PREDICTION OF LOAD-BEARING CAPACITY OF NOTCHED CROSS LAMINATED TIMBER PLATES

E. Serrano, P.J. Gustafsson & H. Danielsson
Division of Structural Mechanics, Lund University, Sweden

This paper was originally published for INTER 2019.

KEYWORDS
Cross laminated timber, CLT, notch, FE-analysis, fracture mechanics

1 BACKGROUND AND AIM

Notches in cross laminated timber (CLT) can be used at supports or to realise connections between elements. At a notch, concentrated perpendicular to grain tension and shear stress appear and the load bearing capacity can be governed by cracking. The aim of this paper is to discuss the design of notched CLT-plates in relation to the current design approach of Eurocode 5 (2004) (EC5) for solid timber beams and suggested design approaches for notched CLT members and to compare these with analytical and numerical calculations and experimental evidence. Of special concern is whether the current approach for solid timber beams can be adapted to CLT plates with notches. The numerical work presented is in part based on Serrano (2018).

2 CURRENT DESIGN RULES AND EXPERIMENTAL RESULTS

2.1 Theoretical basis of design approach in EC5

Design equations for end-notch timber beams found in codes and design recommendations are typically either empirical or with a rational theoretical basis from fracture mechanics. The most frequently used fracture mechanics approach for end-notched beam design equations is based on energy balance considerations (Griffith, 1921) by means of linear elastic beam theory (Gustafsson, 1988). Here assumptions, derivation and result will be summarized for an end-notched timber beam, see Figure 1a. This end-notched beam has from the beam theory analysis point of view the same strength and stiffness as the slit cut beam shown in Figure 1b.

\[
W = -V \delta + \frac{V \delta}{2} = -\frac{V^2 (\delta / V)}{2} = -\frac{V^2 C}{2}
\]

Figure 1: a) Notched beam with notation defining geometry.
b) Equivalent slit cut beam.

The material is assumed to be homogeneous and orthotropic and fracture is assumed to be due to crack propagation along the beam, starting at the tip of the notch. The propagation starts when the release of potential energy \( W \) during increase of crack length from \( \delta \) equals the dissipation of energy at the crack tip during the same crack length increase. The potential energy is the sum the potential energy of the force \( V \) and the elastic strain energy in the structure:

\[
dW = -d(\delta h) \frac{\partial W}{\partial (\delta h)} = d(\delta h) \frac{V^2}{2} \frac{\partial C}{\partial (\delta h)}
\]
The dissipation of energy is in linear elastic fracture mechanics (LEFM) assumed to be proportional to the crack opening area, i.e. where \( G \) is the critical energy release rate of the material and \( b \) is the width of the beam at the tip of the notch. Energy balance between the decrease of potential energy \( \delta V \) and the dissipated energy gives the magnitude of the load \( V_F \) that starts crack propagation:

\[
V_F = \frac{2bG_C}{\sqrt{2C/\delta(\theta b)}} \tag{3}
\]

The material parameter \( G_C \) is affected by the ratio between normal and shear stress at the crack tip. In applied analyses of end-notched beams \( G_C \) is, however, commonly assigned the value for pure normal stress, i.e. the value found from Mode I loading fracture tests. This is an approximation on the safe side, although commonly of insignificant magnitude.

The compliance \( C \) has three components:

\[
C = \frac{\delta V}{V} = (\frac{\delta V}{V})_{\text{bending}} + (\frac{\delta V}{V})_{\text{clamping}} + (\frac{\delta V}{V})_{\text{shear}} \tag{4}
\]

These three deflection parts are illustrated in Figure 2 for a cantilever attached to an elastic half-space. The bending part and the shear part are calculated according to conventional Bernoulli/Euler and Timoshenko theory, respectively, for the two beam parts, I and II. Deformation due to the compliant attachment of beam part I to beam part II is constituted by development of non-plane cross-sections in the vicinity of the notch tip even in case of pure bending. The corresponding deflection is represented by a rotational spring with stiffness set to:

\[
k_\theta = \frac{1}{\sqrt{1/(KGA)_I - 1/(KGA)_II[1/(EI)_I - 1/(EI)_II]}} \tag{5}
\]

where \( (KGA)_I \) and \( (EI)_I \) are the Timoshenko shear stiffness and the bending stiffness, respectively, of beam cross sections I and II. This particular spring stiffness was derived from results of finite element analysis of beams as shown in Figure 2 suggesting as a reasonable approximation that the compliance of such beams may be expressed as (Petersson, 1974):

\[
\delta V = D (E + L)^3 \tag{6}
\]

where \( L \) is the length of the cantilever beam and \( D \) and \( E \) are constants.

