J Wood Sci (2005) 51:455–461
DOI 10.1007/s10086-004-0679-2

ORIGINAL ARTICLE

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Twist of wood studs: dependence on spiral grain gradient

Received: April 5, 2004 / Accepted: September 8, 2004

Abstract Distortions due to moisture changes during drying or in service are a major problem for construction timber. Twist, caused mainly by the cylindrical geometry, the orthotropic nature of the wood material, and the tendency of the wood fibers to grow in a spiral around the stem, is often regarded as the most detrimental distortion of sawn timber. There is a need for a basic mechanical understanding of how the twist distortion arises and also a need for a simple formula to predict the amount of twist distortion. In this article such a formula is proposed, and theory and experimental data that indicate the validity of the formula are shown. The first term in the formula is a modification of a traditional expression which is proportional to the mean value of the spiral grain angle in the cross section in question. The second term in the formula is new and is proportional to the gradient of the spiral grain angle, and this term normally counteracts the first term so that a stud with a left-handed spiral grain might achieve a right-handed twist. Linear elastic finite element method (FEM) results and comparisons with experimental data show that the formula works well and that linear FEM calculations exaggerate the twist, which is probably partly due to nonlinear effects. The formula could be used to predict the twist of sawn timber from measured spiral grain angles on the log surface.

Key words Wood · Spiral grain · Grain angle · Twist · FEM

Introduction

The goal of the sawmill industry is to produce straight sawn timber to be used as building material. Thus, distortions due to moisture changes during drying and during service are a major problem for construction timber. Distortion is divided into crook, bow, cup, and twist, although twist is often regarded as the most detrimental distortion. The causes of distortions are mainly the cylindrical geometry caused by the growth rings and the orthotropic nature of the wood material, i.e., varying shrinkage and elasticity in the radial, tangential, and longitudinal directions. Twist is mainly caused by the tendency of the wood fibers to grow in a spiral around the stem, which causes the sawn and dried timber to twist. Knots and other kinds of local fiber variations also influence distortion to a great degree.

Spiral grain angle can be measured in studs and in logs with older manual methods[1] or automatically[2] or by computerized means.[3] Measurements of spiral grain angle and its correlation to twist angle and other parameters, such as distance from pith, log diameter, compression wood, and growth conditions, have been made by many researchers. Sawn timber in general is a material with varying properties due to circumstances such as the different growth conditions of individual trees, and statistical methods must be used in order to handle the large spread in the measured data. Nyström[2] has measured twist angle and spiral grain angle on log surfaces and found a strong correlation. Forsberg and Warensjö[4] have found that the spiral grain angle on a log surface and the slope of the spiral grain angle curve are strongly correlated to twist angle. Säll[5] and Johansson et al.[6] have measured twist and spiral grain angle in different ways and found correlation. Older experimental results are described by, among others, Forsberg and Warensjö.[4] Ormarsson et al.[7] have calculated twist distortions for sawn timber using finite element methods (FEM).

Even though FE calculations can give detailed information about drying distortions such as the twist of sawn timber, there is a need for a basic mechanical understanding of the cause of twist distortion, and, preferably, a simple formula to make rapid calculations and predict twist distortions. Until now, the formula

$$\varphi = \frac{l}{r}\frac{2s\theta}{(1+s)} \qquad (1)$$

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derived in the late 1950s by Stevens and Johnston[8] has often been used to calculate twist distortions. The formula is valid for the twist angle φ of a thin-walled cylinder of wood with the center axis along the pith axis, length l, radius r, thickness t, where $t \ll r$, spiral grain angle θ, and the relative shrinkage s across the grain. In spite of the formula's validity only for thin cylinders of wood, it is also often used for the prediction of all sorts of twist in sawn timber. In these cases the distance from the pith to some point in the cross section in question, e.g., the middle point, is used as the "mean" radius r in the formula.

In this article, the shortcomings of using Eq. 1 for calculating theoretical twist angles for sawn timber are discussed, and a new formula is proposed where the spiral grain angle and the gradient of the spiral grain angle with respect to the radius and the distance to pith are included. A physical explanation and motivation for the terms in the formula are given, as are FE calculations and measurements which justify the modified formula.

