose acetate, 6 pounds per cubic foot. Strips parallel to 36-inch dimension.

Normal bonds: Adhesive (33 plus 40), a high-temperature-setting formulation of thermosetting and thermoplastic resins plus a room-temperature-setting resorcinol. 4 to 5 grams of adhesive 33 per square foot on facings (brush spread) and cured at 300° F., then lightly sanded. Adhesive 40: 25 grams per square foot on facings (brush spread), none on core.

Poor bond: 10 grams of adhesive 40 per square foot on facing primed with cured adhesive 33, none on core.

Open assembly: 30 minutes.

Assembly technique: One sheet of chipboard and cellophane on each side as cauls.

Cure: Cold press, room temperature, pressure of 14 pounds per square inch, about 48 hours.

Set No. 10

Facings: 10 plies of 112 - 114 glass cloth, parallel laminated.

Core: (34) 1/4-inch nylon phenolic impregnated glass-cloth honeycomb, 1/4-inch cell size, 8 pounds per cubic foot. Ribbons parallel to 36-inch dimension.

Normal bonds: Resin (2), a high-temperature-setting, low-viscosity, laminating resin of the styrene monomer, polyester type. About 50 percent wet-weight impregnation on facings, each sheet impregnated separately by hand. 10 grams per square foot on core (dipped).

Poor bond: Premolded 9-ply parallel laminate, sanded on one side, bonded to core using one sheet of cloth impregnated to 45 percent wet weight; no resin applied to the core.

Open assembly: 30 minutes.

Assembly technique: Cellophane-covered aluminum cauls and one sheet of chipboard on both sides.

Cure: Hot press, 250° F., 60 minutes, pressure of 15 pounds per square inch, removed hot.

Set No. 11

Facings: 10 plies of 112 - 114 glass cloth, parallel-laminated.

Core: (57), waffle type 0.280- to 0.300-inch thick, 11 pounds per cubic foot.

Normal bonds: Resin (2), a high-temperature-setting, low-viscosity, laminating resin of the styrene monomer, polyester type. About 50 percent wet-weight impregnation on facings, each sheet impregnated separately by hand. 10 grams per square foot on core (dipped).

Poor bond: Premolded nine-ply parallel laminate, sanded on one side, bonded to core by using one sheet of cloth impregnated to 50 percent wet weight, about 2 grams per square foot on core (dipped).

Open assembly: 30 minutes.

Assembly technique: Cellophane-covered aluminum cauls and one sheet of chipboard on both sides.

Cure: Hot press, 250° F., 60 minutes, pressure of 15 pounds per square inch, removed hot.

APPENDIX II

The deflection and the stresses induced in the facings and in the core by the tester have been analyzed by the use of formulas derived in U. S. Forest Products Laboratory Report No. 1828.[6] In that report the core and facing materials are assumed to be isotropic and, consequently, the present results are limited to this special case.

In order to carry out the analysis, the distribution of load over the foot of the tester must be specified. An attempt has been made to keep the distribution uniform by covering the foot with a rubber gasket, and it is possible that at small vacuum loads it actually is quite uniform. At large vacuum loads, however, the tester forces the panel to bend, and the load is possibly concentrated more heavily at the rim of the foot than at the center. On the basis of these considerations, the analysis has been carried out for two assumed distributions, namely: (1) a load uniformly distributed over the foot, and (2) a load concentrated at the rim of the foot. For a given vacuum a uniform distribution yields higher predicted shear stress in the core and lower bending stresses in the facings than a load concentrated at the rim of the foot. It is expected that if the true distribution of load over the foot of the tester could be determined, the results would be intermediate between those of the two extreme cases considered.

For the analysis of the stresses induced in a panel it is necessary to specify the edge conditions at the rim of the tester. The given results are based on the assumption that the tester is applied to a panel of infinite extent. These results

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[6] Ericksen, W. S. The Bending of a Circular Sandwich Plate under Normal Load. Reference to equation n in this report is made by the symbol (1828-n).

have been found to differ little from those obtained on the assumption of clamping at the outer rim. However, both of these cases have a symmetry that will not usually exist in practice, and the effects of nonsymmetry in the panel remain unknown.

Notation

a: inner radius of the tester gasket

b: radius of the foot, as subscript denotes a quantity associated with bending deflection

c: thickness of the core

d: outer radius of the panel

$$E = \frac{E_f}{1 - \gamma^2}$$

E_f : Young's modulus of the facing material

f, f': thicknesses of the facings

G: shear modulus of the core material

$$I = I_m + I_f$$

$$I_f = \frac{f^3 + f'^3}{12}$$

$$I_m = \frac{ff'}{f + f'}\left(c + \frac{f + f'}{2}\right)^2$$

$I_o(\alpha r)$, $I_1(\alpha r)$, $K_o(\alpha r)$, $K_1(\alpha r)$: modified Bessel functions

Q_r: shear stress resultant on a circumferential section of the sandwich

q: applied vacuum (p. s. i.)

r: radial coordinate

s: a subscript denoting quantity associated with shear deformation

w: deflection

$$\alpha = \sqrt{\frac{2GI}{EcfI_f}}$$

σ: direct radial stress in facing

τ: shear stress in core

γ: Poisson's ratio of facing material

Analysis

In Report No. 1828, equation 10, the shear stress resultant on a concentric circumferential section, Q_r, of a normally loaded circular sandwich panel is expressed in the form:

$$Q_r = \frac{qr}{2} + \frac{p}{r} \qquad (1)$$

Referring to figure 8, which shows a radial section of a panel covered by the tester, it is seen that the shear stress resultants in the regions indicated are given as follows. In region 1, that covered by the foot,

$$Q_{r1} = (q_1 - q)\frac{r}{2} \; ; \qquad (2)$$

in region 2, the part between the rim of the foot and the outer rim of the tester,

$$Q_{r2} = - \frac{qr}{2} + \frac{q_1 b^2}{2r} \; ; \qquad (3)$$

and in region 3, the portion of the panel not covered by the tester,

$$Q_{r3} = 0 \qquad (4)$$

Now, since the total load on the foot is equal and opposite to the total vacuum load,

$$q_1 b^2 = q a^2 \qquad (5)$$

Therefore from (2)

$$Q_{r1} = q\left(\frac{a^2 - b^2}{b^2}\right) \frac{r}{2} \, , \qquad (6)$$

and from (3),

$$Q_{r2} = - \frac{qr}{2} + \frac{qa^2}{2r} \qquad (7)$$

In the section entitled "Results and Discussion" in Report No. 1828, the deflection is expressed in the form (1828-R-1)[6]

$$w(r) = w_b(r) + w_s(r) \tag{8}$$

For a shear stress resultant of the form (1),

$$w_b(r) = -\frac{1}{EI}\left[\frac{qr^4}{64} + \frac{pr^2}{4}\left\{\log r - 1\right\} + \frac{Ar^2}{2} + B \log r + H_b^*\right] \tag{9}$$

An expression of this form is assumed for each region indicated in figure 1, and subscripts are used to associate the expression with the region. By comparison of (6) and (1) to obtain appropriate expression for q and p,

$$w_{b1}(r) = -\frac{1}{EI}\left[\frac{q(b^2 - a^2)r^4}{64b^2} + \frac{A_1 r^2}{2} + B_1 \log r + H_{b1}\right],$$

$$0 \leq r \leq b \tag{10}$$

by comparing (7) with (1),

$$w_{b2}(r) = -\frac{1}{EI}\left[-\frac{qr^4}{64} + \frac{qa^2 r^2}{8}\left\{\log r - 1\right\} + \frac{A_2 r^2}{2} + B_2 \log r + H_{b2}\right],$$

$$b \leq r \leq a \tag{11}$$

and finally from (4) and (1),

$$w_{b3}(r) = -\frac{1}{EI}\left[\frac{A_3 r^2}{2} + B_3 \log r + H_{b3}\right],$$

$$a \leq r \leq d \tag{12}$$

The constants A_i, B_i, and H_{bi}, $i = 1, 2, 3$ are determined by conditions imposed at the center, at the juncture of the regions, and at the outer rim of the plate. These conditions are the following:

$$\left.\begin{aligned}
\frac{dw_{b1}(r)}{dr} &= 0 && \text{at } r = 0 \\
w_{b1}(r) &= w_{b2}(r) && \text{at } r = b \\
\frac{dw_{b1}(r)}{dr} &= \frac{dw_{b2}(r)}{dr} && \text{at } r = b \\
\frac{d^2w_{b1}(r)}{dr^2} &= \frac{d^2w_{b2}(r)}{dr^2} && \text{at } r = b \\
w_{b2}(r) &= w_{b3}(r) = 0 && \text{at } r = a \\
\frac{dw_{b2}(r)}{dr} &= \frac{dw_{b3}(r)}{dr} && \text{at } r = a \\
\frac{d^2w_{b2}(r)}{dr^2} &= \frac{d^2w_{b3}(r)}{dr^2} && \text{at } r = a \\
\frac{dw_{b3}(r)}{dr} &= 0 && \text{at } r = d
\end{aligned}\right\} \qquad (13)$$