With \( C \) determined as outlined above, Equation (3) can be reformulated as:

\[
V_F = \frac{\sqrt{2bG_C}}{\sqrt{1/(KGA)_I - 1/(KGA)_II} + 6b^2[1/(EI)_I - 1/(EI)_II]} \tag{7}
\]

If the compliant clamping is disregarded, i.e. if \( k_\theta \to \infty \) then the result would become:

\[
V_F = \frac{\sqrt{2bG_C}}{\sqrt{1/(KGA)_I - 1/(KGA)_II}} \tag{8}
\]

Equation (7) has been found to be in better agreement with test results than Equation (8). Note that the length \( Bh \) may be replaced by the bending moment to shear force ratio \( (M/V) \) at the tip of the notch.

For a beam with a homogeneous rectangular cross section \( bh \), the cross section quantities are \( K=5/6 \), \( A_I=bh \), \( A_{II}=bh \), \( l_I=b(a/2)^{1/2} \) and \( l_{II}=bh^{1/2} \), and Equation (7) can be written as:

\[
3/2 \frac{V_F}{b h} = 1.5 \frac{G_C G}{0.6} \sqrt{\frac{1}{\alpha} - \alpha^2 + 6\alpha^{10}(G/E) \sqrt{1/\alpha - \alpha^3}} \tag{9}
\]

where the left hand side of the equation is the formal maximum shear stress in beam part I at the instant of fracture at the notch.

### 2.2 Current EC5 design approach for solid beams

The current version of EC5 (Eurocode 5, 2004) includes provisions for strength design of end-notched structural members made of timber, laminated veneer lumber (LVL) and glulam. The provisions are based on Equation (9) and assuming that

\[
E/G = 15.6 \Rightarrow \sqrt{10G/E} = 0.8 \tag{10}
\]

and assuming that

\[
1.5 \frac{G_C G}{0.6} / f_y = k_n [\text{mm}^{1.2}] = \begin{cases} 4.5 \text{ for LVL} \\ 5.0 \text{ for solid timber} \\ 6.5 \text{ for glulam} \end{cases} \tag{11}
\]

---

Figure 2: A cantilever beam with length \( L \) attached to an elastic half-space.
The EC5 design approach for (90°) end-notched members is formulated in terms of the shear stress capacity of beam part I:

$$\tau_d = \frac{1.5V}{bah} \leq k_v f_{v,d}$$  \hspace{1cm} (12)$$

where $k_v$ is a strength reduction factor given by

$$k_v = \min \left( \frac{k_n}{\sqrt{h\left(\sqrt{\alpha - \alpha^2} + 0.86 \sqrt{1/\alpha - \alpha^2}\right)}} \right)$$  \hspace{1cm} (13)$$

with $k_n$ being defined by Equation (11) and the factor 0.8 by Equation (10).

### 2.3 Current design approach for notched CLT plates

Since EC5 (Eurocode, 2004) does not include structural design of CLT, such design is instead done by following the European Technical Assessment (ETA) documents of the respective CLT-producers. Other sources of technical information, such as technical guidelines (handbooks) are also used in cases when the ETA does not cover a specific design situation. An example of an ETA giving design provisions for notched CLT member is OIB (2017) while Wallner-Novak et al. (2013) is an example of a handbook giving such provisions. Both these technical documents recommend the use of the EC5-approach as formulated in Equations (11) - (13).

Depending on the placement of the notch corner in relation to longitudinal and transverse layers, see Figure 3, the shear acting at the corner may give rise to either longitudinal shear or rolling shear in any case, however, always in combination with perpendicular to grain tensile stresses. The rolling shear strength $f_{v,r}$ is suggested to be used in both the above-mentioned technical documents, OIB (2017) and Wallner-Novak et al. (2013), thus assuming that an appropriate design criterion would be:

$$\tau_d = \frac{1.5V}{bah} \leq k_{v,r} f_{v,r,d}$$  \hspace{1cm} (14)$$

The effective depth, $h_{ef}$, is in the case of the ETA (OIB, 2017) interpreted according to Figure 3, disregarding the thickness of the lower-most layer at the notched support if that layer is a transverse layer. In Wallner-Novak et al. (2013) it is instead assumed that $h_{ef}$ equals the physical depth of the notched part, i.e. including all layers of that part, irrespective of their orientation.