Theory

Twist of a thin-walled cylinder

A thin-walled cylinder (cf. a single growth ring) is considered (see Fig. 1). The length is l, the radius is r, the spiral grain angle is θ (>0 for right-handed spiral grain), and the shrinkage coefficients are α_t in the cross-fiber direction and α_l in the fiber direction. The wall thickness is negligible compared to the radius, and the shrinkage coefficients are defined as relative length change per change in moisture ratio Δw. Δw (<0 for drying) is the ratio of the change of mass of the moisture to the mass of the dry wood, but only the moisture mass change below the fiber saturation point is considered. A fibre coordinate system r-t-l is defined for a point on the cylinder surface in the radial, tangential, and fiber directions and a cylinder coordinate system x-y-z is defined along the radial, tangential, and axial directions. Presuming we have no stresses, the normal strains due to Δw are $\varepsilon_t = \alpha_t \Delta w$ and $\varepsilon_l = \alpha_l \Delta w$. After a rotation θ in the t-l plane, by using Mohr's strain circle, we get

$$\varepsilon_y = \alpha_t \Delta w,\ \varepsilon_z = \alpha_l \Delta w,\ \gamma_{yz} = -2\Delta w(\alpha_t - \alpha_l)\theta, \tag{2}$$

where it is assumed that θ is small. The strains ε_y and ε_z represent changes in circumference and length of the cylinder, respectively, and the shear strain γ_{yz} leads to a twist of the cylinder. From the elementary theory of torsion of circular shafts (e.g., see Timoshenko and Goodier[9]) we have the kinematic relation

$$\gamma_{yz} l = \varphi r \tag{3}$$

where φ is the twist angle of the cylinder (>0 for right-handed twist). Eliminating γ_{yz} with Eqs. 2 and 3 we get

$$\varphi = -\frac{l}{r} 2\Delta w(\alpha_t - \alpha_l)\theta \tag{4}$$

which is essentially the same formula as that of Stevens and Johnston[8] if $-\Delta w \alpha_t$ is replaced with s and α_l is set to zero. Because $\alpha_t > \alpha_l$, drying ($\Delta w < 0$) will give φ the same sign as θ, i.e., a right-handed spiral grain will result in a right-handed twist and vice versa. The twist distortion appears without stresses.

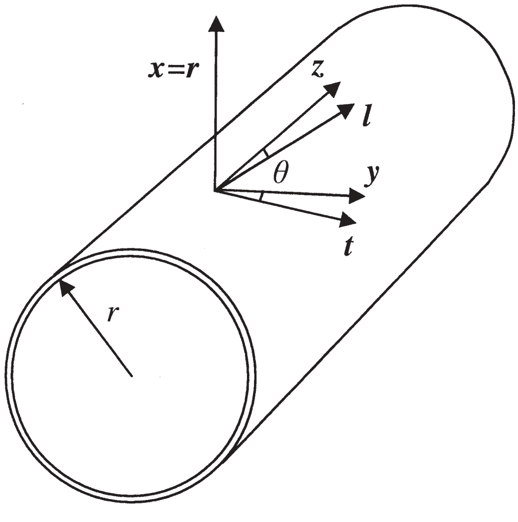


Fig. 1. Thin-walled cylinder with radius r, length l, and spiral grain angle θ

Twist of a circular, thick, solid cylinder

Here we disregard radial shrinkage and approximate a solid cylinder with outer radius r_y as a set of concentric thin-walled cylinders with varying radii coupled in parallel via end torques. Each thin-walled cylinder has the same shear modulus and is able to twist due to the end torques. The spiral grain angle is considered a function of the radius, $\theta = \theta(r)$. We use the condition that the angle of twist is equal and that the resulting torque is zero from all the thin-walled cylinders and we get the twist angle

$$\varphi = -\frac{l}{r_y^4} 8\Delta w(\alpha_t - \alpha_l)\int_0^{r_y} \theta r^2 dr. \tag{5}$$