While it is not necessary to specify the value of d, the outer radius of the plate, considerable simplification in the formulas results by taking either d = a or d infinite. Comparison of the results from these two cases indicates that the stresses at the rim of the foot remain essentially constant as d is increased from a to infinity. The results will therefore be given for d infinite. For this case the deflections in regions (1) and (2) obtained by imposing conditions (13) to (10), (11), and (12) are

$$w_{b1}(r) = -\frac{q}{64EI}\left[\frac{(a^2 - b^2)r^4}{b^2} - 4a^2(b^2 + 2r^2)\log\frac{a}{b} + 5a^2(a^2 - b^2)\right]$$

$$0 \leq r \leq b \qquad (14)$$

$$w_{b2}(r) = -\frac{q}{64EI}\left[a^4 - r^4 + 4a^2(a^2 - r^2) + 4a^2(b^2 + 2r^2)\log\frac{r}{a}\right],$$

$$b \leq r \leq a \qquad (15)$$

When the shear stress resultant is of the form (1) the component of deflection $w_s(r)$ is given by the expression (1828-R-8)

$$w_s(r) = \frac{I_m}{EI I_f \alpha^2}\left[\frac{qr^2}{4} + p\log r + \frac{D}{\alpha}I_o(\alpha r) + \frac{F}{\alpha}K_o(\alpha r) + H_s\right] \qquad (16)$$

Subscripts are again used to associate these functions with regions in figure 1. By comparison of (6), (7), and (4) with (1) for the determination of q and p for the three regions, it is found that

$$w_{s1}(r) = \frac{I_m}{EI I_f \alpha^2}\left[\frac{q(a^2 - b^2)r^2}{4b^2} + \frac{D_1}{\alpha}I_o(\alpha r) + \frac{F_1}{\alpha}K_o(\alpha r) + H_{s1}\right]$$

$$0 \leq r \leq b \qquad (17)$$

$$w_{s2}(r) = \frac{I_m}{EI I_f \alpha^2}\left[-\frac{qr^2}{4} + \frac{qa^2}{2}\log r + \frac{D_2}{\alpha}I_o(\alpha r) + \frac{F_2}{\alpha}K_o(\alpha r) + H_{s2}\right]$$

$$b \leq r \leq a \qquad (18)$$

$$w_{s3}(r) = \frac{I_m}{EI I_f \alpha^2}\left[\frac{D_3}{\alpha}I_o(\alpha r) + \frac{F_3}{\alpha}K_o(\alpha r) + H_{s3}\right]$$

$$a \leq r \qquad (19)$$

The conditions by which the constants D_i, F_i, and H_{si} are determined are obtained from (13) by replacing the subscript b by s throughout. Again taking d infinite, the components (17) and (18) are evaluated in the following forms:

$$w_{s1}(r) = \frac{I_m q}{EI I_f \alpha^2}\left[\frac{(a^2 - b^2)r^2}{4b^2} - \frac{a^2}{2}\log\frac{a}{b} + \frac{a^2 I_1(\alpha b)}{\alpha b}\left\{K_o(\alpha b) - K_o(\alpha a)\right\}\right.$$

$$\left. + \frac{a}{\alpha b}\left\{\left[aK_1(\alpha b) - bK_1(\alpha a)\right]\left(I_o(\alpha r) - I_o(\alpha b)\right) - bK_1(\alpha a)\left(I_o(\alpha b) - I_o(\alpha a)\right)\right\}\right]$$

$$0 \leq r \leq b \qquad (20)$$

and

$$w_{s2}(r) = \frac{I_m q}{EI_f \alpha^2} \left[\frac{a^2 - r^2}{4} + \frac{a^2}{2} \log \frac{r}{a} \right.$$

$$\left. + \frac{a^2 I_1(\alpha b)}{\alpha b} \left\{ K_o(\alpha r) - K_o(\alpha a) \right\} - \frac{a K_1(\alpha a)}{\alpha} \left\{ I_o(\alpha r) - I_o(\alpha a) \right\} \right]$$

$$b \leq r \leq a \qquad (21)$$

The shear stress in the core is obtained from $w_s(r)$ by the formula (1828-42)

$$\tau(r) = \frac{EI_f \alpha^2}{\left(c + \frac{f + f'}{2}\right)} \frac{dw_s(r)}{dr} \qquad (22)$$

With the application of this relation, formulas (20) and (21) yield, respectively:

$$\tau_1(r) = \frac{I_m q}{I\left(c + \frac{f + f'}{2}\right)} \left[\frac{(a^2 - b^2)r}{2b^2} + \frac{a}{b} I_1(\alpha r) \left\{ aK_1(\alpha b) - bK_1(\alpha a) \right\} \right]$$

$$0 \leq r \leq b \qquad (23)$$

and

$$\tau_2(r) = \frac{I_m q}{I\left(c + \frac{f + f'}{2}\right)} \left[-\frac{r}{2} + \frac{a^2}{2r} + \frac{a^2}{b} I_1(\alpha b) K_1(\alpha r) - a I_1(\alpha r) K_1(\alpha a) \right]$$

$$b \leq r \leq a \qquad (24)$$

The component of radial stress at the outer surface of the facing $\underline{f}$ is denoted by $\sigma(r)$ and is obtained from the formulas (1828-R-14 to R-16),

$$\sigma(r) = \sigma_b(r) + \sigma_s(r) \qquad (25)$$

with

$$\sigma_b(r) = -E \left\{ \frac{I_m}{f\left(c + \frac{f + f'}{2}\right)} + \frac{f}{2} \right\} \left\{ \frac{d^2 w_b(r)}{dr^2} + \frac{\gamma}{r} \frac{dw_b(r)}{dr} \right\} \qquad (26)$$

$$\sigma_s(r) = E\left\{\frac{I_f}{f\left(c + \frac{f + f'}{2}\right)} - \frac{f}{2}\right\}\left\{\frac{d^2w_s(r)}{dr^2} + \frac{\gamma}{r}\frac{dw_s(r)}{dr}\right\} \tag{27}$$

The differential expressions in $w_b(r)$ and $w_s(r)$ necessary for the evaluations of these stress components in regions (1) and (2) of the plate are obtained from (14), (15), (20), and (21) in the forms:

$$\frac{d^2w_{b1}(r)}{dr^2} + \frac{\gamma}{r}\frac{dw_{b1}(r)}{dr} = -\frac{q}{16EI}\left[(3 + \gamma)(a^2 - b^2)\frac{r^2}{b^2} - 4(1 + \gamma)a^2 \log\frac{a}{b}\right]$$

$$0 \leq r \leq b \tag{28}$$

$$\frac{d^2w_{s1}(r)}{dr^2} + \frac{\gamma}{r}\frac{dw_{s1}(r)}{dr} = \frac{I_m q}{EII_f}\left[\frac{(a^2 - b^2)(1 + \gamma)}{2a^2b^2}\right.$$

$$\left. + \frac{a}{\alpha b}\left\{I_o(\alpha r) - (1 - \gamma)\frac{I_1(\alpha r)}{\alpha r}\right\}\left\{aK_1(\alpha b) - bK_1(\alpha a)\right\}\right]$$

$$0 \leq r \leq b \tag{29}$$

$$\frac{d^2w_{b2}(r)}{dr^2} + \frac{\gamma}{r}\frac{dw_{b2}(r)}{dr} = -\frac{q}{16EI}\left[-(3 + \gamma)r^2 + 2(3 - \gamma)a^2 - (1 - \gamma)a^2\frac{(b^2 + 2r^2)}{r^2}\right.$$