The choice of definition of the effective depth $h_{ef}$ may influence the predicted load bearing capacity to a significant extent. In addition, CLT plates behave differently from solid timber beams in several aspects. Different values of material strength, stiffness and fracture energy might be relevant to consider depending on the position of the notch in relation to the CLT layers. Furthermore, the ratio of bending stiffness to shear stiffness of the cross section can be very different in a CLT plate compared to a solid timber beam. Equations (11) - (13) are based on the compliance method, for which it is necessary to express the change of the compliance, $C$, of the structural member as a function of crack length during crack propagation. It is by no means self-evident that the use of Equations (11) - (13), which are based on simplifications reasonable for solid timber beams, give accurate results even if fitted to (a limited amount of) CLT test data. These simplifications, expressed in Equations (10) - (11), are not valid for a layered element, and it seems unlikely that a fit of the equations using only the parameter $k_n$ would be valid for more than a very limited number of sizes, material qualities and CLT lay-ups.

### 2.4 Experimental evidence of behaviour of notched CLT plates

In Friberg (2017) a 5-layer CLT plate notched at the end and loaded in three-point bending was investigated using the set-up shown in Figure 4, showing also the notch depths tested. The lay-up of the 160 mm thick CLT was (40-20-40-20-40), without structural edge bonding, quality C24. The specimens had a notch at both ends and thus a larger amount of tests could be performed on a relatively limited amount of material. The notch locations tested were for all cases but one at the interface between a transverse and a longitudinal layer. In addition, a notch depth of 80 mm was also tested, in order to verify the case of crack propagation along grain, within a longitudinal layer. Crack propagation typically took place as indicated in Figure 3 a) and b) and the tests were in general stopped before the crack reached half the length of...
the span. For each notch depth, 6–8 nominally equal specimens were tested.

The outcome of the tests was that there was no significant difference in load bearing capacity between notch depths 40 and 60 mm nor between notch depths 100 and 120 mm. The test results are summarized in Table 1.

**Table 1: Ultimate shear force capacity from tests (Friberg, 2017).**

<table>
<thead>
<tr>
<th>Notch (mm)</th>
<th>Shear force (kN)</th>
<th>Standard dev. (kN)</th>
<th>Num. of tests</th>
</tr>
</thead>
<tbody>
<tr>
<td>40</td>
<td>15.0</td>
<td>1.6</td>
<td>6</td>
</tr>
<tr>
<td>60</td>
<td>14.4</td>
<td>1.7</td>
<td>7</td>
</tr>
<tr>
<td>80</td>
<td>8.9</td>
<td>1.3</td>
<td>6</td>
</tr>
<tr>
<td>100</td>
<td>5.5</td>
<td>0.80</td>
<td>8</td>
</tr>
<tr>
<td>120</td>
<td>5.3</td>
<td>0.65</td>
<td>8</td>
</tr>
</tbody>
</table>

It was noted during the tests that cracking always started from the re-entrant corner of the notch. For notch depths of 40 and 100 mm the crack propagated at an approximately 45° angle through the transverse layer. For the cases where the notch corner was at the lower edge of a longitudinal layer (notch depths 60 and 120 mm) or in the middle of the longitudinal layer (notch depth 80 mm), the crack propagated along the grain direction of the longitudinal layer. It was not always possible to identify, from the force-displacement curves, at which load level the crack propagation started.

### 3 DESIGN OF NOTCHED CLT PLATES: RATIONAL METHODS

#### 3.1 Overview of methods and input parameters

In the following, four different methods are presented: a) analytical beam theory by consistent use of the EC5-approach, b) structural element approach (FE-based), c) 2D-continuum element approach based on LEFM and d) 2D-continuum element approach based on cohesive zone modelling, including softening. The material parameters used in these analyses are given in Table 2.

**Table 2: Material properties assumed in the analyses.**

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Values</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$E_{0}$ ; $E_{90}$</td>
<td>12 000 ; 500</td>
<td>MOE along grain; perp. grain [MPa]</td>
</tr>
<tr>
<td>$G_{0,0}$ ; $G_{90,90}$</td>
<td>600 ; 75</td>
<td>Shear modulus, longitudinal; rolling shear [MPa]</td>
</tr>
<tr>
<td>$\nu_{0,90}$ ; $\nu_{90,90}$</td>
<td>0.3 ; 0.3</td>
<td>Poisson’s ratios [-]</td>
</tr>
<tr>
<td>$G_{c,1}$ ; $G_{c,II}$</td>
<td>400 ; 1500</td>
<td>Critical energy release rate, Mode I; Mode II [J/m²]</td>
</tr>
<tr>
<td>$f_{t}$ ; $f_{v}$</td>
<td>5 ; 2</td>
<td>Material strength, tension perp; rolling shear [MPa]</td>
</tr>
</tbody>
</table>

