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# Derivation of Nominal Strength for Wood Utility Poles

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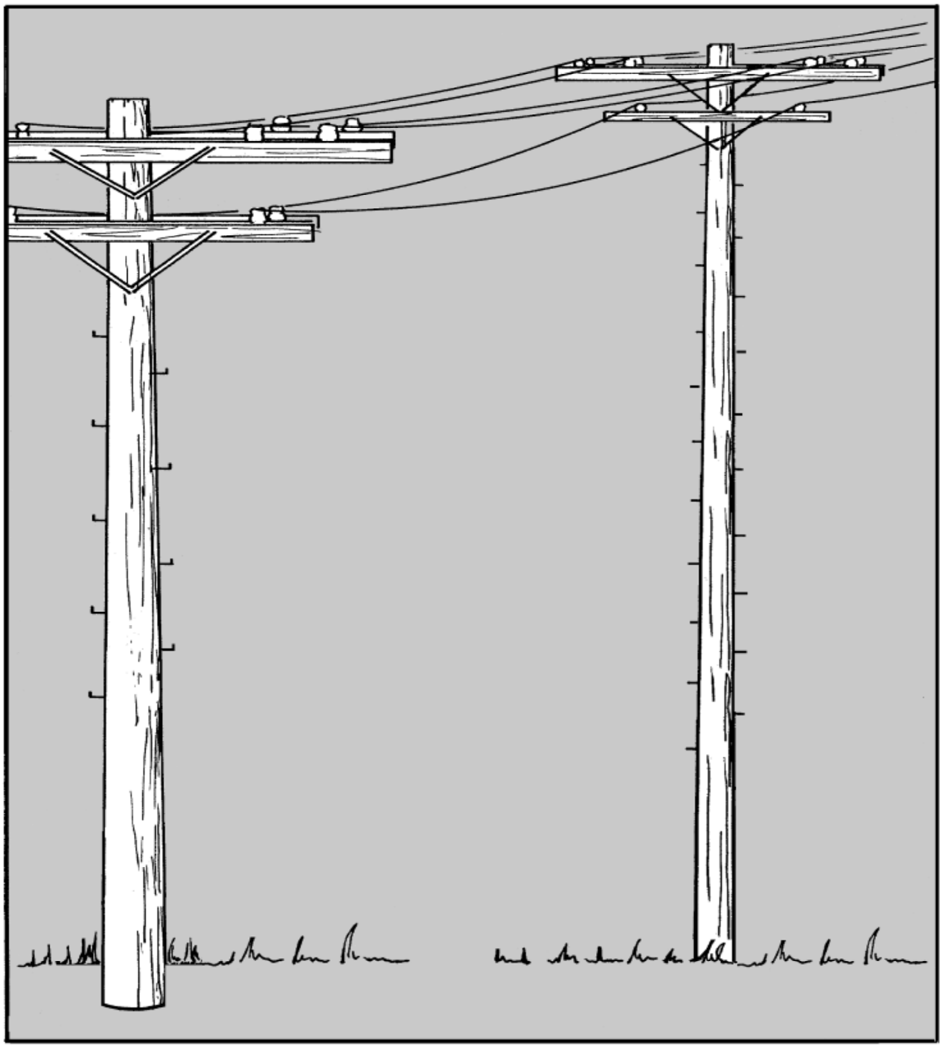

# Abstract

The designated fiber stress values published in the American National Standards Institute Standard for Poles, ANSI O5.1, no longer reflect the state of the knowledge. These values are based on a combination of test data from small clear wood samples and small poles (<55 ft (<17 m)) and field experience up to the time of adoption of the standard in 1965. A number of changes over the past 35 years require that the wood pole industry update the basis for the ANSI fiber stress values if it is to maintain a lead role in the utility pole market. Changes that will impact wood pole design include new data for larger wood poles, increased pressure from competing materials, and the evolving transition from Allowable Stress Design (ASD) to a reliability-based Load and Resistance Factor Design (LRFD) format. This paper presents an approach to updating the basis for deriving fiber stress values for wood poles, which will provide uniform reliability across class sizes as well as species. We review the current basis for ANSI fiber stress values and recent pole test data. Our work suggests that adjustments such as those for load sharing and moisture effects be considered load factors rather than material factors and recommend a method of calibrating the new LRFD format to the ASD approach.

