AN INVESTIGATION OF THE FRACTURE OF BUTT JOINTS IN PARALLEL-LAMINATED VENEER[1]

Joseph Jung and *Joseph F. Murphy*

General Engineers

Forest Products Laboratory,[2] Forest Service, U.S. Department of Agriculture

Madison, WI 53705

(Received 15 March 1982)

ABSTRACT

A finite-element analysis and experimental testing program was conducted to investigate the fracture mechanics aspects of butt joints in parallel-laminated veneer (PLV). The effects of the number of plies, ply thickness, and joint spacing were investigated. It was found that the isotropic and orthotropic solutions for edge-cracked specimens were very close. Also, the increase in the stress-intensity factor for the exterior butt joint was of the order of only 4% for a joint separation spacing of four ply-thicknesses. In general, it was found that Mode I fracture toughness increased with decreasing veneer thickness and that the fracture toughness of exterior butt-jointed specimens varied with veneer thickness and number of plies. Results of the butt-joint interaction tests indicate a much higher degree of joint interaction than predicted by the finite-element analysis.

Keywords: Parallel-laminated veneer, butt joints, fracture mechanics, joint spacing.

INTRODUCTION

Research at the Forest Products Laboratory (FPL) in recent years has explored many aspects of parallel-laminated veneer (PLV) or Press-Lam (FPL Press-Lam Team 1976; Moody and Peters 1972; Schaffer et al. 1972). Results of this work have shown that the production of PLV can provide a greater yield than obtainable from conventional sawing because the material is peeled on a veneer lathe producing no sawdust waste. Also, since strength-reducing characteristics such as knots are dispersed and randomized through the material when the veneers are laminated to form a member, this provides for more uniform strength properties and the opportunity to utilize lower grade logs.

Since veneers come in finite lengths, typically 8 ft, some method of joining the ends of the veneers together is necessary to form longer structural members. This study was initiated to explore the mechanics of using a butt joint at the veneer ends. The effects of butt joints have been explicitly studied in only a few investigations (Leicester and Bunker 1969; Penny 1977; Smith and Penny 1980; Walsh 1973; Walsh et al. 1973) and although the interaction of the joint has been studied (Walsh 1973), a general interaction relationship on the number of plies, thickness of the plies, and joint spacing has not been developed either theoretically or experimentally. Specifically, the objectives of this study were to establish the loss in performance due to the butt joint, and to find the effect of joint spacing on performance.

The primary problem addressed in this study consisted of an orthotropic member assumed to be in plane stress and subjected to uniaxial tension (Fig. 1). A ply

---

[1] This article was written and prepared by U.S. government employees on official time and is therefore in the public domain.

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

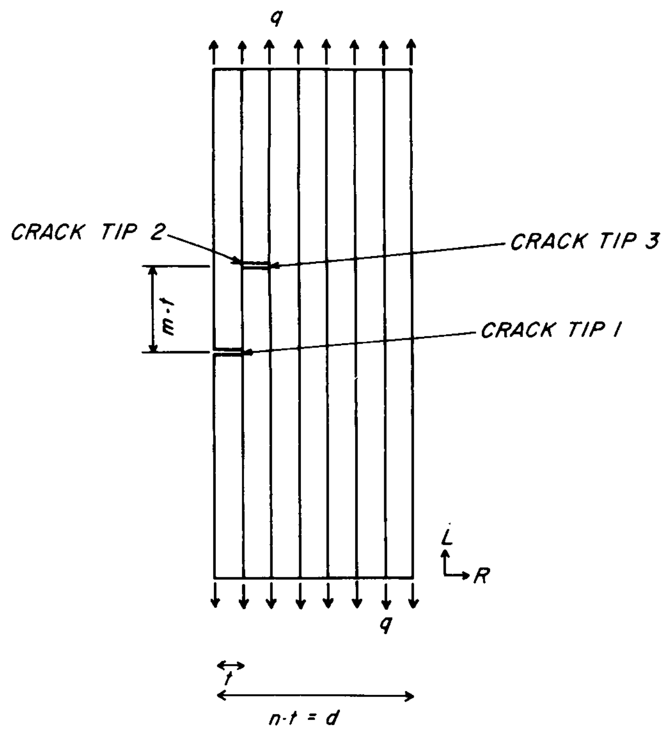

FIG. 1. Laminated wood member with two butt joints.

thickness is denoted by "t" and the member is comprised of "n" such plies. There were two joints in the member, one in an exterior ply and one in the adjacent interior ply. The joints were separated in a direction along the member length by a distance of "m" ply-thicknesses. It was assumed that there was no direct stress transfer across the two surfaces of the butt joint. For the purposes of this study the butt joints were considered to be cracklike flaws. Each of the crack tips are numbered as shown in Fig. 1.

Fracture mechanics analysis

The approach used in this study was based on linear elastic fracture mechanics (LEFM). Fracture mechanics, as an analytical tool, developed around work primarily concerned with the brittle failure of metals, but has been extended and applied to wood and other materials (Atluri et al. 1975b; Barrett 1976; Barrett and Foschi 1971; Broek 1974; Lei and Wilson 1979; Mindess et al. 1975; Moody and Peters 1972; Murphy 1979; Penny 1977; Schaffer et al. 1972; Schniewind and Centeno 1973; Schniewind and Lyon 1973; Schniewind and Pozniak 1971; Sih et al. 1965; Wu 1967). LEFM relates the stresses in the vicinity of a crack tip to the nominal stress applied to the structure or component, to the flaw size, shape, and orientation, and to the material properties. Furthermore, LEFM attempts to make predictions as to when a crack will propagate. The magnitude of the elastic stress field can be described by three stress-intensity factors, K_I, K_{II}, K_{III}, which are associated with the three principal modes of fracture: Mode I, Mode

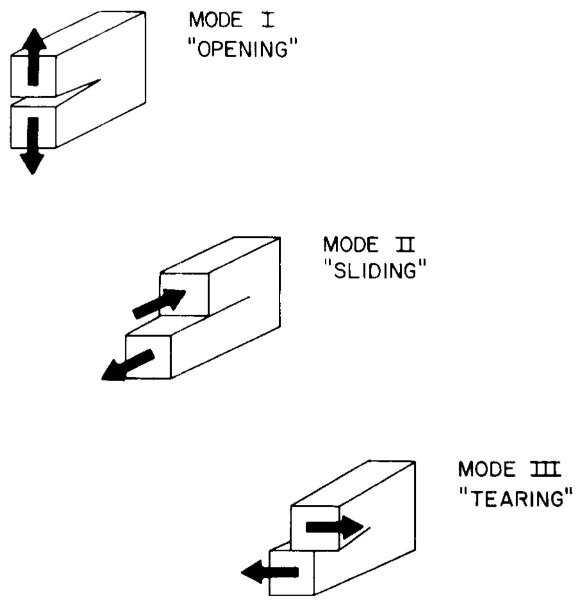

FIG. 2. Three basic modes of crack surface displacement. (M 146 386)

II, and Mode III (Fig. 2). We assume in our two-dimensional problem that there are no Mode III deformations, i.e., K_{III} is identically equal to zero. Unstable crack propagation (in a coplanar fashion) under a single mode of deformation occurs when the stress-intensity factor, associated with the crack, reaches a critical value, e.g., in the case of Mode I when $K_I = K_{Ic}$. This information is important for setting design stress levels and predicting remaining life (Mindess et al. 1975).