If θ is constant and not a function of the radius, then using Eq. 5 we find that Eq. 4 can be used for a solid cylinder if $r = r_m = 3r_y/4$ is used as a "mean" radius in Eq. 4. If $\theta(r)$ is a linear function

$$\theta(r) = \theta_0 + \left(\frac{d\theta}{dr}\right)_0 r \tag{6}$$

where $\left(\frac{d\theta}{dr}\right)$ is the (constant) gradient of the spiral grain angle function, then using Eq. 6 in Eq. 5 we conclude that Eq. 4 gives the same result as Eq. 5 if "mean" values $r = r_m = 3r_y/4$ and $\theta = \theta_m = \theta(3r_y/4)$ is used in Eq. 4. In this case the resulting twist distortion creates self-equilibrating shear

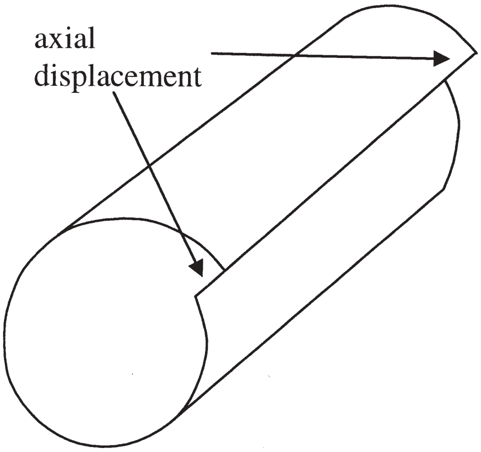


Fig. 2. Warp of a thin-walled cylinder with an axial cut exposed to a shear strain γ_{yz}

stresses in the cylinder, but the twist angle is not dependent on the size of the shear modulus.

Twist of thin-walled and solid cylinders with axial cuts

In both of the two cases treated above, the cross sections of the twisted bodies will not warp; i.e., they will remain plane due to the cylindrical symmetry. However, when timber is sawn, the growth rings are cut and the cross sections of the sawn timber will consist of only parts of growth rings. Thus, there is no cylindrical symmetry, and the cross sections may warp; i.e., the cross sections do not remain plane. As an example we may think of a thin-walled cylinder with an axial cut. If the cylinder surface exhibits a shear strain γ_{yz}, then the cut axial adjacent edges may move axially relative to each other (i.e., the cross section warps) and form what is called a screw dislocation instead of receiving a twist distortion (see Fig. 2). In practice, a thin-walled cylinder with an axial cut will receive a combination of twist and warp, and thus the twist angle is reduced compared with the twist angle of an uncut cylinder. Likewise, a solid cylinder with an axial cut from the outside to the pith will also have cross sections which will warp, and the twist angle will be less than the twist angle for an uncut solid cylinder. In practice we may multiply Eq. 4 with a constant factor C, $0 < C < 1$ in order for it to be valid also for a thin-walled or solid cylinder with an axial cut

$$\varphi = -C\frac{l}{r_{\mathrm{m}}}2\Delta w(\alpha_t - \alpha_l)\theta_{\mathrm{m}}. \tag{7}$$

Twist of a thin strip

The twist of a thin strip is interesting to study as a simple extreme case, because sawn timber with cross sections lying near the outside of thick logs will have rather flat growth rings. Here the thin strip is interpreted as a part of the

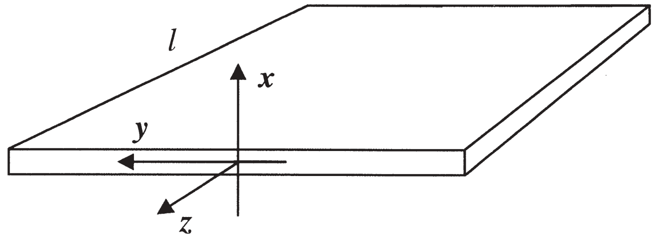


Fig. 3. Thin strip to be twisted around z-axis

surface of a thin-walled cylinder with a very large radius. The twist of a thin strip loaded with torques at the ends is treated in elementary solid mechanics textbooks (e.g., see Timoshenko and Goodier[9]), but instead, here we treat a variant of the elementary problem where the twist angle φ of the strip as a function of the shear strain γ_{yz} due to shrinkage is sought. We study the strip in a Cartesian coordinate system x-y-z, where x is pointing in the radial direction, y in the tangential direction, and z in the axial direction of the strip (see Fig. 3). The strip is thin in the x-direction, and thus the shear strains $\gamma_{xy} = \gamma_{xz} = 0$, and the only nonvanishing shear strain is γ_{yz}. Therefore, γ_{yz} from Eq. 2 and a linear θ variation from Eq. 6 give