Keywords: standard, wood, utility, poles, load and resistance factor design, LRFD, nominal resistance.

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# Derivation of Nominal Strength for Wood Utility Poles

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## Introduction

Wood utility pole standards have been in existence for more than 70 years, but the last major revision of the American National Standards Institute (ANSI) O5 standard was written 35 years ago. The data analysis method and committee decisions that form the basis for deriving "designated fiber stresses" published in the ANSI O5.1 standard "Specifications and Dimensions for Wood Poles" (ANSI 1997) were summarized by Wood and Markwardt (1965). Committee decisions were based on a combination of full-size pole test results (Wood and others 1960), published small clear specimen strength values (ASTM 1998), and pole performance history. Adjustment factors for load sharing, moisture content, and conditioning effects that were not considered in the testing phase were discussed and adopted by consensus vote.

Over the past 35 years, the states of the art and knowledge of utility structures and wood poles have been enhanced by new test data and more thorough evaluation (Bodig and Arnette 2000, Bodig and others 1986a,b, IEEE 1997). The ANSI O5 committee acknowledged these changes by adding an annex to the ANSI O5.1, which discusses application of reliability based design. However, no further action was taken to incorporate the new data into the derivation of nominal resistance for utility poles.

## Objective

In-service moisture content and critical stress location are functions of the design environment and loading conditions. The application of a moisture adjustment should therefore be done at the discretion of the designer. Based on the state of the knowledge of strength–moisture relations, the moisture adjustment recommended by Wood and Markwardt (1965) ($K_m = 1.10$) appears to be more appropriate as a generic adjustment than is the 16% increase inherent in the current ANSI fiber stress values.

## Current ANSI O5.1 Standard

The current ANSI O5.1 standard incorporates several legitimate adjustment factors, but some of these are not sufficiently documented or explained. These factors, summarized by Wood and Markwardt (1965), include adjustments for geometric form, moisture content, pretreatment conditioning, size classification, and load sharing. Although not all these adjustments are directly applied, all are discussed as justification for the assumptions made in one or more fiber stress derivations. The designated fiber stress values listed in table 1 of ANSI O5.1 represent a consensus decision rather than a standard analytical procedure. They were derived from a combination of small clear specimen tests, full-size pole tests, and engineering judgment.

### Form Factor

Form factor refers to the adjustment of bending strength to make values derived from square members applicable to round ones. Newlin and Trayer (1924) found that round members have the same bending strength as that of rectangular members of the same cross-sectional area despite having an 18% smaller section modulus. This indicates that when the standard bending stress equation (bending moment divided by section modulus) is used, the round section exhibits higher stress at failure.

Wood and Markwardt (1965) referenced the work of Newlin and Trayer (1924) to support the concept of a stress adjustment for predicting round wood capacity using small clear specimen test data, but they did not use the recommended factor. They chose instead to compare the strength of full-size poles with that of matched small clear specimens, referencing the ASTM program as the source of pole to small clear specimen strength ratio values. Strength ratios were tabulated for six species. Similar tables were given in the ASTM pole report (Wood and others 1960) and at least one interim report (Wood 1956), but because the values in those reports do not agree, it is unclear how the strength ratios were derived. The ANSI O5 committee eventually adopted an 8% increase for the change from small clear bending strength to full-size pole strength.

## Moisture Content Adjustment ($K_m$)

Wood and Markwardt (1965) recommended a 10% increase to the green modulus of rupture (MOR) values for poles dried in service. Their recommendation was based on summaries of several studies related to in-service moisture content and pole strength. Work by Wilson and others (1923, 1930) suggested MOR increases of 17% and 10% for tamarack and shortleaf pine poles, respectively, when dried from green to 20% moisture content. Surveys conducted by the Rural Electrification Administration indicated that more than 96% of all distribution-size poles surveyed had less than 20% moisture content at 4 ft (1.2 m) above ground (Wood and Markwardt 1965). Of the nine species included in this survey, southern yellow pine (called southern pine here) was the only species for which more than 4% of in-service poles had more than 20% moisture content at 4 ft (1.2 m) above ground.