A difficulty arises when the cracklike flaw in wood is perpendicular to the grain. The crack does not propagate in a coplanar fashion across the grain but rather the crack extends perpendicular to itself *along* the grain. This was noted by Mindess (1975), and methods to take this crack turning into account have been proposed by Murphy (1979) which requires a combined Modes I and II failure criterion and by Smith and Penny (1980), which assumes the crack has already turned. In this paper, crack turning is not accounted for and the apparent critical stress-intensity factor, when crack propagation is noncoplanar, will have a superscript hat, e.g., $\hat{K}_{Ic}$ (same convention as in Mindess et al. 1975). The numerical value, though, is calculated as if propagation is coplanar. Tension perpendicular to grain and shear stresses on the imminent fracture plane are linear functions of the apparent stress-intensity factor (Murphy 1979); therefore, even in the absence of a combined Modes I and II failure criterion, the apparent critical stress-intensity factor is useful for predicting crack extension for a particular geometry and loading condition.

FINITE-ELEMENT ANALYSIS

The problem shown in Fig. 1 was analyzed by the method of finite elements (plane stress condition). The analysis included depth-to-lamination-thickness ratios (d/t) of 3, 4, 6, 8, and 10. This ratio can also be thought of as the number of laminations. The two butt joints were separated by 2, 4, 6, 8, or 10 lamination thicknesses (m). Twenty-five combinations of the number of laminations and joint spacings were investigated. The material properties used in the analysis were representative of clear Douglas-fir (*Pseudotsuga menziesii*) at 12% moisture content (Wood Handbook, USDA Handb. No. 72 (Rev.) 1974):

$$E_L = 1.95 \times 10^6 \text{ lb/in.}^2$$
$$E_R = 1.33 \times 10^5 \text{ lb/in.}^2$$
$$\nu_{RL} = 0.020$$
$$G_{LR} = 1.25 \times 10^5 \text{ lb/in.}^2$$

where

E is modulus of elasticity,
ν is Poisson's ratio,
G is modulus of rigidity, and
L and R subscripts designate the longitudinal and radial directions.

A typical grid used in this study is shown in Fig. 3. All of the cases were run using 1,154 nodes and 352 elements. Two types of elements were used. The four elements surrounding each crack tip were eight-noded, assumed-displacement hybrid elements (Atluri et al. 1975a, b). These special elements contain the proper singular displacement and stress fields for a region near the crack tips, and yielded the Modes I and II stress-intensity factors. The elements away from the crack tips were standard eight-noded, quadratic isoparametric plane elasticity elements.

The K_I, opening mode factor, at any of the three crack tips shown in Fig. 1 can be described by the formula:

$$K_I(\text{crack tip i}) = \sigma\sqrt{\pi t}f_i\left(\frac{d}{t}\right) g_i\left(\frac{d}{t}, m\right)$$

where

σ is the remote tensile stress,
t is the veneer thickness,
d is the specimen width,
m is butt-joint spacing in dimension of t,
f_1 is a correction function relating the stress intensity factor to t/d ratios for an edge crack in isotropic material (Tada et al. 1973)

$$= 1.12 - 0.231\left(\frac{t}{d}\right) + 10.55\left(\frac{t}{d}\right)^2 - 21.72\left(\frac{t}{d}\right)^3 + 30.39\left(\frac{t}{d}\right)^4,$$

$f_2 = f_3$ are correction functions relating the stress intensity factor to t/d ratios for a center crack in isotropic material (Tada et al. 1973)

$$= 1 - 0.128\left(\frac{t}{d}\right) - 0.288\left(\frac{t}{d}\right)^2 + 1.525\left(\frac{t}{d}\right)^3, \text{ and}$$

g_i are functions to correct for interaction of an edge butt joint and butt joint in the next adjacent lamina.

K_{II} for all joint configurations analyzed was negligibly small. The results of the finite-element analysis indicated that at a joint spacing, m, of 10 lamination

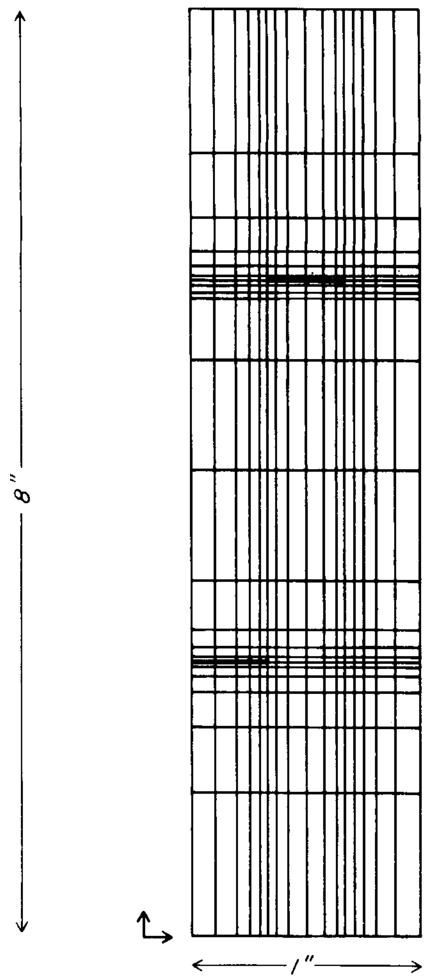

FIG. 3. Typical finite-element grid.

thicknesses, there was essentially no interaction between the two joints for all d/t (i.e., n) ratios. Since there is no interaction at this spacing, the effects of the material orthotropy on the Mode I stress-intensity factors can be assessed. A graphical comparison of the stress-intensity factors for an isotropic (Tada et al. 1973) and orthotropic specimen under uniaxial stress with a single edge crack is shown in Fig. 4. The results indicate that the effect of orthotropy, at least for the properties used in this study, is relatively small. A maximum difference between the isotropic solution and the finite-element results was approximately 3.4% and occurred at a d/t ratio of 10.