$$\gamma_{yz}(x) = -2\Delta w(\alpha_t - \alpha_l)\left(\theta_0 + \left(\frac{d\theta}{dx}\right)_0 x\right) = \gamma_0 + \left(\frac{d\gamma_{yz}}{dx}\right)_0 x. \tag{8}$$

We now divide the influence on the twist angle φ from γ_{yz} into two separate parts, namely, first the influence of the first term γ_0 and the influence of the second term $\left(\frac{d\gamma_{yz}}{dx}\right)_0 x$. We then realise at first that the constant part γ_0 will not affect the twist, because the strip is able to get a simple shear deformation in the y-z plane due to γ_0. However, when it comes to the influence of the second term, which is proportional to x, such a simple shear deformation is not possible. Instead, the result is a twist angle φ which we show as follows. Twist deformation of the thin strip gives displacements

$$u_x = -\frac{\varphi}{l}yz,\ u_y = \frac{\varphi}{l}xz,\ u_z = \frac{\varphi}{l}\psi(x,y) \tag{9}$$

according to Saint-Venant's theory of torsion.[9] u_x, u_y, and u_z are displacements in the x, y, and z directions, respectively; ψ is the warping function, and l is the length of the strip. The displacement/strain relations give zero normal strains, but the shear strains become

$$\gamma_{xy} = 0,\ \gamma_{yz} = \frac{\varphi}{l}\left(\frac{d\psi}{dy} + x\right),\ \gamma_{xz} = \frac{\varphi}{l}\left(\frac{d\psi}{dx} - y\right). \tag{10}$$

Since $\gamma_{xz} = 0$ is a condition due to the thinness of the thin strip, Eq. 10 gives the solution $\psi = xy$. This solution also fulfils Laplace's equation $\Delta\psi = 0$, which is a necessary requirement in torsion theory. Now Eq. 10 with $\psi = xy$ gives

$$\gamma_{yz} = 2x\frac{\varphi}{l}, \tag{11}$$

which is the relation between shear strain and twist we seek. Equation 11 with γ_{yz} from the second term of Eq. 8 finally gives the twist angle

$$\varphi = -l\Delta w(\alpha_t - \alpha_l)\left(\frac{d\theta}{dx}\right)_0. \tag{12}$$

Thus, the twist of a thin strip is not dependent on the constant part of $\theta(x)$ but instead is proportional to the gradient of θ. Because $\alpha_t > \alpha_l$, drying ($\Delta w < 0$) will give φ the same sign as $\left(\frac{d\theta}{dx}\right)_0$; i.e., a positive spiral grain gradient (θ increases with increasing radius) will give a positive φ and vice versa. The sum of influences on φ of Eqs. 12 and 4 contains two terms which can act opposite to each other, as is the case for Norway spruce. Norway spruce normally has a left-handed spiral grain ($\theta_0 < 0$) at the pith which linearly changes toward a right-handed spiral grain at the log surface ($\left(\frac{d\theta}{dx}\right)_0 > 0$).[2,5]

Twist of studs with arbitrary cross sections

A dimensional analysis of the twist angle of a stud with a specified cross section $b \times h$ cut from a log at different distances r_m from the pith (see Fig. 4) is done under the following assumptions: θ is a linear function (see Eq. 6) which here is characterised by a value θ_m in the middle of the cross section, and the constant gradient $\left(\frac{d\theta}{dr}\right)_0$. φ is proportional to $-\Delta w(\alpha_t - \alpha_l)l$ and is a function of θ_m, $\left(\frac{d\theta}{dr}\right)_0$, r_m, and the cross section width b and height h. Then $\varphi/[-\Delta w(\alpha_t - \alpha_l)l]$ is a function of θ_m, $\left(\frac{d\theta}{dr}\right)_0$, r_m, b, and h.