In 1963, the ANSI O5 committee adopted a 16% increase for drying on the premise that the moisture content of in-service poles will rarely exceed 20% at 4 ft (1.2 m) above ground, the most likely location for distribution-size pole failure. Information provided in the *Wood Handbook* (Forest Products Laboratory 1999) led Wolfe (2000) to conclude that a decrease in moisture content from fiber saturation point to 20% leads to an increase in the moment capacity of round timber that ranges from 1% for southern pine to 25% for western hemlock. In deriving these values, Wolfe accounted for both the section modulus decrease and the strength increase with drying. The range of moment capacity values reflects species differences with respect to the moisture content at which strength properties begin to exhibit a significant increase upon drying.

In-service moisture content and critical stress location are functions of the design environment and loading conditions. The application of a moisture adjustment should therefore be done at the discretion of the designer.

## Pretreatment Conditioning ($K_{pc}$)

Pretreatment conditioning improves the treatability of poles. Conditioning methods that involve high temperature, especially in the presence of steam, can have detrimental effects on wood strength (Eaton and others 1978, Wilkinson 1986). The ANSI O5.1 standard currently groups species into four treatment categories: air seasoning, Boulton drying, steam conditioning, and kiln drying. In 1965, the ANSI O5 committee recommended a 10% reduction in strength for Boultonizing and low temperature kiln drying (<174°F (<79°C)), a 15% reduction for steaming at 245°F (118°C), and no reduction for air drying (Wood and Markwardt 1965). These recommended adjustments are applied at the discretion of the design engineer. They are not incorporated in the designated fiber stress values listed in table 1 of ANSI O5.1.

## Class Size ($K_{cl}$)

The ANSI pole-size classification specifies the minimum circumference measured 6 ft (1.8 m) from the butt and at the tip of the pole. On average, pole size falls midway between successive class minimums. Pole tip circumference varies by increments of 2 in. (51 mm), and pole circumference 6 ft (1.8 m) from the butt varies by increments ranging from 2 to 4 in. (51 to 102 mm). When a pole is placed in a size class, it must meet or exceed the minimum values (tip and 6 ft (1.8 m) from the butt) specified for the pole class. Therefore, most poles in any given class are larger than the minimum specified. This effect was discussed by Colley (1932) and was incorporated, along with the effect of load sharing, in the derivation of designated fiber stress (Wood and Markwardt 1965).

The added load capacity of a size-classified pole in bending is equal to the ratio of its actual section modulus to that assumed on the basis of its minimum dimension at the point of greatest stress. With the assumption that on average the circumference of a pole at 6 ft (1.8 m) from the butt falls midway between the minimum specified for the class ($C$) of the pole and the minimum specified for the next larger class ($C_i$), an oversize factor $K_{cl}$ would be derived as

$$K_{cl} = [C + 0.5(C_i - C)/C]^3 \qquad (1)$$

As the pole circumference increases, the value of $K_{cl}$ decreases. This adjustment varies from a 19% increase for the smallest pole (ANSI class 10, 20 ft (6 m) long) to a 7% increase for the largest one (ANSI class H6, 125 ft (38 m) long). One way of dealing with a variable factor such as this is to incorporate it into the analysis of pole strength. This requires classifying each test pole and estimating its ground line stress at failure (MORGL) using the class minimum ground line circumference rather than its actual dimension. Another way to handle the adjustment is to simply give a constant increase across the board as an adjustment for the classification effect.

A 10% increase ($K_{cl} = 1.1$) to account for the effect of size classification gives more conservative designs for smaller poles.

## Load Sharing

Load sharing refers to the mechanism by which members in a system gain support from stiffer or less heavily loaded adjacent members. For utility lines, the premise behind load sharing is that when one pole deflects more than its neighboring poles, the cable that connects them will distribute load to the adjacent poles and away from the more deflected pole. The fact that wood poles are more limber than are concrete or steel poles makes them more likely to exhibit significant out-of-line deflection prior to failure and therefore more apt to redistribute load in this manner. The magnitude of load sharing, however, is strongly dependent on the line design; it is not an inherent material property.