A similar type of comparison can be made for the two crack tips on the interior butt joint. Again, using a joint spacing of 10 lamination thicknesses to avoid joint interaction, a comparison between isotropic (Tada et al. 1973) and orthotropic results can be made (Fig. 5). It should be kept in mind that this is not a comparison of exactly the same specimen geometries, since the isotropic solution is for a

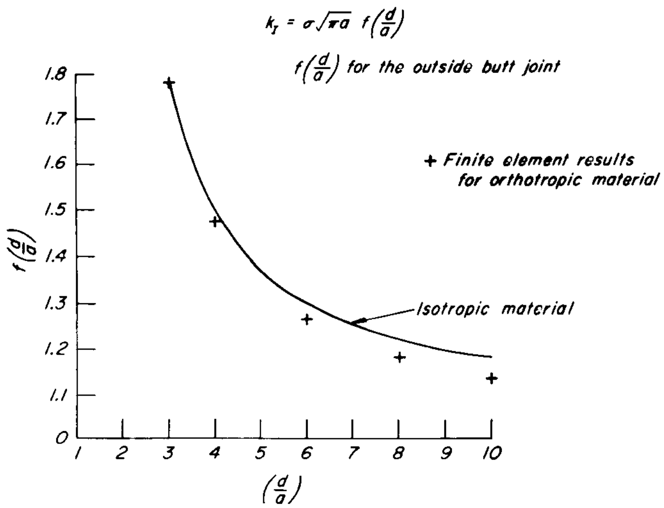

FIG. 4. Comparison of K_I for an isotropic and orthotropic edge-crack specimen.

centrally cracked plate under uniaxial stress, while the crack in the finite-element problem is over to one side of the plate. Even with this discrepancy, the maximum deviation from isotropic theory is approximately 4%. This result suggests that the combined effects of noncentering and orthotropy have only a small effect on the stress-intensity factors. Moreover, the stress-intensity factors for the crack tips of a plate with a given d/t ratio may be very insensitive to the location of the crack so long as the crack remains in the interior of the plate and perpendicular to the line of the applied uniaxial stress.

As the butt joints were brought closer together in the finite-element models (decreasing m), the stress-intensity factors began to increase, i.e., the joints began to interact.

$$\underset{\text{interaction SIF}}{(K_I)_i} = \underset{\text{no interaction SIF}}{(K_I)_i} \times \underset{\text{interaction function}}{g_i}$$

The value of this function, g, is dependent on the d/t ratio and the joint spacing, m. The subscript "i" denotes the crack tip number.

The values of g for crack tips, 1, 2 and 3 (Fig. 1) are given in Tables 1, 2, and 3. For crack tip 1, it is interesting to note that the interaction is relatively independent of the d/t ratio and dependent on only the joint spacing, m, for joint

TABLE 1. *Values of the interaction function g_I for crack tip 1.*

Joint spacing parameter, m	d/t ratio				
	3	4	6	8	10
10	1.00	1.00	1.00	1.00	1.00
8	1.00	1.00	1.00	1.00	1.00
6	1.00	1.01	1.01	1.01	1.01
4	1.03	1.04	1.04	1.03	1.03
2	1.16	1.17	1.14	1.12	1.11

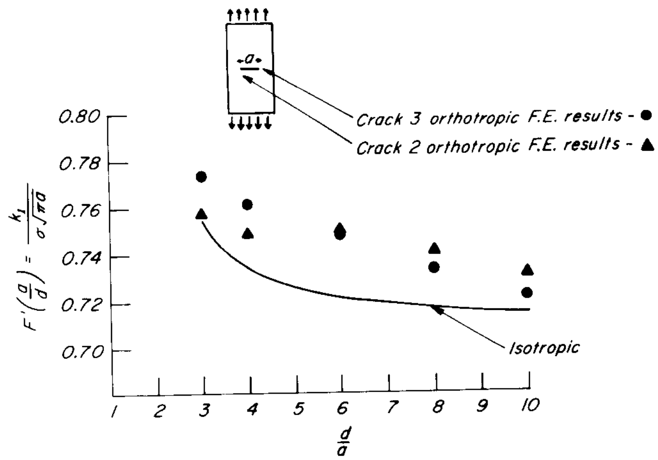

FIG. 5. Comparison of an isotropic and orthotropic specimen with a crack.

spacings greater than 2. The same statement can also be made for crack tip 2, the interior butt joint, for joint spacings greater than 6. By averaging the values of this function at any given joint spacing, an approximate value can be plotted on a single curve for each crack tip (Fig. 6). It can be seen that for the critical outside butt joint, crack tip 1, there is only a 4% increase in K_I if the two joints were as close as four lamination thicknesses apart.

EXPERIMENTAL OUTLINE AND PROCEDURES

In order to confirm and supply experimental data to support the finite-element results, a series of four tests were conducted. These included Mode I fracture, tension tests on members with outside butt joints, tension tests on members without joints, and tension tests on members with two interacting butt joints. The first three of these tests were on specimens constructed of three veneer thicknesses, i.e., 1/10 inch, 1/4 inch, and 4/10 inch.

Material preparation

Two 52-inch-long Douglas-fir bolts were heated and peeled on a lathe to yield approximately sixty-five 10-inch-wide pieces in each of three veneer thicknesses: 4/10 inch, 1/4 inch, and 1/10 inch. This material was subsequently press dried at 50 lb/in.[2] between 350 F platens to a moisture content of approximately 12%. The veneer was then randomly placed in stacks and stored in a 74 F, 65% relative humidity condition, for several months.

TABLE 2. *Values of the interaction function g_2 for crack tip 2.*

Joint spacing parameter, m	d/t ratio 3	4	6	8	10
10	1.00	1.00	1.00	1.00	1.00
8	1.01	1.02	1.02	1.02	1.02
6	1.05	1.08	1.07	1.06	1.06
4	1.16	1.21	1.18	1.16	1.15
2	1.48	1.53	1.43	1.39	1.36

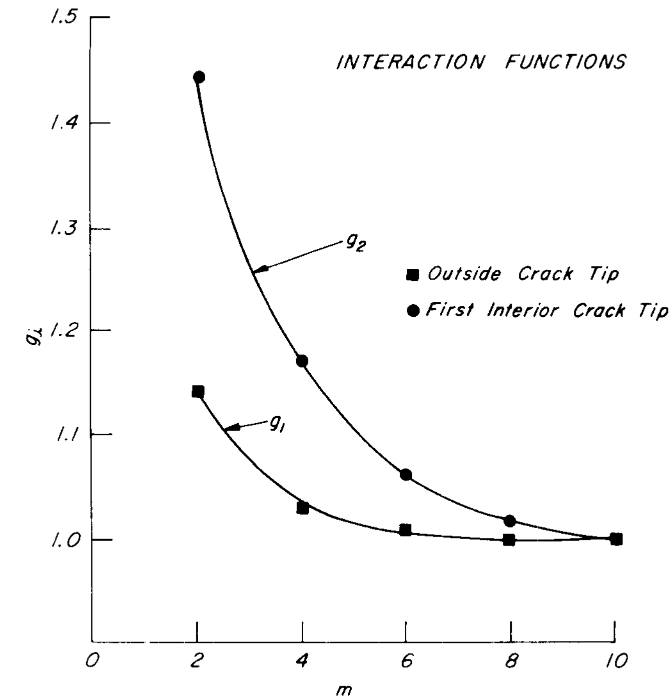

FIG. 6. Average value of the interaction function for crack tips 1 and 2.