$$\left. + 4(1 + \gamma)a^2 \log\frac{r}{a}\right], \quad b \leq r \leq a \tag{30}$$

$$\frac{d^2w_{s2}(r)}{dr^2} + \frac{\gamma}{r}\frac{dw_{s2}(r)}{dr} = \frac{I_m q}{EII_f}\left[-\frac{\{r^2 + a^2 + \gamma(r^2 - a^2)\}}{2a^2r^2}\right.$$

$$\left. + \frac{a^2 I_1(\alpha b)}{\alpha b}\left\{K_o(\alpha r) - (1 - \gamma)\frac{K_1(\alpha r)}{\alpha r}\right\} - \frac{aK_1(\alpha a)}{\alpha}\left\{I_o(\alpha r) - (1 - \gamma)\frac{I_1(\alpha r)}{\alpha r}\right\}\right]$$

$$b \leq r \leq a \tag{31}$$

In the preceding analysis the load exerted by the foot has been assumed to be uniformly distributed over the area of the foot. This is an arbitrary assumption because the true distribution of the load is not known. It is, however, plausable to assume that the load is more heavily distributed near the rim of the foot than it is at the center. This suggests the assumption that the entire load is concentrated on the rim of the foot. Formulas for the deflection and stresses in the facings and core under this type of loading are obtained by the following means.

If the load is taken to be concentrated at the rim of the foot:

$$Q_{r1}(r) = -\frac{qr}{2}, \quad 0 \leq r \leq b \tag{32}$$

$$Q_{r2}(r) = -\frac{qr}{2} + \frac{qa^2}{2r}, \quad b \leq r \leq a \tag{33}$$

and

$$Q_{r3}(r) = 0, \quad a \leq r \leq d \tag{34}$$

The boundary conditions for determining $w_{bi}(r)$ and $w_{si}(r)$ are the same as those previously imposed. These are given for $w_{bi}(r)$ by (13), and the same formulas with $w_{si}(r)$ replacing $w_{bi}(r)$ apply for $w_{si}(r)$. Again taking the outer radius of the plate infinite, the results are given as follows:

$$w_{b1}(r) = -\frac{q}{64EI}\left[5a^4 - r^4 + 4a^2(r^2 - 2b^2) - 8a^2(r^2 + b^2)\log\frac{a}{b}\right] \tag{35}$$

$$w_{s1}(r) = \frac{I_m q}{EI I_f \alpha^2}\left[\frac{a^2 - r^2}{4} - \frac{a^2}{2}\log\frac{a}{b} + \frac{a^2}{2} I_o(\alpha b)\left\{K_o(\alpha b) - K_o(\alpha a)\right\}\right.$$
$$\left. - \frac{a}{\alpha}\left\{I_o(\alpha r) - I_o(\alpha a)\right\} K_1(\alpha a) + \frac{a^2}{2\alpha b}\left\{I_o(\alpha r) - I_o(\alpha b)\right\}\left\{\frac{1 + \alpha b I_o(\alpha b) K_1(\alpha b)}{I_1(\alpha b)}\right\}\right] \tag{36}$$

$$\tau_1(r) = \frac{I_m q}{I\left(c + \frac{f + f'}{2}\right)}\left[-\frac{r}{2} + \frac{\alpha a^2}{2} I_1(\alpha r)\left\{\frac{1 + \alpha b I_o(\alpha b) K_1(\alpha b)}{\alpha b\, I_1(\alpha b)} - \frac{2K_1(\alpha a)}{\alpha a}\right\}\right] \tag{37}$$

$$\frac{d^2 w_{b1}(r)}{dr^2} + \frac{\gamma}{r}\frac{dw_{b1}(r)}{dr} = -\frac{q}{16EI}\left[-(3+\gamma)r^2 + 2(1+\gamma)a^2 - 4(1+\gamma)a^2 \log\frac{a}{b}\right] \tag{38}$$

$$\frac{d^2 w_{s1}(r)}{dr^2} + \frac{\gamma}{r}\frac{dw_{s1}(r)}{dr} = \frac{I_m q}{EI I_f}\left[-\frac{(1+\gamma)}{2\alpha^2} + \frac{a^2}{2}\left\{I_o(\alpha r) - (1-\gamma)\frac{I_1(\alpha r)}{\alpha r}\right\}\right.$$

$$\left.\left\{\frac{1 + \alpha b I_o(\alpha b) K_1(\alpha b)}{\alpha b I_1(\alpha b)} - \frac{2K_1(\alpha a)}{\alpha a}\right\}\right] \tag{39}$$

on $0 \leq r \leq b$

and

$$w_{b2}(r) = -\frac{q}{64EI}\left[a^4 - r^4 + 4a^2(a^2 - r^2) + 8a^2(r^2 + b^2)\log\frac{r}{a}\right] \tag{40}$$

$$w_{s2}(r) = \frac{I_m q}{EI I_f a^2}\left[\frac{a^2 - r^2}{4} + \frac{a^2}{2}\log\frac{r}{a} + \frac{a^2}{2} I_o(\alpha b)\left\{K_o(\alpha r) - K_o(\alpha a)\right\}\right.$$

$$\left. - \frac{a}{\alpha}\left\{I_o(\alpha r) - I_o(\alpha a)\right\} K_1(\alpha a)\right] \tag{41}$$

$$\tau_2(r) = \frac{I_m q}{I\left(c + \frac{f + f'}{2}\right)}\left[-\frac{r}{2} + \frac{a^2}{2r} - aI_1(\alpha r)K_1(\alpha a) + \frac{\alpha a^2}{2} I_o(\alpha b) K_1(\alpha r)\right] \tag{42}$$

$$\frac{d^2 w_{b2}(r)}{dr^2} + \frac{\gamma}{r}\frac{dw_{b2}(r)}{dr} = -\frac{q}{16EI}\left[-(3+\gamma)r^2 + 4a^2 - \frac{2a^2b^2(1-\gamma)}{r^2}\right.$$

$$\left. + 4(1+\gamma)a^2\log\frac{r}{a}\right] \tag{43}$$

$$\frac{d^2 w_{s2}(r)}{dr^2} + \frac{\gamma}{r}\frac{dw_{s2}(r)}{dr} = \frac{I_m q}{EII_f}\left[-\frac{1+\gamma}{2a^2} - \frac{a^2(1-\gamma)}{2a^2 r^2} + \frac{a^2}{2} I_o(ab) \left\{K_o(ar) - (1-\gamma)\frac{K_1(ar)}{ar}\right\} - \frac{a}{a}\left\{I_o(ar) - (1-\gamma)\frac{I_1(ar)}{ar}\right\} K_1(aa)\right] \quad (44)$$

on $b \leq r \leq a$

Pressure on the Core

Formulas by which the pressure on the core can be estimated have not been developed. A method that yields such formulas has been used for a sandwich beam in the WADC Technical Report 52-185.[7] This method consists in simulating a load on one facing of the sandwich by the superposition of symmetrical and antisymmetrical loads on both facings. The analysis for the latter type of loading, where the two facings have the same deflection at all points, is that discussed above. Under the symmetrical loading the facings have equal but opposite deflections. While the pressure on the core is caused mainly by the symmetrical loading, it is expected that the effects of this loading upon the deflection, the radial stresses in the facings, and the shear stress in the core predicted by the formulas developed above, would not be of practical importance.

Simplification of Results

For most constructions that are likely to be of practical interest, the quantity

$$a = \sqrt{\frac{2GI}{EcfI_f}} \quad (45)$$

is so large that the Bessel functions that appear in a number of the above formulas with argument ar can be expressed in asymptotic forms (1828-64 and 65) at $r = a$ and $r = b$. These forms,

[7] "The Flexural Rigidity of a Rectangular Strip of Sandwich Construction -- Deflection and Distribution of Stresses in the Facings of a Centrally Loaded Transparent Beam," by W. S. Ericksen.

$$
\left.
\begin{aligned}
I_n(\alpha r) &\doteq \frac{\ell^{\alpha r}}{\sqrt{2\pi\alpha r}}, \quad n = 0,\ 1 \\
K_n(\alpha r) &\doteq \frac{\pi}{\sqrt{2\pi\alpha r}}\ell^{-\alpha r} \cos n\pi, \quad n = 0,\ 1
\end{aligned}
\right\} \qquad (46)
$$

are used in deriving the formulas that follow. The given results are based on the further simplifications that the facings are of equal thickness, that $\frac{I_m}{I} = 1$, and that term of order not greater than $\ell^{-\alpha(a - b)}$ and $\frac{1}{\alpha^2}$ are neglected in comparison with unity. If any of the conditions under which these simplifications are made are not met in a particular construction, the complete formulas listed above should be used.