Rule 261A2e of the National Electrical Safety Code (NESC) (IEEE 1997) describes specific conditions under which an increase in design stress of up to 33% can be applied for load sharing. However, the increase is applied to only one pole in three, where the other two poles are known to be strong enough to carry load shed by the weakest pole. The NESC requirements limit pole spacing as well as strength variation between adjacent poles and require that the wire tension, cables, and conductors be stiff enough to support the weaker pole.

Wood and Markwardt (1965) described the method used to account for load sharing and class size effect as being equivalent to recognizing a reduction in individual pole strength variability. These researchers referred to an analysis by Colley (1932) in which the effects of load sharing and pole classification were presented as equal to selecting a design basis at the mean minus one-half standard deviation. Given the assumption that poles have a strength coefficient of variation (COV) of 14% (Wolfe 2000), this is equivalent to setting the design point at 93% of the mean (1 – 0.5(0.14)). In comparison, the basis of conventional engineered wood design is the lower 5% exclusion limit:

$$\text{mean strength } [1 - 1.645(0.14)] = 77\% \text{ of mean}$$

The ANSI derivation has an inherent adjustment of 1.21 (0.93/0.77) for load sharing and classification. If we assume a 10% increase for classification, this implies a 10% increase for load sharing.

Because load sharing is not a material property, it should be removed from the derivation of designated fiber stresses and included as a design adjustment. Its use should be left to the discretion of the design engineer, and it should be endorsed by the NESC.

## Duration of Load ($K_d$)

Wood is a visco-elastic material whose strength depends on the duration of the applied load it supports. The shorter the duration, the higher the applied load the material can support.

In most standards, a duration of load factor of 1.6 is applied to adjust for the difference between test and service load conditions in wood structural components subject to bending. Strength data generated in a 10-min test are divided by 1.6 to estimate the load capacity of wood for a duration of 10 years. As peak wind and ice loads on utility poles are generally of a very short duration (less than 10 min), the test duration is considered to be conservatively representative of service conditions and the duration of load adjustment has been set at unity ($K_d = 1.0$).

## Pole Strength Derivation Assumptions

Conventional methods of designing wood pole structures rely on a conservative assessment of pole strength. Variables that influence the evaluation of pole strength include pole physical and mechanical properties, test sample preparation, test setups, and measurements and analysis of ground line stress at the failure load. Each of these variables adds to the degree of uncertainty inherent in the derivation of a nominal resistance used as the basis for design of utility structures. In addition, it is important to remember that the standard cantilever test (ASTM 1999) gives only a relative measure of pole capacity. Actual in-service loads often include some combination of bending and axial load that may influence the location as well as amplitude of maximum stress and pole load capacity. As the market becomes more competitive, there will be increased demand for improving the precision with which these variables are determined.

### Material Properties

In the case of tapered round timbers, the influence of physical property variations is often included with mechanical properties to characterize what might be classified as material properties. A pole may be classified by its capacity to carry a cantilever load or by the ground line stress corresponding to that cantilever load. In either case, this is a function of its strength, length, ground line dimension, and taper. For a given ground line circumference, the cantilever load capacity decreases as a linear function of length and taper. As taper increases, the most highly stressed cross section moves higher on the pole while the ground line stress at the failure load (MORGL) decreases. It is generally assumed that this analysis, combined with the evaluation of ground line stress at maximum load (MORGL), provides a conservative estimate of pole strength.

Theoretically, the maximum stress in a pole subject to a cantilever-type load will occur at a point where the pole circumference ($C_{fl}$) is 1.5 times that at the load point ($C_t$). If this theory could be supported, the point of maximum stress would, on average, occur at a distance equal to $0.5C_t$ divided by the pole taper below the load point.