The materials from which the Mode I, outside butt-joint, and tension specimens were taken were laminated using a phenol-resorcinol adhesive spread of 60 pounds per 1,000 square feet of glueline. The assemblies, resembling that shown in Fig. 7, were pressed at room conditions at approximately 150 lb/in.^{2} pressure for 18 h. The top plies were precut to form the butt joints, and the top ply of section C was not glued to the board so that the tension specimens had one less ply than the butt-jointed specimens. The final failure mechanism in the butt-jointed members was expected to be a tension failure of the member after the butt-jointed ply had separated. By having the nonjointed tension specimens of one less ply than the butt-jointed members, a direct comparison of ultimate loads could be obtained.

The $^4/_{10}$-inch veneer made 2-, 4-, and 6-ply specimens; the $^1/_4$-inch and $^1/_{10}$-inch veneer each made 2-, 4-, 6-, and 10-ply specimens. Two boards at each veneer thickness-number of ply combinations were made.

TABLE 3. *Values of the interaction function g_3 for crack tip 3.*

Joint spacing parameter, m	d/t ratio				
	3	4	6	8	10
10	1.00	1.00	1.00	1.00	1.00
8	1.00	1.01	1.00	1.00	1.00
6	1.04	1.05	1.03	1.01	1.01
4	1.17	1.16	1.11	1.09	1.07
2	1.69	1.59	1.44	1.38	1.35

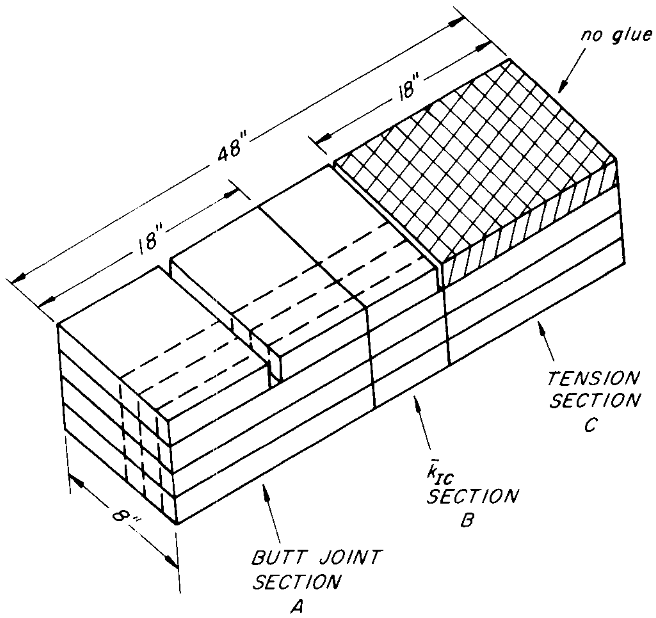

FIG. 7. Laminating assembly.

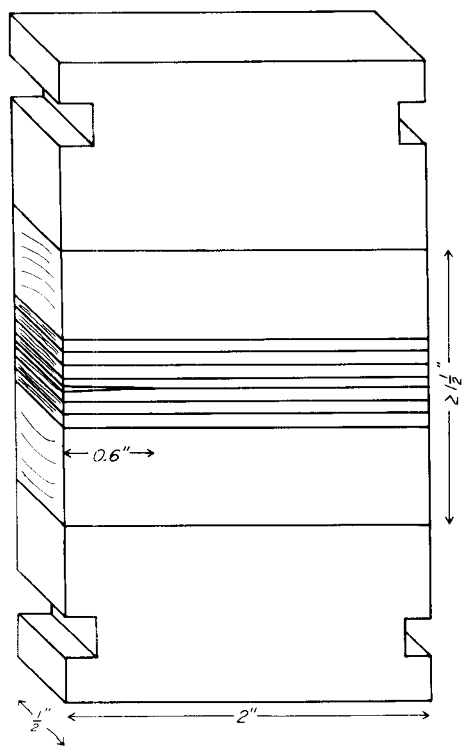

FIG. 8. Specimen for Mode I fracture tests.

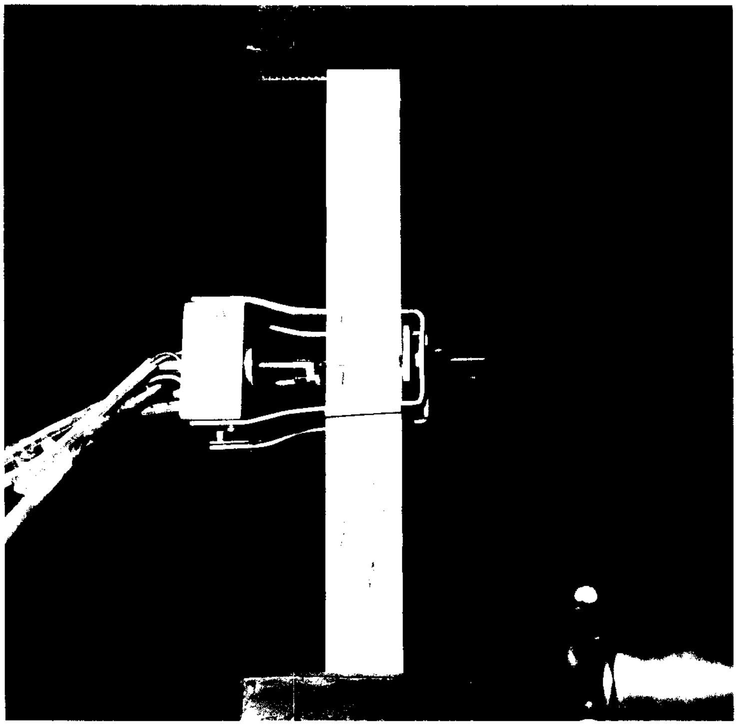

FIG. 9. Outside butt joint. Fracture specimens with instrumentation.

After laminating, eight ½-inch-wide rips were taken from each assembly shown in Fig. 7. The final specimens were cut from these ½-inch-wide pieces.

The materials for the last group of tests, the joint interaction series, were constructed from $\frac{4}{10}$-inch veneer with joint spacings of two and six lamination thicknesses. The manufacturing procedure was similar to that described for the other specimens.

Test procedures

The Mode I fracture tests were conducted using the specimen configuration used by Lei and Wilson, (1979) and shown in Fig. 8. At least 1½ inches were maintained between the two steel loading heads to minimize edge effects. To accomplish this, solid Douglas-fir spacers were glued to the loading heads and PLV. A $\frac{6}{10}$-inch-long bandsaw cut was made in the middle glueline. The specimens were tested at a loading head speed of 0.01 inch per minute. The maximum load was recorded. Forty-eight specimens constructed from $\frac{4}{10}$-inch veneer, 64 specimens from ¼-inch veneer, and 48 specimens from $\frac{1}{10}$-inch veneer were tested.