Since all of the following formulas are obtained from those given above by evaluation at a particular value of r, it is no longer necessary to use the subscripts 1 or 2, which denote the regions of applicability. These subscripts are replaced by u, or c, which indicate the applicability of a formula when the load is uniformly distributed over the foot, or concentrated at the rim of the foot, respectively.

The deflection at the center of the tester relative to the outer rim of the foot is given by the formula:

$$
w_{u,\,c} = w_{bu,\,c} + w_{su,\,c} \qquad (47)
$$

For a load uniformly distributed over the foot, formulas (14) and (20) yield, respectively:

$$
w_{bu} = \frac{qa^2}{64EI}\left[5(a^2 - b^2) - 4b^2 \log \frac{a}{b}\right] \qquad (48)
$$

and

$$
w_{su} = \frac{qa^2}{2EI_f\alpha^2}\left[\log \frac{a}{b} - \frac{2}{\alpha^2 b^2}\right] \qquad (49)
$$

From (35) and (36), respectively,

$$w_{bc} = \frac{qa^2}{64\,EI}\left[5a^2 - 8b^2\left(1 + \log\frac{a}{b}\right)\right] \tag{50}$$

and

$$w_{sc} = \frac{qa^2}{2\,EI_f\,a^2}\left[\log\frac{a}{b} - \frac{1}{2}\right] \tag{51}$$

for the load concentrated at the rim of the foot.

The maximum shear stress in the core can be estimated for any foot radius by the use of figure 9, where curves for both a uniformly distributed foot load and one concentrated at the rim of the foot are given. These curves were constructed by the use of formulas (24) and (42).

Under the assumption that the facings are of equal thickness, formulas (25), (26), and (27) are combined into the forms

$$\sigma_{u,\,c} = -\frac{q(c + 2f)}{32I}\left[m_{bu,\,c} + \frac{16(c + f)\,(3c + 2f)}{f(c + 2f)}\,m_{su,\,c}\right] \tag{52}$$

where

$$m = \frac{16\,EI}{q}\left\{\frac{d^2w}{dr^2} + \frac{\gamma}{r}\,\frac{dw}{dr}\right\} \tag{53}$$

and the subscripts attached to $\underline{m}$ and $\underline{w}$ have their usual meaning. For the load uniformly distributed over the foot (28) and (29) yield, respectively,

$$m_{bu} = 4(1 + \gamma)\,a^2\log\frac{a}{b}\,,$$

and

$$m_{su} = \frac{(1 + \gamma)\,(a^2 - b^2)}{2a^2b^2}\,, \tag{54}$$

Table 1.--Results of tester, flexure, and tension tests

Panel construction	Well-bonded panel				Poorly bonded panel			
	Maximum core shear stress[1]			Tensile strength[3]	Maximum core shear stress[1]			Tensile strength[3]
	Tester	Flexure[2]			Tester	Flexure[2]		
	(1)	(2)	(3)	(4)	(5)	(6)	(7)	(8)
	P.s.i.	P.s.i.	P.s.i.	P.s.i.	P.s.i.	P.s.i.	P.s.i.	P.s.i.
Set No. 1	322C	....	213C	....	258C	208C	180C	....
Facings: 0.012-inch 24 ST clad aluminum	299C	296F	172C	....	277C	240C	207C	....
Core: 1/2-inch honeycomb of 0.002-inch-3SH aluminum foil expanded to 3/16-inch cells, 5.4 P.C.F. density	274C	297F	198C	....	251C	240C	197C	....
Average	298	296	194	772	262	229	195	358
Percent of well-bonded value					87.5	77.5	100.5	46.2
Set No. 2	310C	341C	229C	....	216B	166C	162C	....
Facings: 0.020-inch 24 ST clad aluminum	302C	327F	227C	....	183B	195C	132C	....
Core: 1/2-inch honeycomb of 0.002-inch-3SH aluminum foil expanded to 3/16-inch cells, 5.4 P.C.F. density	331C	311C	190C	....	208B	156C	130C	....
Average	314	326	215	660	202	172	131	236
Percent of well-bonded value					64.3	52.7	61.0	35.6
Set No. 3	321C	290C	236C	....	286C	188C	196C	....
Facings: 0.032-inch 24 ST clad aluminum	323C	341C	227C	....	254B	218C	181C	....
Core: 1/2-inch honeycomb of 0.002-inch-3SH aluminum foil expanded to 3/16-inch cells, 5.4 P.C.F. density	334N	371C	225C	....	239C	197C	159C	....
Average	326	334	229	748	260	201	179	268
Percent of well-bonded value					79.8	60.2	78.2	35.8
Set No. 4	456C	382C	371C	....	479C	316C	375C	....
Facings: 0.012-inch 24 ST clad aluminum	434C	344C	412C	....	509C	362C	310C	....
Core: 1/4-inch end-grain balsa of 9 to 11 P.C.F. density	470C	329C	411C	....	535C	383C	356C	....
Average	453	352	398	2,560	508	354	347	1,624
Percent of well-bonded value					112.4	100.5	87.1	69.0
Set No. 5	642C	244C	260C	....	472C	351C	284C	....
Facings: 0.020-inch 24 ST clad aluminum	504C	240C	242C	....	463B	368C	335C	....
Core: 1/2-inch end-grain balsa of 9 to 11 P.C.F. density	362C	252C	272C	....	524C	332C	284C	....
Average	503	245	258	2,143	486	350	301	1,759
Percent of well-bonded value					96.6	143.0	117.0	82.0
Set No. 6	475C	459C	326C	....	506B	311C	291C	....
Facings: 0.032-inch 24 ST clad aluminum	580C	372C	352C	....	516B	298C	253C	....
Core: 1/2-inch end-grain balsa of 9 to 11 P.C.F. density	480C	344C	363C	....	446B	382C	310C	....
Average	512	392	347	2,770	489	330	285	1,134
Percent of well-bonded value					95.5	84.0	82.2	41.0
Set No. 7	340N	202C	91C	....	340B	173C	87C	....
Facings: 0.020-inch 24 ST clad aluminum	340N	191C	85C	....	....	184C	79C	....
Core: 1/2-inch honeycomb of resin-impregnated cotton cloth corrugated to 7/16-inch cells	340N	190C	89C	....	340B	141C	76C	....
Average	340	194	88	253	340	166	81	201
Percent of well-bonded value					100.0	85.6	92.0	79.5
Set No. 8	74C	35C	34C	....	74C	62C	25C	....
Facings: 0.032-inch 24 ST clad aluminum	81C	79C	35C	....	77C	58C	27C	....
Core: 1-inch honeycomb of resin-treated paper expanded to 1/4-inch cells, 2.7 P.C.F. density	90C	74C	82C	....	85C	32C	30C	....
Average	82	63	50	143	79	51	27	78
Percent of well-bonded value					96.5	81.0	54.0	54.5
Set No. 9	392N	125C	166C	....	392N	122C	151C	....
Facings: 0.012-inch 24 ST clad aluminum	392N	120C	171C	....	392N	101C	165C	....
Core: 1/4-inch cellular cellulose acetate, 6 P.C.F. density	392N	112C	170C	....	392N	139C	169C	....
Average	392	119	169	261	392	131	162	135
Percent of well-bonded value					100.0	110.0	95.8	51.7
Set No. 10	660N	479C	395C	....	154B	115B	67B	....
Facings: 10 plies of resin-bonded glass cloth 112-114	660N	408C	377C	....	115B	123B	49B	....
Core: 1/4-inch honeycomb of nylon-phenolic bonded glass cloth corrugated to 1/4-inch cells, 8 P.C.F. density	660N	389C	370C	....	267B	129B	78B	....
Average	660	425	381	462	179	126	65	70
Percent of well-bonded value					27.2	29.6	17.0	15.2
Set No. 11	278X	120B	149B	....	236X	108B	88B	....
Facings: 10 plies of resin-bonded glass cloth 112-114	300X	123B	151B	....	202X	87B	81B	....
Core: 3/10-inch glass mat waffle type core, 11 P.C.F. density	278X	149B	137B	....	278X	86B	72B	....
Average	285	131	146	133	239	94	80	55
Percent of well-bonded value					84.0	71.8	54.8	41.4

[1]Failure locations are shown by letters: C, core shear failure; B, bond failure; F, facing failure by local buckling under load point; N, some deformation but no apparent failure; X, type of failure could not be identified.