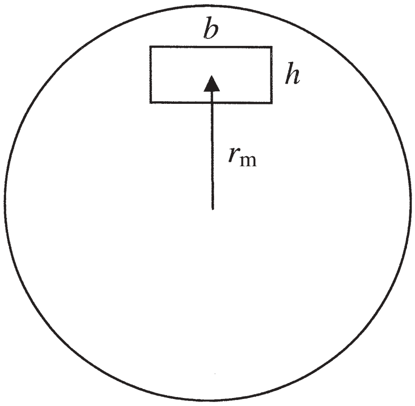


Fig. 4. Place of cross section of stud in log

According to standard theory of dimensional analysis, $\varphi r_m/[-\Delta w(\alpha_t - \alpha_l)l]$ must then be a function of the four dimensionless variables θ_m, $\left(\frac{d\theta}{dr}\right)_0 r_m$, b/h, and b/r_m. Rewriting and expanding φ as a series according to Taylor's formula, retaining only the linear, first-order terms and using the condition that $\varphi = 0$ if $\theta_m = \left(\frac{d\theta}{dr}\right)_0 = 0$ gives as the only option the first-order approximation

$$\varphi = -l\Delta w(\alpha_t - \alpha_l)\left(C\frac{2\theta_m}{r_m} + D\left(\frac{d\theta}{dr}\right)_0\right) \tag{13}$$

where C and D are undetermined constants. The reason for choosing $2C$ and not C as a constant is in order to keep similarity with Eqs. 4 and 7. Now, in view of the discussion of the case of the twist of a thin-walled cylinder and the twist of a thin strip, the two terms in Eq. 13 can be physically explained as follows: realistic studs have cross sections that consist of more or less complete growth rings, growth-ring half rings, or parts of growth rings that are quite flat. The twist angle can be approximated as a sum of contributions from both the effect of spiral grain of growth ring cylinders according to Eqs. 4 and 7 and the effect of the spiral grain gradient of flat growth rings according to Eq. 12. The C and D constants determine the contribution of each term, and we expect them to be of the order of magnitude +1. The second term explains the perhaps surprising result from FE calculations that Eq. 4 does not explain, namely, that a stud with a cross section where the middle point and also all other points in the cross section have a left-handed spiral grain can still exhibit a right-handed twist after drying.

With Eq. 6 we can substitute θ_m for θ_0 in Eq. 13 and get

$$\varphi = -l\Delta w(\alpha_t - \alpha_l)\left(C\frac{2\theta_0}{r_m} + (2C + D)\left(\frac{d\theta}{dr}\right)_0\right). \tag{14}$$

Solving for the cross section radius r_{m0} which will give twist angle $\varphi = 0$, gives

$$r_{m0} = -\frac{2C}{(2C + D)}\frac{\theta_0}{\left(\frac{d\theta}{dr}\right)_0}. \tag{15}$$

If the r value where $\theta = 0$ is notated as $r_{\theta 0}$, then from Eq. 15 we get

$$r_{m0} = \frac{2C}{(2C + D)}r_{\theta 0}. \tag{16}$$

Equation 16 shows that if $r_{m0} = r_{\theta 0}$, then $D = 0$ and no gradient term exists. If $r_{m0} \neq r_{\theta 0}$ then the gradient term exists and $D \neq 0$. This fact can be used to experimentally prove the existence of the second term; i.e., that $D \neq 0$. There is no influence from the cross section dimensions b and h in the first-order approximation according to Eq. 13, but such an influence will exist if a second-order analysis is made.

Table 1. Orthotropic material constants

	Direction		
	Radial *(r)*	Tangential *(t)*	Fiber *(l)*
Elastic modulus E_r, E_t, E_l (MPa)	400	220	9700
Shear modulus G_{rt}, G_{rl}, G_{tl} (MPa)	25	400	250
Poisson's ratio $\vartheta_{r\varphi}$, ϑ_{rz}, $\vartheta_{\varphi z}$	0.55	0.0124	0.0136
Moisture expansion coefficients α_r, α_t, α_l	0.19	0.35	0.0045