The outside butt-joint fracture specimens were instrumented as shown in Fig. 9. Two linear variable differential transformers (LVDT), one on each side of the butt joint, were used. Load versus displacements were recorded on x-y plotters. The fracture load was taken as that load at which a large change in displacement could be visually confirmed as one side of the joint started separating from the

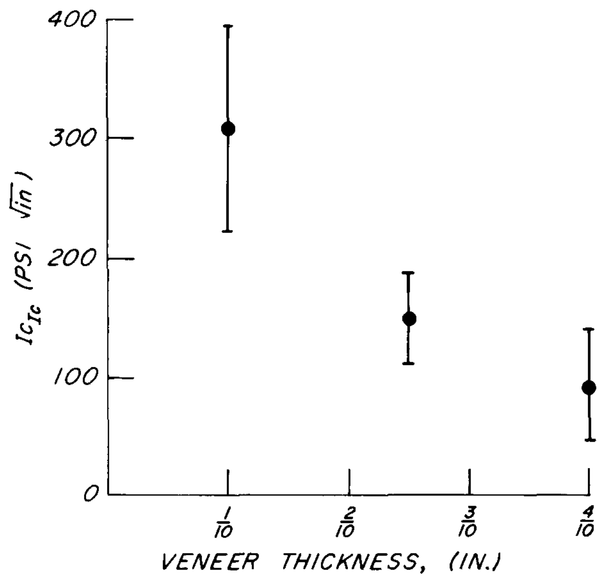

FIG. 10. Mode I fracture results (means ± standard deviation).

specimen. Testing was continued until maximum load was reached. A machine head speed of 0.1 inch per minute was used. Sixteen specimens for each veneer thickness-number of ply combination were tested. The specimens were 18 inches long and ½ inch thick. There were approximately 8 inches between the grips.

The tension tests were conducted in a similar fashion to the outside butt-joint fracture test except that there was no LVDT instrumentation and only maximum load was recorded. The same number of specimens was used in this test as in the outside butt-joint tests. These specimens were the same length and thickness as the butt-joint specimens.

The procedures for the joint interaction tests were the same as those for the outside butt joint. Sixteen specimens at each of two joint spacings, i.e., two and six lamination thicknesses, were tested. These specimens were also 18 inches long and ½ inch thick.

EXPERIMENTAL RESULTS

The following (example) notation will be used in describing one way analysis of variance: 0.1, 2,4,10,6 ($P = 0.0312$). This is shorthand for the following statements: For a constant veneer thickness of 0.1 inch the mean measured values (for different number of plies) ranked in order from high to low values are 2,4,10,6. At a 0.05 significance level 2,4,10 are statistically equivalent and 10,6 are statistically equivalent, 2,6 and 4,6 are statistically different. To reject the null hypothesis: There is no difference due to ply thickness, we expect to be wrong 3.12% of the time when in fact the null hypothesis is true.

Mode I fracture tests

The Mode I fracture results (Table 4 and Fig. 10) show an increase in the critical stress-intensity factor, K_{Ic}, as the veneer thickness decreases. It is believed that

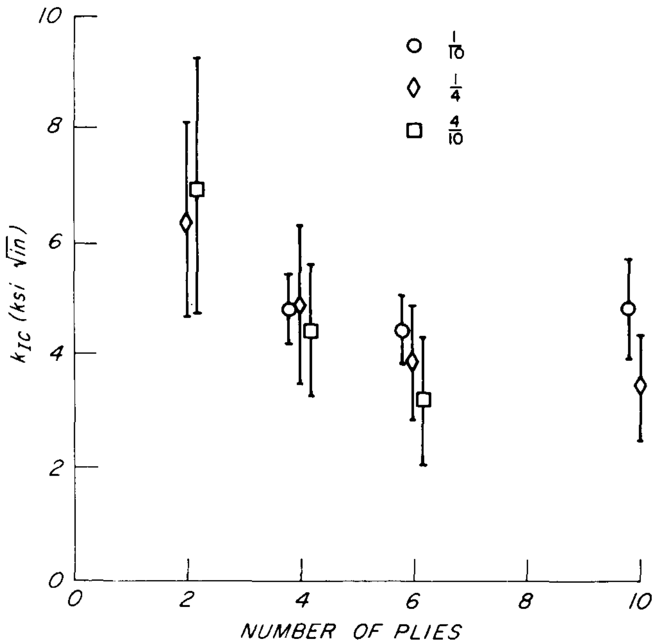

FIG. 11. $\hat{K}_{Ic}$ results for outside butt-joint fracture tests (average ± standard deviation).

there are two principal reasons for this trend. The first is that the wood is damaged during the peeling process. This damage takes the form of more surface cell damage and more severe checking, which occurs as the veneer thickness increases. The second reason for the noted trend is that it would be expected that poorer glue bonds would be obtained because of the rougher surfaces as the veneer thickness increases.

These results compare well to those obtained by Lei and Wilson, (1979). They reported K_{Ic} results for ⅛-inch sawn veneer which averaged 289 lb/in.^{2} $\sqrt{in.}$ with a coefficient of variation of 8.3%. The study results for the 1/10-inch veneer averaged 308 lb/in.^{2} $\sqrt{in.}$with a coefficient of variation of 27.9%. This higher variability is suspected to be due to differences in the adhesive application system or differences between sawn and peeled veneer quality.

Exterior butt-joint fracture

Failure in terms of $\hat{K}_{Ic}$.—The apparent critical stress-intensity factor, $\hat{K}_{Ic}$, for the exterior butt-joint tension tests are given in Table 5 and graphically shown in Fig. 11. The apparent critical stress-intensity factor is not constant. The exper-

TABLE 4. *Mode I fracture test results.*

Veneer thickness	Number of samples	Average K_{Ic}	Standard deviation	Coefficient of variation
In.		*Lb/in.^{2} $\sqrt{in.}$*	*Lb/in.^{2} $\sqrt{in.}$*	*Percent*
4/10	48	94.96	46.99	49.5
1/4	64	150.07	38.2	25.5
1/10	48	308.92	86.33	27.9

TABLE 5. *Summary of outside butt-joint fracture results.*[1]

Veneer thickness	No. of plies	Nominal stress at joint failure: Average	Nominal stress at joint failure: Coefficient of variation	Apparent critical stress-intensity factor at joint failure: Average	Apparent critical stress-intensity factor at joint failure: Coefficient of variation	Nominal stress at maximum load: Average	Nominal stress at maximum load: Coefficient of variation	Net stress at maximum load: Average	Net stress at maximum load: Coefficient of variation
In.		*Lb/in.²*	*Percent*	*Lb/in.² $\sqrt{in.}$*	*Percent*	*Lb/in.²*	*Percent*	*Lb/in.²*	*Percent*
4/10	2	2,236	32.2	6,997	32.2	4,785	31.5	9,570	31.5
	4	2,692	26.0	4,461	26.0	7,605	15.8	10,140	15.8
	6	2,234	34.9	3,211	34.9	9,210	13.6	11,053	13.6
1/4	2	2,576	27.0	6,394	27.0	4,814	24.3	9,627	24.3
	4	3,734	28.8	4,929	28.8	9,563	14.7	12,752	14.7
	6	3,401	26.6	3,901	26.6	9,783	13.0	11,738	13.0
	10	3,368	25.9	3,487	25.9	10,944	10.3	12,159	10.3
1/10	4	5,650	12.9	4,822	12.9	8,836	11.9	11,782	11.9
	6	6,057	14.0	4,473	14.0	9,393	8.5	11,276	8.5
	10	7,197	18.1	4,834	18.1	9,907	11.1	11,008	11.1