[2]Two values are reported; stress parallel or perpendicular to a panel edge.

[3]Individual values are not reported, these are averages of 8 or 16 tests.

Rept. No. 1832-B

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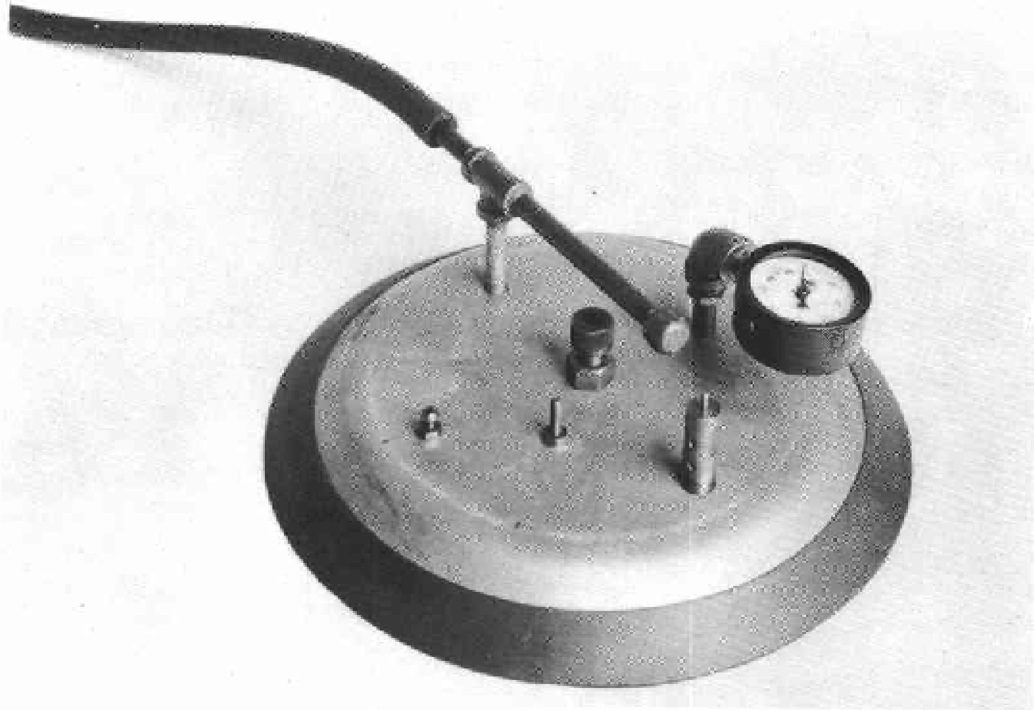

Figure 1.--External view of the sandwich tester, showing rubber gasket, attached vacuum line, and vacuum gage. The center bolt extends to the interior foot. An auxiliary poppet valve has been installed (immediately below the gage on the photograph) for control of vacuum during use as a tester.

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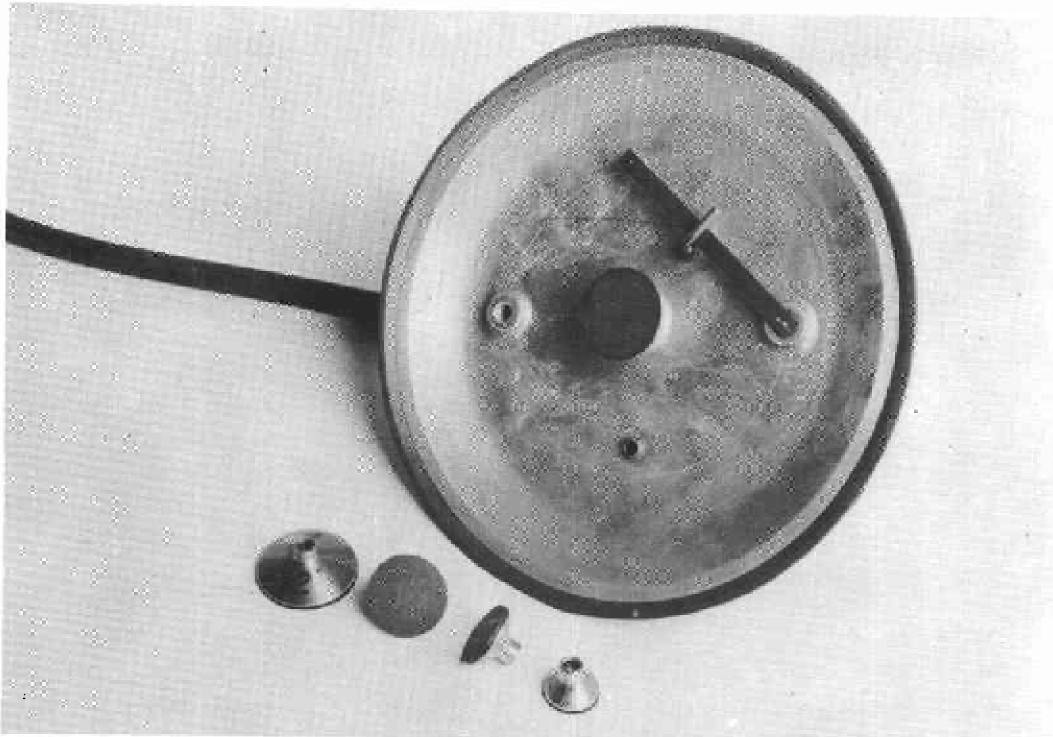

Figure 2.--Internal view of the tester, showing the central foot and alternate foot sizes available for use. The cantilever spring actuating the poppet valve for control of vacuum is also shown.