#### 3.2 Consistent use of design equation based on compliance method

A consistent derivation of an analytical compliance method approach should use Equation (7) as a starting point, instead of applying directly Equations (11) - (13). The calculation of the shear stiffness $KGA_i$ and the bending stiffness $EI_i$ is for the non-homogeneous case of CLT-plates very involved, and closed-form solutions are not practical. An algorithm for calculation of the shear stiffness is given in e.g. Wallner-Novak et al. (2013), and that algorithm was applied here. In the calculation of the load level at which crack propagation occurs, it was assumed that $G_c = G_{c,1}$. This gives a lower-bound LEFM-solution to the problem, noting that LEFM-solutions, in general, overestimate the capacity (since they assume infinite material strength).

#### 3.3 Structural element model

The second approach adopted included modelling with structural beam elements in a configuration according to the test setup shown in Figure 4.
to Figure 5 and making use of the above described compliance method. In the model, the notched part has length $L_1 + a$ and the un-notched part has length $L_2 - a$, where $a$ is the current crack length. A rigid link element connects the two shear flexible beam elements via a rotational spring. The link element is used in order to account for the possible effect of eccentricity between parts I and II. This type of beam model can be made to coincide with the analytical expressions of Equations (7) - (8), if Timoshenko beam type elements are used and, of course, if the rotational spring stiffness is set to the same value, e.g. according to Equation (5).

Another way of introducing a compliant coupling in the beam model is to assume an additional (fictitious) length of the crack, which indeed is also a way to interpret Equation (6). In that case the crack length $a$, mentioned above, would be different from the length of the physical crack. A similar approach to account for the compliant coupling was used in Danielsson & Gustafsson (2015).

In the analyses presented in this paper the shear flexible beams were of Timoshenko type, as for the method in section 3.2. Furthermore it was assumed that $G = G_C$, $k = \infty$ and that the crack extended an additional length equal to the notch depth, i.e. $(1-\alpha)h$. This choice of additional crack length was made to obtain a reasonable fit to test results and 2D-continuum model results. The choice can also be motivated by the fact that for a larger notch depth, the activation of the full cross section will take place at a larger distance from the notch. According to Danielsson & Gustafsson (2015) another choice could be to extend the crack length with the amount of $\alpha h/2$.

### 3.4 2D Linear elastic fracture mechanics model

A convenient compliance method approach is to use 2D-plane stress elements to model the geometry of the CLT plate, including a crack. With such a model, a number of linear elastic analyses are performed, each analysis for a different crack length along a pre-defined crack path. Thus, the compliance of the structure as a function of crack length can be calculated by pure post-processing, i.e. the critical load for crack propagation, as a function of crack length, can be determined by use of Equation (3).

An example of a FE-mesh used is shown in Figure 6. The model relates to analyses for a notch depth of 40 mm, i.e. equalling the thickness of the outermost longitudinal layer. For cracking within a longitudinal layer, the crack path followed the grain direction. The crack path within the transverse layers was set to be 45° to the longitudinal layers. When the crack reached the border to the next longitudinal layer, the path was assumed to be oriented along the longitudinal layer. These assumptions of the crack paths are in accordance with the experimental observations in Friberg (2017). The bond lines between the laminations were not specifically modelled. The supports and the loading point were modelled with linear constraints.

### 3.5 2D Nonlinear cohesive zone model

Cohesive zone models have been used in many different applications related to timber engineering, see e.g.
Serrano & Gustafsson (2006). The main advantage with such models is that they are relevant to use for a wide range of material parameters and absolute sizes of members, i.e. for a wide range of brittleness. In the present study, the built-in feature known as cohesive contact in the software (Dassault, 2017) was used to model the cohesive softening behaviour along pre-defined crack paths. The same crack paths as those used for the 2D LEFM model described above were adopted. An example of a FE-mesh used in the analyses is shown in Figure 7. In these analyses, both the supports and the loading point were modelled by the use of rigid surfaces interacting with the CLT-member (coefficient of friction was set to 0.3).

4 RESULTS AND DISCUSSION

4.1 Comparison of theoretical results and test results

Table 3 gives an overview of the results from the tests and from the predictions using the four approaches discussed above.