[1] There were 16 specimens at each veneer thickness-number of plies combination. The average moisture content for all of the specimens was 9.61 pct.

imental data show that the $\hat{K}_{Ic}$ not only varies with veneer thicknesses, but also with the number of plies. It appears that for thinner plies, $\hat{K}_{Ic}$ is more constant. In fact, the results for the 1/10-inch veneer are not statistically different 0.1, 10,4,6 ($P = 0.2800$). The $\hat{K}_{Ic}$ for the two thicker veneers appears to decrease as the number of plies increases 0.25, 2,4,6,10 ($P = 0.0001$) and 0.4, 2,4,6 ($P = 0.0001$). This decrease in $\hat{K}_{Ic}$ appears to level off after six plies, with the $\hat{K}_{Ic}$ for the 4/10-inch veneer being the lowest, and that for the 1/10-inch veneer being the highest. This same ranking in performance was observed in the Mode I fracture tests. The reasons for this trend are also probably the same, i.e., the thicker the veneer, the more severe the wood cell damage and also the rougher the gluing surface. Thus, at similar d/t ratios it appears that the members constructed from the thinner plies will perform better in terms of apparent fracture toughness.

Failure of these joints appeared to be initiated with a tension perpendicular to grain failure followed or accompanied by a shearing along the glueline. This is similar to the observation made with composites by Ueng et al. (1977). The failure typically involved the glueline and, also, the wood on either side of the glueline for the 4/10-inch and 1/4-inch specimens, but the failure plane of the 1/10-inch specimens generally extends into the interior of the material.

Joint failure in terms of applied stress.—The applied axial stress at which joint failure occurred is given in Table 5 and shown in Fig. 12. These results appeared to be in qualitative agreement with the fracture mechanics results that assume isotropic properties and a constant $\hat{K}_{Ic}$. For a given number of plies, the strength varies inversely with the crack length (veneer thickness), and in most cases the strength appears to increase, then level off, as the number of plies increases (Fig. 13). Noting these trends in Fig. 12, it can be seen that the nominal stress at joint failure for the 4/10-inch veneer is the lowest, while that for the 1/10-inch veneer is the highest, 2, 0.25,0.4 ($P = 0.1859$); 4, 0.1,0.25,0.4 ($P = 0.0001$); 6, 0.1,25,0.4 ($P = 0.0001$); 10, 0.1,0.25 ($P = 0.0001$). The strength of the members constructed

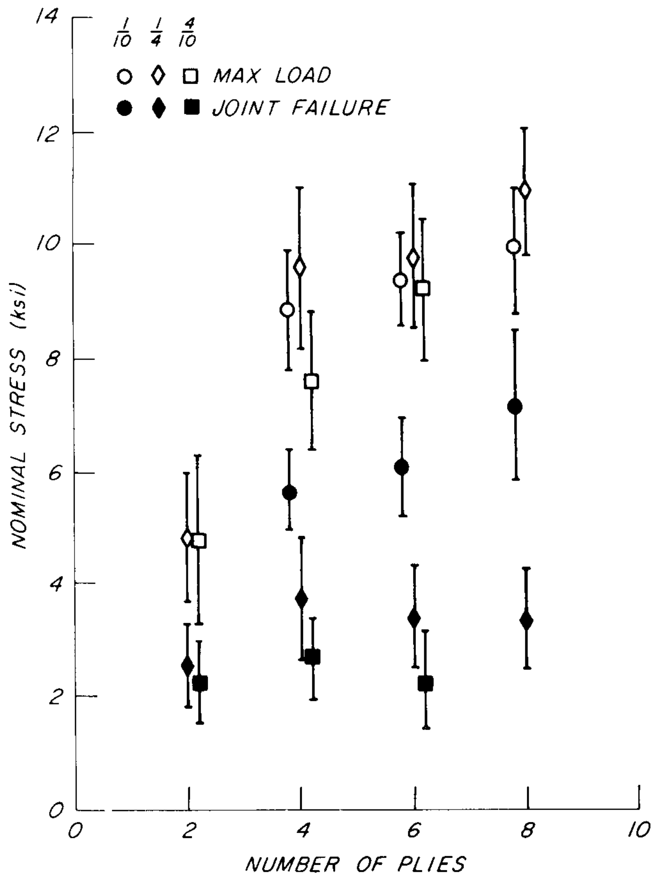

FIG. 12. Applied stress at joint failure and ultimate load for outside butt-joint specimens (averages ± standard deviation).

from 1/10-inch veneer is still increasing at 10 plies, while that for 1/4-inch veneer members has dropped and then leveled out after four plies, 0.1, 10, 6,4 (P = 0.0002); 0.25, 4,6,10,2 (P = 0.0048). This is not true for the results for the 4/10-inch veneer members. which show a slight drop in strength at six plies 0.4, 4,2,6, (P = 0.1391). This drop in strength at six plies may not be indicative of the true trend since four- and six-ply data indicate that the means can be considered statistically equivalent with an analysis of variance at 95% confidence.

Comparison of applied stress at joint failure and stress at ultimate load.—The differences between the applied stresses at joint failure and ultimate load are graphically depicted in Fig. 12 (both measured on each specimen). The nominal axial stress at failure was calculated by assuming that there were no butt joints, i.e., that the butt-jointed lamina were continuous. The averages of the applied stress values at joint failure ranged from 24 to 73% of the stress at ultimate failure. In other words, joint failure can occur at a load significantly lower than the ultimate strength load.

Nonjointed and single butt-joint tension tests.—The results of the nonjointed tension tests (Fig. 14 and Table 6) show the effects of ply thickness and number

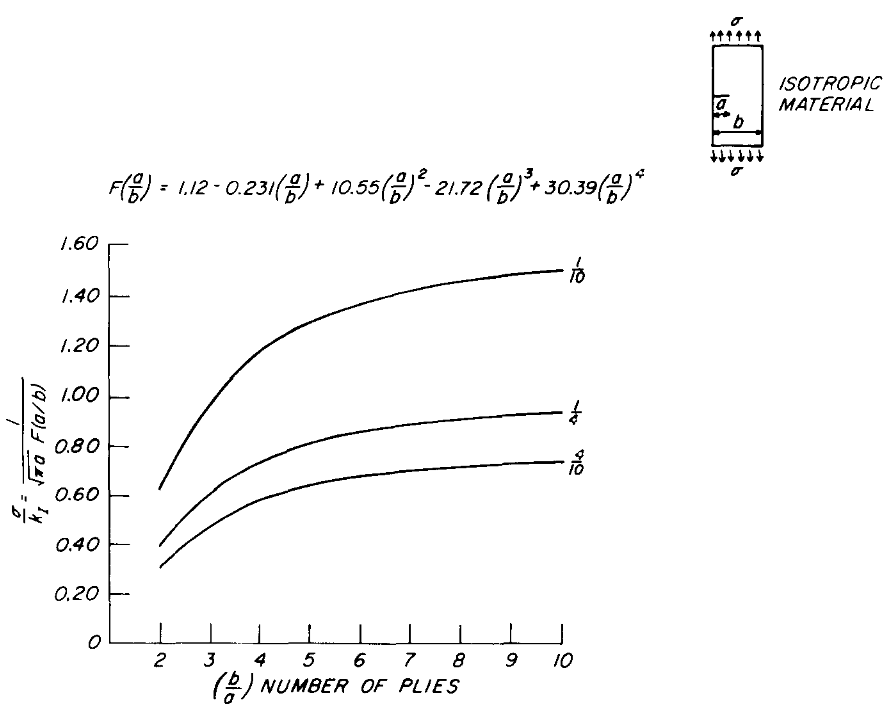

FIG. 13. Predicted behavior of a side-cracked isotropic specimen in terms of stress.