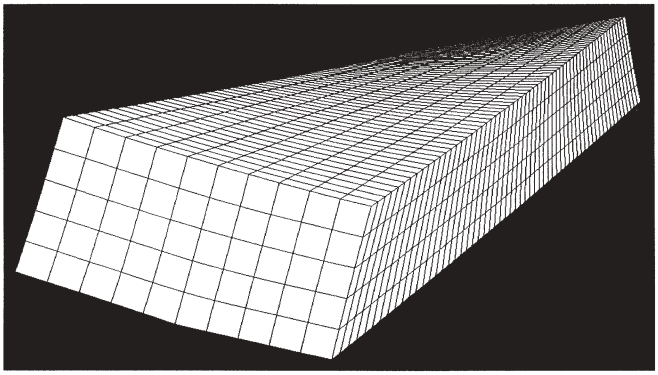
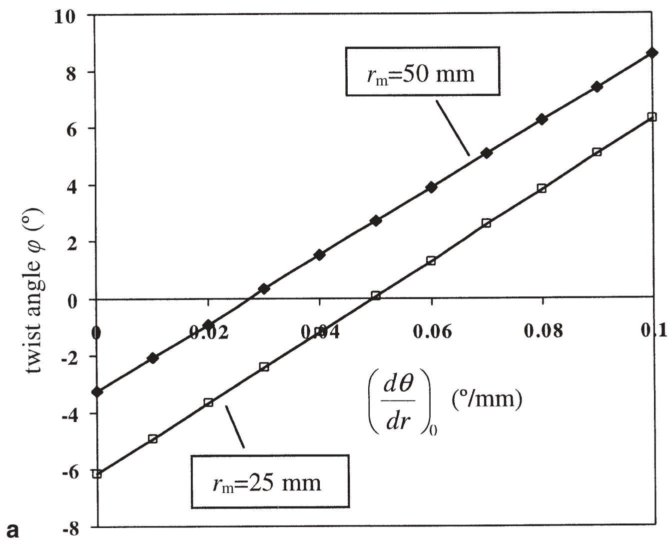


Fig. 5. Finite element method (FEM) calculated deformed shape of 50 × 100-mm stud with $r_m = 25$ mm, $\theta_m = -3°$, $l = 3$ m, $\left(\frac{d\theta}{dr}\right)_0 = 0$, $\Delta w = -0.11$, $\alpha_t - \alpha_l = 0.345$. The deformation is exaggerated by a factor 2



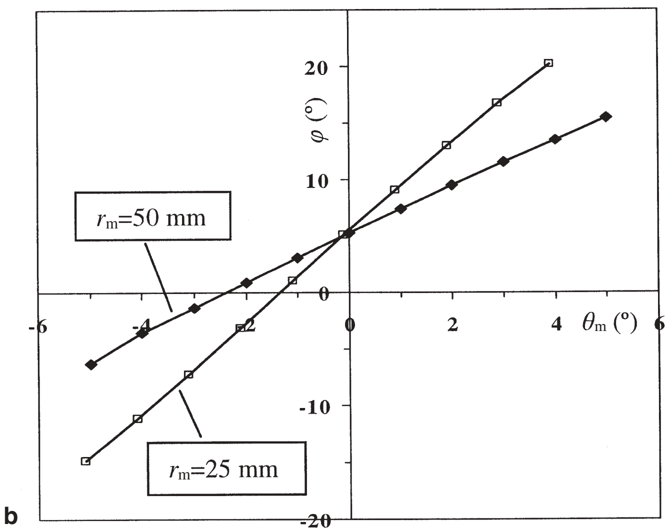


Fig. 6. **a** Twist angle φ as a function of $\left(\frac{d\theta}{dr}\right)_0$ for $\theta_m = -1.5°$. **b** Twist angle φ as a function of θ_m for $\left(\frac{d\theta}{dr}\right)_0 = 0.044°/\text{mm}$. Calculated with FEM for $r_m = 25$ mm and 50 mm for a 50 × 100-mm cross section with $l = 3$ m, $\Delta w = -0.11$ and $\alpha_t - \alpha_l = 0.345$

Materials, methods, and results

FEM calculations were made using ABAQUS[10] with a cylindrical, linear elastic orthotropic material model for 50 × 100-mm cross sections. The FEM model has 50 × 10 × 50 parabolic elements and the boundary conditions only restrict rigid-body movements. The elastic, orthotropic material data (see Table 1), are valid for Norway spruce and taken from Ormarsson et al.[7] A typical deformed stud is shown in Fig. 5.