The consistent application of the ECS-approach gives reasonable results in relation to test results for notches less than 50% of the plate thickness. For larger notches it turns out that the equations produce unphysical results. The assumptions of shear flexible beam theory based on the use of the Timoshenko definition of shear stiffness, $KGA$, produce non-compatible strain distributions for the notched and un-notched parts of the beam. As a consequence, and depending on the value of the rolling shear modulus, even complex values can be the outcome (i.e. the value of $KGA_I$ is larger than the value of $KGA_II$), cf. Equation (7). As an example, if the rolling shear modulus is set to 60 MPa (instead of the assumed 75 MPa), complex values are obtained from Equation (7) for a notch depth of 100 mm.

As compared to the test results, it seems like the other three methods give reasonable predictions. Note that the approach using structural elements, cf. Figure 5,

<table>
<thead>
<tr>
<th>Notch</th>
<th>Test</th>
<th>Consistent ECS</th>
<th>Structural Elements</th>
<th>2D LEFM</th>
<th>2D non-linear</th>
</tr>
</thead>
<tbody>
<tr>
<td>40</td>
<td>15.0</td>
<td>13.9</td>
<td>15.9</td>
<td>13.8</td>
<td>13.6</td>
</tr>
<tr>
<td>50</td>
<td>N/A</td>
<td>13.6</td>
<td>15.2</td>
<td>14.4</td>
<td>14.2</td>
</tr>
<tr>
<td>60</td>
<td>14.4</td>
<td>13.5</td>
<td>14.7</td>
<td>14.1</td>
<td>14.0</td>
</tr>
<tr>
<td>80</td>
<td>8.9</td>
<td>9.7</td>
<td>9.9</td>
<td>12.1 (10.4)</td>
<td>10.3</td>
</tr>
<tr>
<td>100</td>
<td>5.5</td>
<td>11.1</td>
<td>5.1</td>
<td>5.2</td>
<td>5.4</td>
</tr>
<tr>
<td>120</td>
<td>5.3</td>
<td>9.8</td>
<td>4.2</td>
<td>6.0 (5.1)</td>
<td>5.6</td>
</tr>
</tbody>
</table>

Table 3: Tested and predicted ultimate shear force capacity (kN). For the results in boldface, the ECS approach gives unrealistic results, and results highly dependent on the value of the rolling shear modulus. Values in parenthesis refer to evaluating the ultimate load at a crack length of 12 mm.
involves calibration to test results in terms of choice of crack length, as mentioned previously.

The two methods based on 2D FE-analyses give credible predictions, and the relative influence of the notch depth is indeed in accordance with the test results. It should be underlined that no detailed calibration of material parameters such as fracture energies was done.

An important outcome is also that the assumption of $G_C = G_I$, during crack propagation, assumed in the LEFM analyses, seems accurate. Even when mixed mode behaviour is accounted for, in the 2D non-linear analyses, that mixed mode behaviour seems to have a very limited influence on the predicted load bearing capacity. Another observation is that, since LEFM and non-linear fracture theory give similar results, the influence of local material strength is very limited. Thus, failure is instead (in terms of material properties) governed by fracture energy and material stiffness.

As mentioned previously, the result from the compliance method approach is in terms of crack propagation load as a function of crack length. Thus, it is in general not possible to give a specific crack propagation load without also including some other criterion, e.g. choice of critical crack length. The results from all 2D continuum element analyses are shown in Figure 8. There, the compliance method analyses are represented by curves and the non-linear analyses based on a cohesive zone approach and the test results are represented by markers. It is seen that for all cases except 80 and 120 mm notch depths, a local maximum of the ultimate load versus crack length curve can be seen. For these cases, this local maximum is used as the ultimate load level in Table 3, whereas for the 80 and 120 mm notch depths, the initial failure load is used. As an alternative (Serrano & Gustafsson, 2006), the critical load can be defined for a crack length which depends on the material properties and the current state of mixed mode. A rough estimate of such a length, taking into account the current material properties and assuming a pure Mode I state, would be 5-12 mm depending on if a transverse layer or a longitudinal layer is considered. Thus, it is possible to define the ultimate capacity from the 2D-LEFM analyses by choosing the crack propagation load corresponding to such a crack length. The difference in results can be seen in Table 3, where the values in parenthesis are based on a 12 mm crack length estimation. For the case of a notch depth of 80 mm, using the estimate based on 12 mm crack length, an improved prediction of the failure load is obtained, from 24.2 kN to 20.8 kN, i.e. from 12.1 kN to 10.4 kN shear force (the results in Figure 8 relate to the ultimate load applied, in this case twice the shear force, cf. Figure 4).