of plies. Strength differences due to veneer thickness (for five plies and less) support the notion that the wood material is damaged more the thicker it is cut. The effect of laminating is clearly shown in the results for both the 1/4 and 4/10-inch veneer members as the number of laminations is increased from one to three, 0.1, 3,5,9 ($P = 0.0004$); 0.25, 9,3,5,1 ($P = 0.0732$); 0.4, 3,5,1 ($P = 0.0358$). Not only was there a marked increase in strength, but also a dramatic decrease in variability. Any trends in the results beyond three plies are not clear.

TABLE 6. *Summary of tensile-test results on nonjointed members.*

Veneer thickness	Number of plies	Maximum tensile stress: Average	Maximum tensile stress: Coefficient of variation
In.		*Lb/in.²*	*Percent*
4/10	1	9,125	23.2
	3	10,351	8.49
	5	10,191	8.17
1/4	1	11,865	22.8
	3	12,946	6.66
	5	12,251	11.5
	9	13,308	8.31
1/10	3	15,149	15.8
	5	14,669	14.7
	9	12,376	8.37

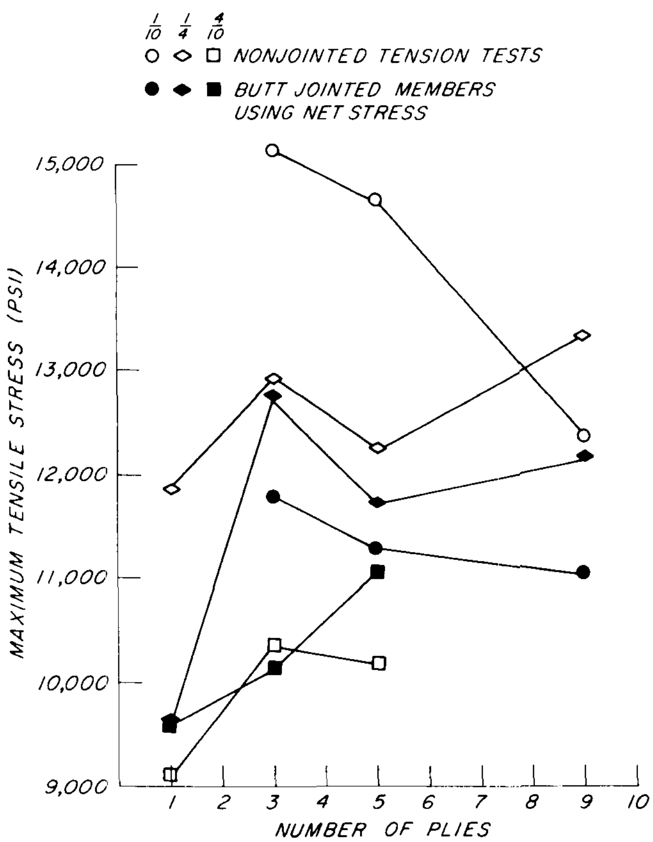

FIG. 14. Comparison of maximum net stress for the outside butt-jointed members and maximum stress for the nonjointed member.

Also shown in Fig. 14, along with the strength results for the nonjointed tension tests, are the mean maximum axial net stresses of the butt-jointed member. These net stresses were calculated by taking the maximum load and dividing by the net cross-sectional area, neglecting the area of the butt-jointed lamina. The net section was used since at ultimate load the butt-jointed lamina have already peeled off the specimen. A two sample *t*-test was performed. The purpose of the analyses of variance was to determine if using a net section approach would give adequate estimates of the failure strengths. At 95% confidence, results for the 4/10-inch veneer members at each of the three levels of number of plies could be considered statistically equivalent. The same statement could be made of the results for the 1/4-inch veneer members for the four- and six-ply results. None of the results for the 1/10-inch veneer groups were statistically equivalent at 95% confidence level. These findings suggest that a net section approach to estimating the ultimate strength may be reasonable for members constructed of relatively thick veneer (1/4 in. thick or more). This approach appears to break down for the thinner veneers. In examining the failures of the butt-jointed members, it was noted that the joint failures of the 4/10- and 1/4-inch members were usually in or around the glueline. This was not, in general, the case for the 1/10-inch veneer members in which the

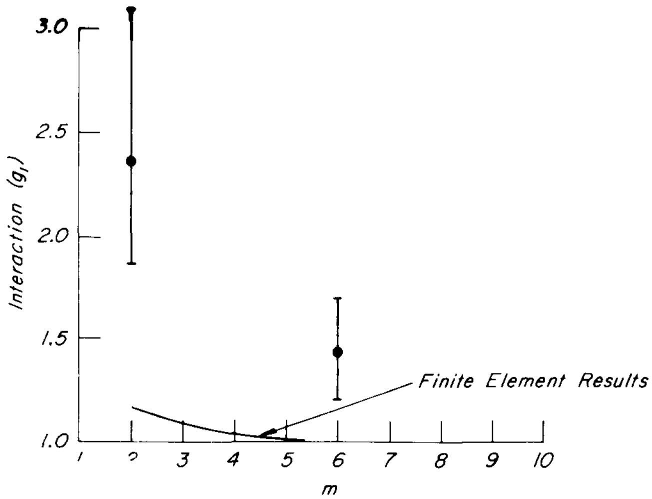

FIG. 15. Comparison between predicted and experimental (means and 95 percent confidence intervals) joint interaction.

joint failure surfaces generally propagated significant distances into the interior of the specimens, possibly due to better glue bonds in these specimens.

Joint interaction tests.—The joint interaction results are given in Table 7 and Fig. 15 with 95% confidence intervals (Kendall and Stuart 1961) on the ratios of noninteracting applied stresses at joint failure divided by the applied stresses at joint failure with interaction. It can be seen that the test results show a much higher degree of interaction than that predicted by the finite-element analysis. The reasons for this discrepancy are unclear but may include: (1) the techniques for measuring fracture were not sensitive enough, (2) fracture mechanics is not totally applicable to this system, (3) mode of fracture is different than assumed, and (4) there may be other factors involved with failure. It should be noted that the failures always started on the exterior butt joint, that the failure plane was on or around the glueline, and that it extended toward the interior butt joint.