Twist angles when drying studs are calculated with FEM for different values of θ_m, $\left(\frac{d\theta}{dr}\right)_0$, and r_m. The FEM results for $r_m = 25$ mm and $r_m = 50$ mm are shown in Fig. 6a and 6b as the influence of $\left(\frac{d\theta}{dr}\right)_0$ on φ for constant θ_m and the influence of θ_m on φ for constant $\left(\frac{d\theta}{dr}\right)_0$, respectively. The linear behaviour shown in Fig. 6a and 6b agrees with the predicted linear behaviour of Eq. 13 and an evaluation of C and D by fitting Eq. 13 to the FEM results for varying r_m values gives the C and D values shown in Fig. 7. $C = 0.48$ and $D = 1.05$ are chosen here as the C and D values that make Eq. 13 agree with linear FEM.

Twist angles φ for studs have been measured[11] and correlated to the spiral grain angle measured on the stud surface θ_{stud}. The result is $\varphi = 1.23\ \theta_{stud}$ for 50 × 100-mm cross sections with $r_m = 25$ mm, $\Delta w = -0.1$, $l = 1$ m, and $\alpha_t - \alpha_l = 0.345$. This result corresponds to Eq. 13 with $C = 0.37$ and $D = 0.74$.

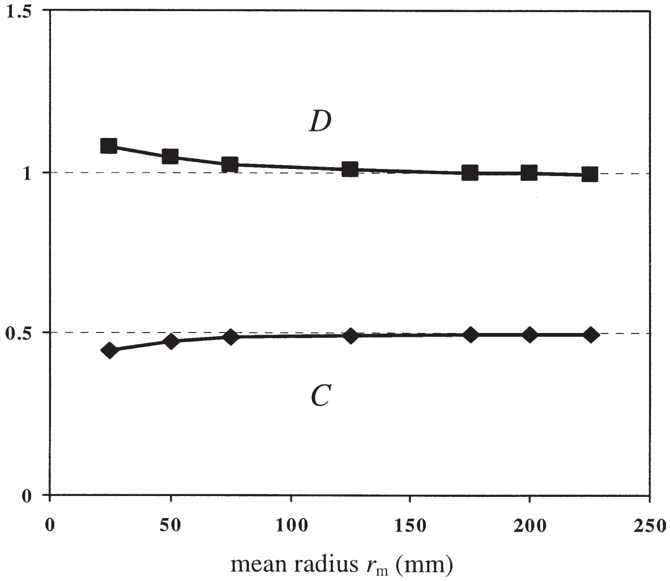


Fig. 7. C and D constants for varying radii r_m evaluated from FEM calculations on 50×100-mm cross sections with $\theta_0 = -3°$ and $\left(\frac{d\theta}{dr}\right)_0 =$ 0 and 0.03°/mm, respectively. $l = 3$ m, $\Delta w = -0.11$, $\alpha_t - \alpha_l = 0.345$