Figure 1 shows data collected from more than 2,500 cantilever bending pole tests. In these cases, the location of the failure was measured and the circumference determined assuming a linear taper between the circumference measurements taken at the ground line and at the load point. These poles had a 3% average circumference taper, and the majority of them failed at the ground line. Of the poles that failed above the ground line, most had circumference ratios of less than 1.2 and failed in the lower third of the pole length. The values and scatter of the circumference ratios suggest that the physical and mechanical property assumptions required to support the geometric derivation of this maximum stress point are not applicable to wood poles. The approach to estimating the failure location requires more

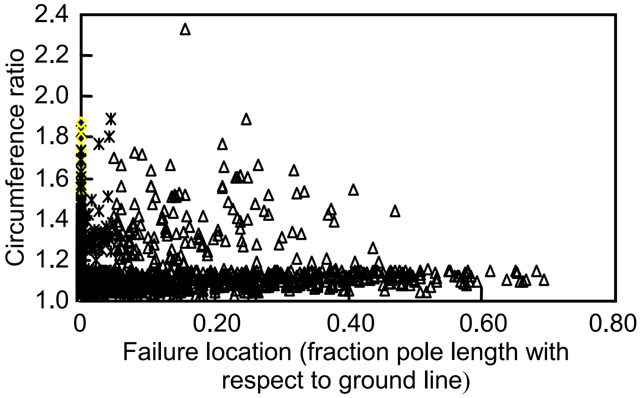

**Figure 1—Theoretical ratio of circumference at point of maximum stress to that at load point is 1.5. This assumes uniform material properties, a round section, and linear taper. Test data suggest that the supporting assumptions do not apply to wood poles.**

knowledge of individual poles, but it is no more accurate than MORGL as an estimate of pole load capacity.

## Height Adjustment

The increased use of larger poles and multiple pole structures focused attention on the inadequacy of the fiber stress values reported in the ANSI O5.1 standard. Because these numbers were based on a combination of small pole tests and strength of small test samples taken from clear mature wood within the lower 16 ft (4.9 m) of the pole, there was concern that these numbers would not apply to locations higher in the pole. In 1974, the ANSI committee adopted a model (Annex A, ANSI O5.1), proposed by Bohannan (1971), which estimates a linear reduction in strength with height ($X$) to 75% of ground line strength ($F_1$) at mid-height ($X/L = 0.5$). Beyond mid-height, the material strength is assumed to remain constant.

$$F_2 = F_1(1 - 0.5X/L) \quad (2)$$

In developing this model, a number of conservative assumptions were made to ensure a safe estimate of strength at height $X$. The derivation was based on a combination of bending tests of 50- and 55-ft (15- and 16.8-m) poles, compression and bending tests of 50-ft- (15-m-) long piles, tests of small clear samples from one 120-ft- (36.6-m-) Douglas-fir pole, and strength tests of four 100-ft (30.5-m) poles. In one case, Bohannan (1971) also considered five series of tests made to compare cantilever bending strength of upper and lower halves of 50-ft (15-m) poles and found on average that the strength of the upper half was 86% the strength of the lower half. In comparing the cantilever bending strength of 50-ft (15-m) poles that failed close to the ground line to the two-point bending strength of 50-ft (15-m) piles that failed at roughly two-thirds their length, Bohannan found strength ratios of 0.91 for Douglas-fir and 0.73 for southern pine. For the four 100-ft (30.5-m) poles, failure locations ranged from 4% to 55% of the pole length above ground line. One pole failed at 27% of its length above ground line and had a strength of only 59% of the ANSI fiber stress value. Another pole failed at 55% of its length and had a strength of 68% the ANSI value. Small clear samples taken from the 120-ft (36.6-m) Douglas-fir pole showed a 10% reduction in strength at mid-height compared with the strength at ground line.

In addition to providing a summary of the bending test results, Bohannan referenced a relationship between strength and specific gravity to support the strength reduction with height. His assumption that strength is proportional to the ratio of specific gravity to the power of 1.5 gives a conservative estimate by most accounts. For example, in the *Wood Handbook* (FPL 1999), MOR is proportional to specific gravity to the power 1.01.