Z M 88040 F

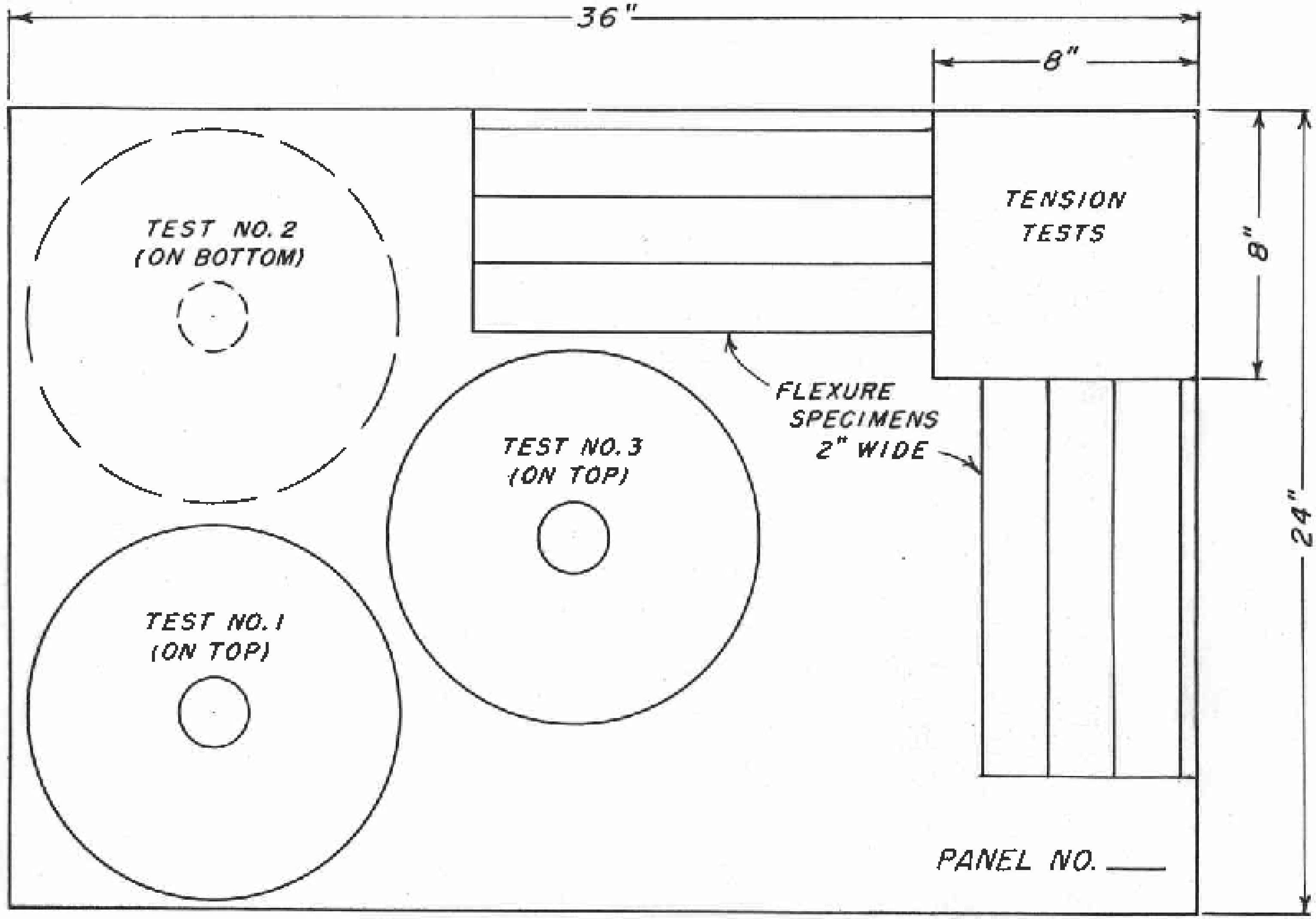

Figure 3. --Location of tester and test specimens on sandwich panels.

Z M 91780 F

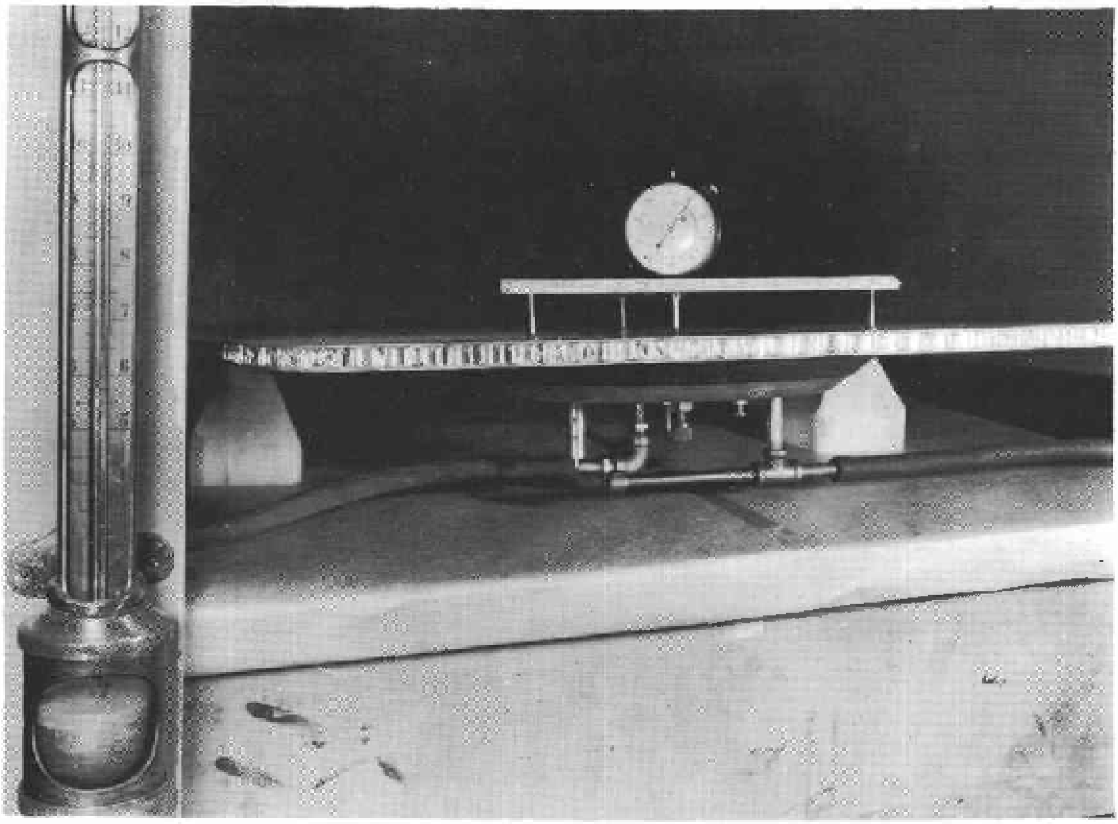

Figure 4. --The tester in use during evaluation tests, showing the addition of a deflection-measuring device on the opposite facing.

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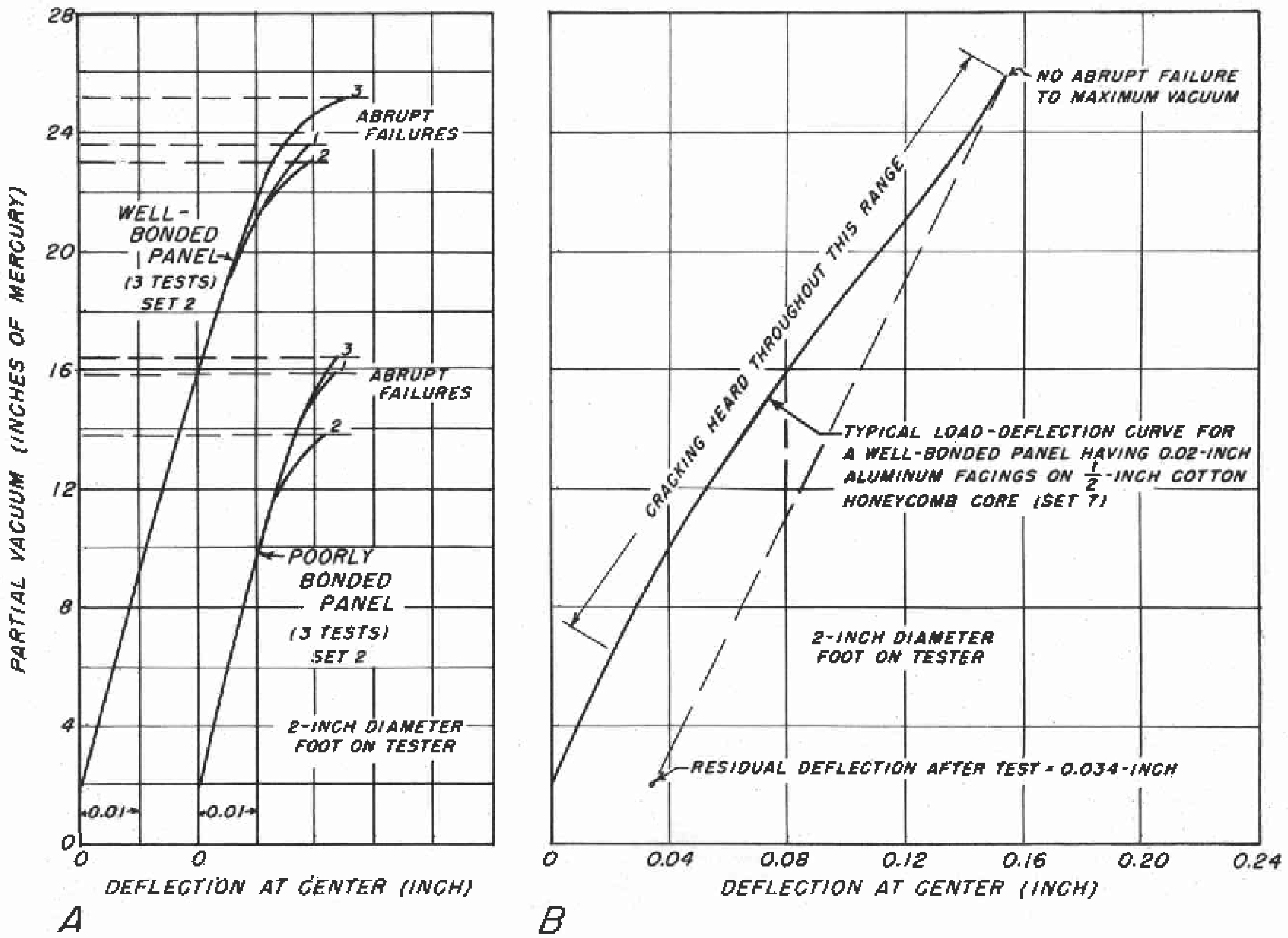

Figure 5. --Typical load-deflection curves for sandwich panels.