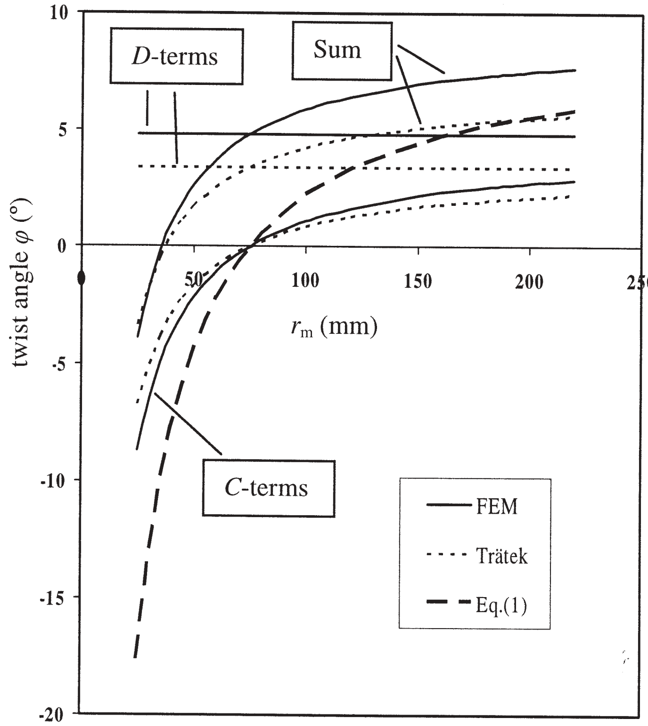


Fig. 8. Twist angle φ according to Eq. 13 shown as divided in the C term and the D term and the sum of the terms and according to Eq. 1. Linear FEM has $C = 0.48$ and $D = 1.05$ and the Trätek[11] measurement has $C = 0.37$ and $D = 0.74$. $\theta_0 = -3.3°$, $\left(\frac{d\theta}{dr}\right)_0 = 0.044°$mm , $l = 3.0$ m, $\Delta w = -0.10$, and $\alpha_t - \alpha_l = 0.345$

Figure 8 shows the influence of each of the two terms of Eq. 13 on φ for the two sets of C and D constants mentioned above, namely, $C = 0.48$ and $D = 1.05$ from linear FEM and $C = 0.37$ and $D = 0.74$ from Trätek[11] measurements. Equation 1 is also shown in Fig. 8.

Discussion

Figure 7 shows that the C and D values are reasonably constant, and therefore Eq. 13 has good accuracy compared with the FEM calculations for r_m values in the interval of 25 to 225 mm. The linear relation between $\left(\frac{d\theta}{dr}\right)_0$ and φ for constant r_m and θ_m shown in Fig. 6a indicates the validity of Eq. 13 and the influence of the second term in Eq. 13. In the same way, Fig. 6b shows the linear relation between φ and θ_m for constant $\left(\frac{d\theta}{dr}\right)_0$ and r_m, which shows the influence of the first term of Eq. 13. The FEM results confirm that a cross section with negative θ values in all material points all over the cross section may achieve a positive twist if the gradient $\left(\frac{d\theta}{dr}\right)_0$ is positive enough.

Comparison with the experimental results obtained at Trätek[11] (see Fig. 8 and Table 2) show that Eq. 13 overestimates the twist if the C and D values which conform to linear elastic FEM calculations are used. If the C and D values are chosen to make Eq. 13 fit to the Trätek[11] experimental results, then we get lower C and D values. The reason for the overestimation of twist in the FEM calculations may be the linear elastic assumption in the FEM calculations, which excludes plastic and creep effects. In practice, this reduces the drying deformations and makes the FEM calculation exaggerate the twist. Also, Δw is not constant in practice, as is assumed in the FEM calculations, but instead can vary over the cross section. Thus, Eq. 13 with $C = 0.48$ and $D = 1.05$ gives an upper limit for the twist angle φ, which can possibly be reached for a stud with no constraints during drying and exposed to a very slow low-temperature drying course of events with small creep or plastic effects. Figure 8 shows the relationship between the C term and the D term in Eq. 13 and that the D term is larger than the C term for $r_m > 35$ mm. Also Fig. 8 shows that Eq. 1 overestimates φ for small r_m values. This behavior was also observed by Säll[5] and Johansson et al.[6]

Table 2. Evaluated C and D factors

Method	C	D
From linear FEM	0.48	1.05
From experiments by Trätek[11]	0.37	0.74

Equation 13 with C and D values adjusted to fit the situation in question, whether it is an experimental or a theoretical situation, could be a good choice for calculating the twist angle φ for a stud, knowing the spiral grain angle θ at an arbitrary point in the log cross section, e.g., a point on the log surface or a point on the stud surface. One practical use of the formula is to predict the twist a certain stud will achieve after drying with the aid of measured θ angles on the log surfaces, e.g., in a sawmill.

Acknowledgment The author expresses his gratitude to Formas (the Swedish Research Council for Environment, Agricultural Sciences, and Spatial Planning) for their support.

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The publication of this article was made possible by an Emachu Research Fund. The author is grateful for the fund.