The conservatism built into the height–strength relationship in ANSI Annex A was confirmed by Bodig and others (1986b). In a study of the strength of longer poles, a series of tests was conducted on individual poles to assess change in strength with height. Table C.5 of the current ANSI standard shows that a reduction in strength with height was observed for southern pine, but not for Douglas-fir or western redcedar. The reduction in mean strength for southern pine poles was on the order of 9% by mid-height.

Reduction in strength with height is generally attributed to an increased proportion of juvenile wood and the increased frequency of knots with height. These findings imply that design values must be reduced for any design that requires an assessment of pole strength above those locations where failures commonly occur in standard cantilever tests. In general, this is only an issue for multiple structures where cross bracing imposes moments above mid-height. The tabulated ANSI O5.1 pole strengths are directly applicable only to single pole structures.

## Size Effect

Size effect is another issue that needs to be addressed in the derivation of pole strength (Phillips and others 1985). Volume effect has been recognized for many engineered wood components and is commonly referred to as the Weibul weak link theory (Bohannan 1966). Although the strength–size phenomenon has both theoretical and empirical support, no research has been conducted to specifically quantify the pole strength–size relationship. The simplest approach to dealing with it is to provide an empirical evaluation of trends apparent in test data over a range of pole sizes. The simplest approach from the design standpoint is to incorporate the adjustment into the derivation of ground line stress at failure (MORGL). When designing on the basis of MORGL one must be aware that the MORGL at which a long pole is likely to fail is less than that expected of a short pole of the same species.

If a pole is designed as an element in a braced frame or as a guyed structure, where the maximum moment is not at the ground line, it is important to know how reduction in strength with height and with size interact. In general, if the MORGL value is adjusted for size on the basis of cantilever test results on a single pole, the change in strength with height is an inherent part of that adjustment for any situation where maximum stress occurs below mid-height. In situations where the maximum stress occurs above mid-height, strength reduction for height is not accounted for by the test basis of MORGL.

### Durability

Species with a natural resistance to decay tend to retain their initial strength longer than do species that are difficult to treat. This means that over the life of the utility line, reliability may decrease at a slower rate for poles of a decay-resistant species or with enhanced treatments, such as through boring. Durability is addressed by the NESC as an "at replacement" requirement, but it has not been recognized by ANSI O5 in the derivation of nominal strength.

# Proposed Change to Nominal Strength Derivation

Any proposal for derivation of pole design stress should be compatible with other standards for wood design stress, technically supportable, and easily updated as new data become available. The fact that utility poles exhibit so much variability in size, shape, and durability and are designed to carry such a wide range of load configurations adds to the complexity of developing a simple approach that will satisfy all needs.

## Nominal Resistance

Rigorous evaluation of pole test results shows that assumptions made in the derivation of the ANSI O5 "designated fiber stresses" deviate from those generally applied to other engineered applications of wood. Nominal resistance values derived for engineered wood components are normally based on the lower 5% exclusion value for individual member strength. However, the ANSI O5.1 designated fiber stress for poles does not explicitly support any specific fractile of a strength distribution for poles. This detracts from any attempt to create uniform reliability across species.

The assumptions related to drying in service and load sharing are also subject to some debate. A 16% increase for drying in service, applied uniformly across species, cannot be supported by test data. Even if it could be, there is little evidence that at the time of failure, the moisture content of most poles is below 20% in the failure region. As for load sharing, it is highly unlikely that the same adjustment is equally applicable to all pole utility structures.

## Circumference

The effects of pole size were first noticed in the analysis of more recent tests of larger poles (Bodig and others 1986ab, Phillips and others 1985). These results suggest that wood strength is slightly lower for larger poles. As discussed in the previous text, the assessment of MORGL from test data for new untreated poles includes effects that might be attributed to change in strength with height. Although the majority of individual studies show a negative effect of ground line circumference ($C_{\mathrm{gl}}$) on strength, the number of variables that influence pole strength leads to significant scatter and low correlation ($r^2 < 0.35$).

Figure 2 shows normalized strength as a function of circumference for three species: Douglas-fir, western redcedar, and southern pine. The normalized strength in this case represents the ratio of measured MORGL to the lower 90% confidence bound on the mean MORGL for each species. If each species is evaluated separately, the reduction in strength with increasing circumference is lower than this composite plot might suggest. A log–log regression was used to fit the following continuous functions to the various species plots.