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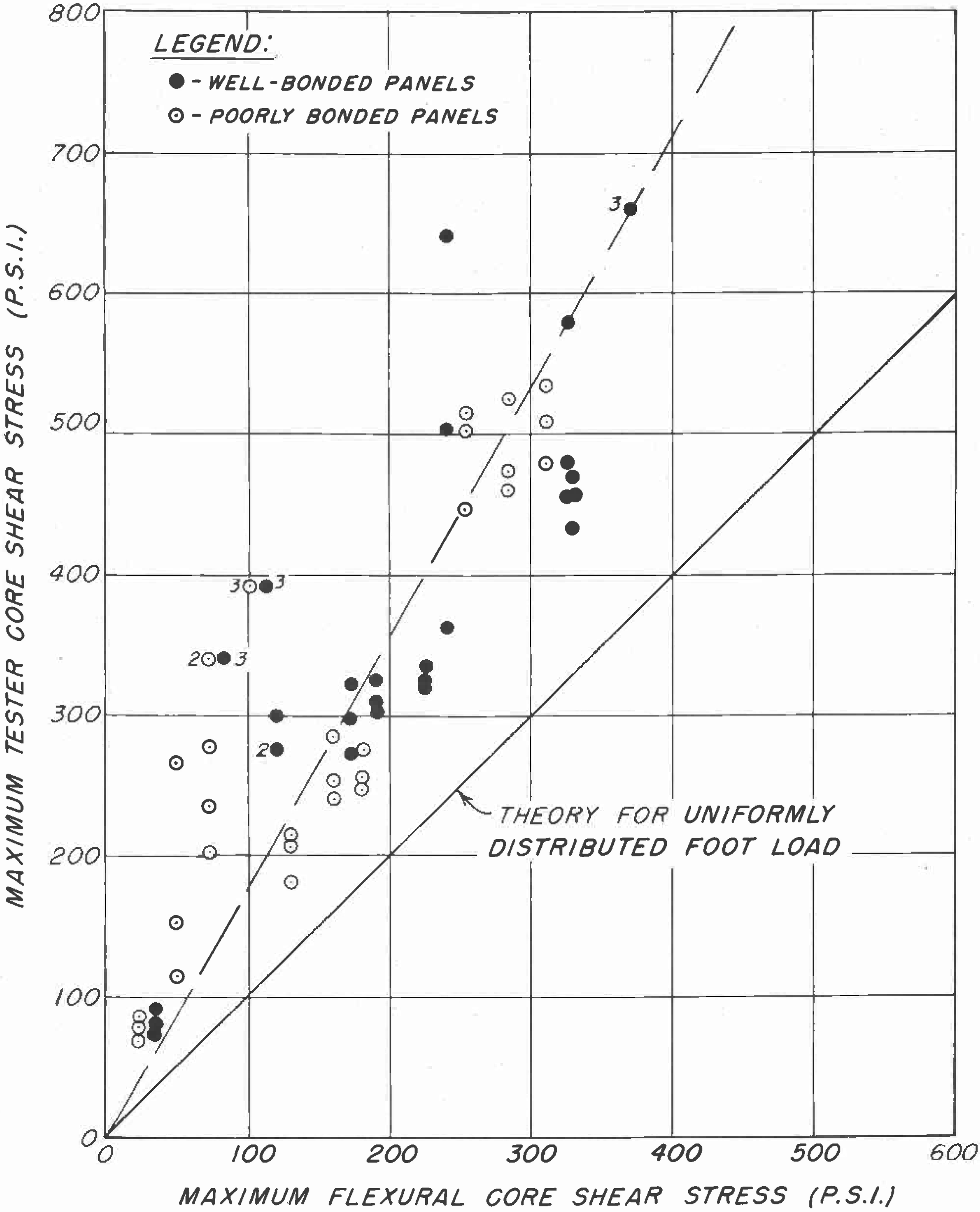

Figure 6. --Individual tester shear stresses plotted against lowest individual flexural shear stress.

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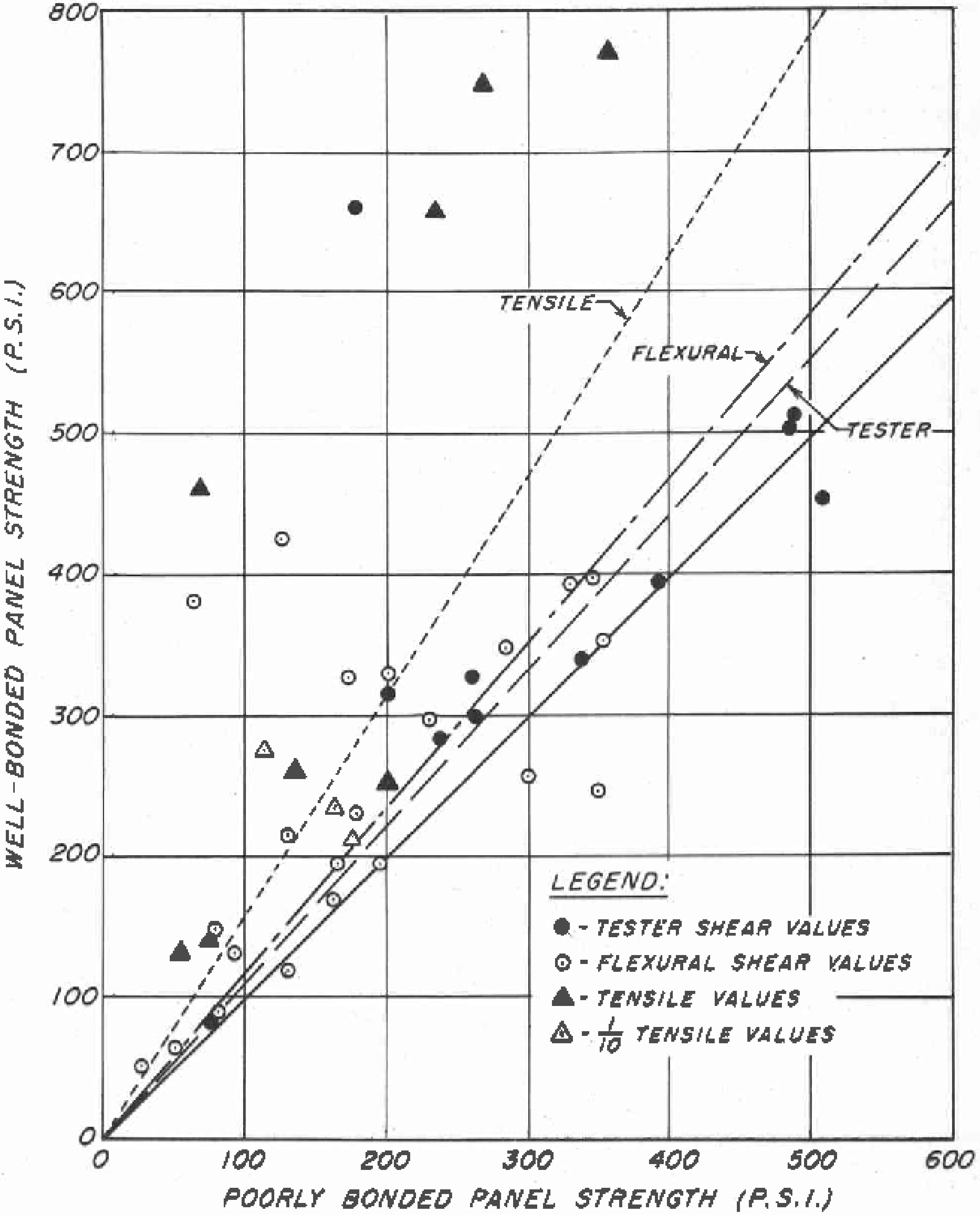

Figure 7. --Comparison of strength of well-bonded and poorly bonded panels as determined by tester, flexure, and tensile tests.