CONCLUSIONS

The finite-element results indicated that there is very little difference between isotropic and orthotropic solutions for an edge-cracked specimen. The maximum difference observed was about 3.4% at a d/t ratio of 10. It was also found that the stress-intensity factors for a centrally cracked isotropic specimen under uniaxial tension are very close (within 4%) to those of a similarly loaded orthotropic

TABLE 7. *Joint interaction results.*[1]

Joint spacing (number of lamination thicknesses)	Average applied stress at failure	Coefficient of variation
	Lb/in.²	*Percent*
2	1,132	42.8
6	1,865	23.4

[1] There were 16 samples at each joint spacing level.

plate with a crack that is not centered. This finding suggests that the location of an interior butt joint is not an important factor in determining tension failure loads since the value of the stress-intensity factor does not vary significantly with the location of the joint. The finite-element results also indicated that the effect of joint interaction on the exterior butt joint from a joint in the adjacent ply is negligibly small until the joints are as close as four lamination thicknesses apart when there is approximately a 4% increase in K_I for the exterior joint.

The experimental results showed that for Mode I tests the critical stress-intensity factor increased with decreasing veneer thickness. It is felt that there are two reasons for this. First, the thicker the veneer, the more it is damaged in cutting; and second, thicker veneers generally have rougher surfaces which are more difficult to bond.

The apparent critical stress-intensity factor for the exterior butt-joint tension tests is not constant as it usually is for metals, but it varies with veneer thickness and number of plies. The average applied stress at joint failure for these specimens ranged from 24 to 73% of the applied stress at ultimate failure. Thus, joint failure can occur at a load significantly lower than the ultimate strength load. The correlation of tension test results on nonjointed members with the results of the butt-jointed members showed that using the net section stress for the butt-jointed members which neglects the area of the butt-jointed lamina would give a good estimate of the failure load for members constructed from ¼-inch-thick veneers or thicker. This net section analysis does not appear to be satisfactory for members constructed from thin veneers, e.g., ¹⁄₁₀ inch, since at butt-joint failure the fracture plane extends a significant distance into the interior of the member. This is not the case for the thicker veneers since the fracture plane generally follows the glueline.

The butt-joint interaction tests indicated that the degree of joint interaction is much higher than predicted by the finite-element analysis. The reasons for this are unclear but indicate further analysis and experimentation is needed.

ACKNOWLEDGMENTS

The authors wish to gratefully acknowledge the assistance of Professor Satya Atluri and Dr. Michihiko Nakagaki of the Georgia Institute of Technology for their help in the development of the computer code.

REFERENCES

ATLURI, SATYA N., ALBERT S. KOBAYASHI, AND MICHIHIKO NAKAGAKI. 1975a. An assumed displacement hybrid finite-element model for linear fracture mechanics. Int. J. Fract. Mech. 11(2): 257–271.

———, ———, AND ———. 1975b. A finite-element program for fracture mechanics analysis of composite material. ASTM STP 593, pp. 86–98.

BARRETT, J. D. Shear fracture of wood. 1976. Int. Union of For. Res. Organ. Congr., Oslo, Norway.

———, AND R. O. FOSCHI. 1971. Mode II stress-intensity factors for cracked wood beams. Eng. Fract. Mech. 9(2):371–378.

BROEK, DAVID. 1974. Elementary engineering fracture mechanics. Noordhoff Int. Publ., Leyden, The Netherlands.

FOREST PRODUCTS LABORATORY PRESS-LAM TEAM. 1976. Press-Lam: Progress in technical development of laminated veneer structural products. USDA For. Serv. Res. Pap. FPL 279. For. Prod. Lab., Madison, WI.

KENDALL, MAURICE G., AND ALAN STUART. 1961. The advanced theory of statistics. 2(3):130–132. Hafner Publ. Co., New York, NY.

LEI, YOUNG-KONG, AND JAMES B. WILSON. 1979. Fracture toughness of parallel-laminated veneer. For. Prod. J. 29(8):28–32.

LEICESTER, R. H., AND P. C. BUNKER. 1969. Fracture at butt joints in laminated pine. For. Prod. J. 19(2):59–60.

MINDESS, S., J. S. NADEAU, AND J. D. BARRETT. 1975. Slow crack growth in Douglas-fir. Wood Sci. 8(1):389–396.

MOODY, R. C., AND C. C. PETERS. 1972. Feasibility of producing a high-yield laminated structural product: Strength properties of rotary knife-cut laminated southern pine. USDA For. Serv. Res. Pap. FPL 178. For. Prod. Lab., Madison, WI.

MURPHY, J. F. 1979. Using fracture mechanics to predict failure of notched wood beams. Proc. First Int. Conf. on Wood Fracture 1978, Banff, Alberta.

PENNY, DWIGHT T. 1977. Fracture mechanics analysis of butt joint in laminated wood beams. M.S. thesis. Dep. of Mech. Eng., Colo. State Univ., Fort Collins, CO.

SCHAFFER, E. L., R. W. JOKERST, R. C. MOODY, C. C. PETERS, J. L. TSCHERNITZ, AND J. J. ZAHN. 1972. Feasibility of producing a high-yield laminated structural product: General summary. USDA For. Serv. Res. Pap. FPL 175. For. Prod. Lab., Madison, WI.

SCHNIEWIND, A. P., AND J. C. CENTENO. 1973. Fracture toughness and duration of load factor I. Six principal systems of crack propagation and the duration factor for crack propagating parallel to grain. Wood Fiber 5(2):152–159.

———, AND D. E. LYON. 1973. A fracture mechanics approach to the tensile strength perpendicular to grain of dimension lumber. Wood Sci. Technol. 7:45–59.

———, AND R. A. POZNIAK. 1971. On the fracture toughness of Douglas-fir wood. Eng. Fract. Mech. 2:223–233.

SIH, G. C., P. C. PARIS, AND G. R. IRWIN. 1965. On cracks in rectilinearly anisotropic bodies. Int. J. Fract. Mech. 1:189–203.

SMITH, F. W., AND D. T. PENNY. 1980. Fracture mechanics analysis of butt joints in laminated wood beams. Wood Sci. 12(4):227–235.

TADA, HIROSHI, PAUL C. PARIS, AND GEORGE R. IRWIN. 1973. The stress analysis of cracks handbook. Del. Res. Corp.

UENG, C. E. S., J. A. ABERSON, AND B. A. LAFITTE. 1977. Tensile analysis of an edge notch in a unidirectional composite. J. Composite Mater. 11:222–235.

UNITED STATES DEPARTMENT OF AGRICULTURE, FOREST SERVICE, FOREST PRODUCTS LABORATORY. 1974. Wood handbook: Wood as an engineering material. U.S. Dep. of Agric. Handb. No. 72 (rev.). Washington, DC.

WALSH, P. F. 1973. The interaction of butt joints. J. Int. Wood Sci. 6(2):22–27.

———, R. H. LEICESTER, AND A. RYAN. 1973. The strength of glued lap joints in timber. For. Prod. J. 23(5):30–33.

WU, E. M. 1967. Application of fracture mechanics to anisotropic plates. J. Appl. Mech. 34:967–974.