4.2 Comparison with current ETA-approach

A comparison with the design approach of (OIB, 2017), is done in the following. First of all, it would be of interest to compare the relative influence of the notch sizes (length and depth), but limited test data is available in open sources. Therefore, the

![Figure 8: Results from FE-analyses using compliance method (curves), from analyses using a softening cohesive model and from tests (shown with markers and indicating notch depths with numbers).](image-url)
current comparison is restricted to the cases already presented here and where test results are available in Friberg (2017). Since design is done based on characteristic, 5% values, it is difficult to compare with results from a limited test series. Therefore, the comparison is made with the relative influence of notch depth on the predicted shear force capacity. For the different approaches and for the test results, the shear force capacity is normalised such that it is set to 1.0 for \( \alpha = 0.5 \). Doing so also eliminates the influence of the choice of the factor \( k_n \), cf. Equation (11). The results from this comparison are shown in Figure 9. Note that the results from the compliance-based 2D continuum approach, for notch depths of 80 and 120 mm (=relative notch depth of 0.5 and 0.75, respectively), relate to a critical crack length of 12 mm.

![Figure 9: Comparison of test results, theoretical models and design approach.](image)

The ETA (OIB, 2017) mentions that characteristic values of rolling shear strength (0.8–1.2 MPa) should be used as a basis for design. Applying the ETA approach assuming a shear strength value of 1.2 MPa and \( k_n = 4.7 \), results in a characteristic shear force capacity of the CLT-plate with a 50% notch of 2.86 kN. The tests, see Table 1, gave an average of 8.9 kN, based on 6 tests. An estimate of the corresponding characteristic capacity, using the procedure of EN 14358 (CEN, 2016), assuming 15% COV and 6 nominally equal test specimens, cf. Table 1, gives 8.9(1-2.3x0.15) \approx 5.8 \text{ kN}. Thus, it seems like the ETA approach is very much on the safe side.

At first glance, it seems like all the applied approaches predict accurately the influence of the notch depth. It must be emphasized, however, that the current investigation only has considered one lay-up, one notch length and, above all, only one orientation of the notch in relation to the main directions of the CLT. The notch orientation considered in the present work is probably relevant for supports. Another important application of notched members would be for joining CLT plates using a half-in-half type of joint, with the joining line being parallel to the main load bearing direction of the plate(s). This case has not been considered here, and it is not evident that the influence suggested by the ETA-approach is correct also for that case. It should be noted that the ETA-approach only mentions an orientation of the notch according to Figure 3, and also limits the application of the design equation to \( \alpha \leq 0.5 \).

4.3 Conclusions, recommendations for design and further work

The following conclusions, including recommendations for design, are the results from the work presented in this paper:

- A consistent application of the EC5-approach for notched solid timber beams is not useful in general, due to incompatibilities with the underlying beam theory when applied to CLT.
- Both LEFM and non-linear softening theory seem appropriate approaches in determining the load bearing capacity of notched CLT plates.
- The use of LEFM involves the non-trivial choice of critical crack length.
- It has been shown that the definition of effective member depth, \( h_{ef} \), should not include outer transverse layers at the notch.
- As regards recommendations for design, the approach of OIB (2017) can be used. However, it must be emphasised that this EC5-based method should be treated as an empirical approach, valid only for CLT lay-ups and orientations for which the expression has been calibrated.

For future work it is essential that test data be made public, such that the applicability of design formulae can be verified for more load cases and CLT lay-ups. Furthermore, it would be of great interest to extend the current study to also include notch orientations relevant for CLT-plate joints. In terms of further development of the theoretical basis, it would be of interest to apply fracture mechanics based methods using higher order beam theories, see e.g. Tessler et al. (2009). Possibly also methods developed for other layered products, such as plywood, see e.g. Nairn (2006), could be useful in order to find analytical formulations.
5 ACKNOWLEDGMENTS

The research presented has been financially supported by the Swedish Research Council FORMAS through grant 2016-01090, and by Vinnova, FORMAS and the Swedish Energy Agency under the umbrella of ERA-NET Cofund ForestValue (project InnoCrossLam). ForestValue has received funding from the EU Horizon 2020 research and innovation programme under grant agreement N° 773324. The financial support from these organisations is gratefully acknowledged.

6 REFERENCES