Z M 91777 F

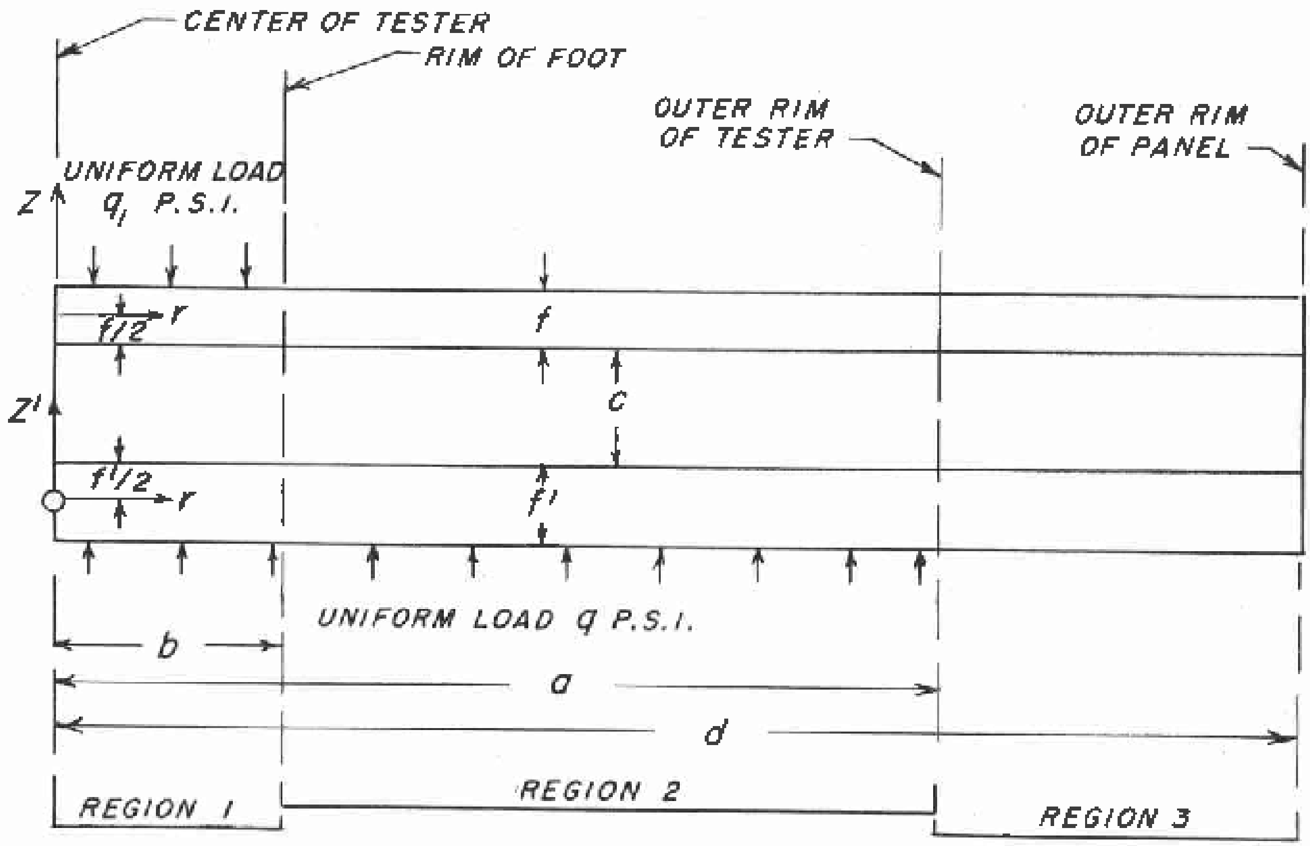

Figure 8. --Radial section of panel.

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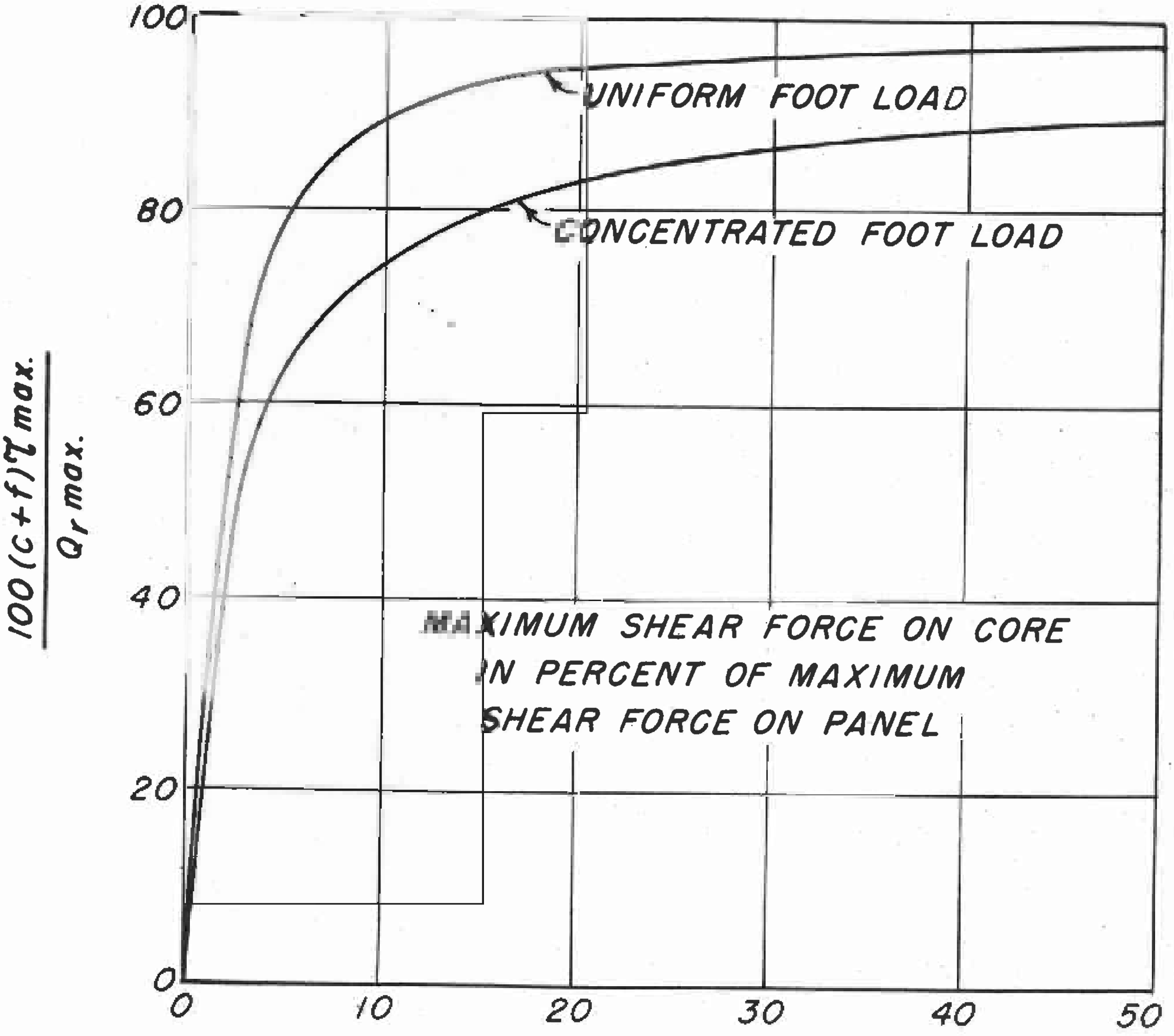

Figure 9. --Maximum shear force on core in percent of maximum shear force on panel.

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