These functions give some idea of how median strength varies with circumference for species included in the ANSI data base.

$$\text{Southern pine} \quad \overline{\text{MORGL}} = 25{,}739\, C_{\mathrm{gl}}^{-0.326} \tag{3}$$

$$\text{Douglas-fir} \quad \overline{\text{MORGL}} = 18{,}220\, C_{\mathrm{gl}}^{-0.257} \tag{4}$$

$$\text{Western redcedar} \quad \overline{\text{MORGL}} = 42{,}970\, C_{\mathrm{gl}}^{-0.593} \tag{5}$$

Table 1 provides the calculated range of MORGL values for these species using the smallest (class 10, 20 ft (6m)) and largest (class H6, 125 ft (38 m)) poles. Despite the relatively large scatter of data with $C_{\mathrm{gl}}$, the trend for reduction in

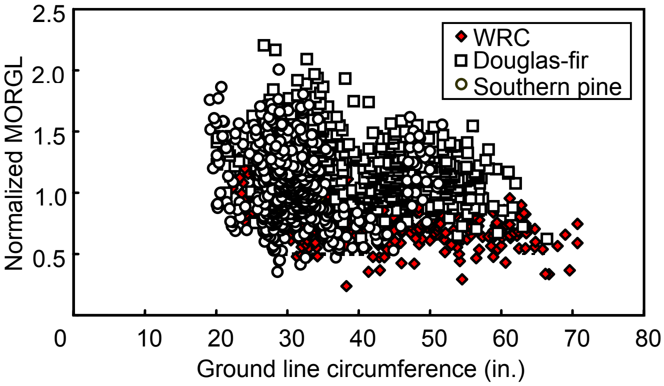

**Figure 2—Decrease in normalized MORGL with increase in ground line circumference for western redcedar, Douglas-fir, and southern pine. MORGL normalized by dividing by mean value. 1 in. = 24.5 mm.**

**Table 1—Range of MORGL values associated with the size effect**[a]

| | Class 10, 20-ft poles | | | Class H-6, 125-ft poles | | | |
|---|---|---|---|---|---|---|---|
| Species | $C_6$ (in.) | $C_{gl}$ (in.) | MORGL ($\text{lb/in}^2$) | $C_6$ (in.) | $C_{gl}$ (in.) | MORGL ($\text{lb/in}^2$) | MORGL range ($\text{lb/in}^2$) |
| Southern pine | 14 | 14.5 | 10,764 | 86 | 86 | 6,059 | 4,705 |
| Douglas-fir | 14 | 14.4 | 9,176 | 86 | 86 | 5,821 | 3,355 |
| Western redcedar | 15 | 15.8 | 8,375 | 95.5 | 95.5 | 2,919 | 5,456 |

[a] 1 ft = 0.3048 m; 1 in. = 25.4 mm; 1 $\text{in/lb}^2$ = 6.895 kPa.

strength with pole size is still significant. Ignoring it would require reducing strength values for small poles to provide safe design for the larger pole classes.

The traditional basis for wood design is the lower 5% exclusion limit (L5%EL). For poles, a log normal distribution provides a better fit to data than does a normal distribution. To facilitate evaluation of the ground line circumference effect on a log normal L5%EL of pole moment capacity, Wolfe (2000) proposed an exponential function of the form

$$\text{MORGL5\%} = AC_{\text{gl}}^{B} \qquad (6)$$

The regression equation constants $A$ and $B$ were derived using the computer program RECIPE developed by Vangel (1995, 1996) of the National Institute of Standards and Technology (see App. A). This program provides a means of accounting for variations between as well as within studies in deriving a confidence bound about a designated fractile of the strength distribution.

Values listed in Table 2 were derived to represent a 50% confidence in the L5%EL of a log normal probability distribution function. For the three major transmission pole species (Douglas-fir, southern pine, and western redcedar), values for $A$ and $B$ (Eq. (6)) were derived using a log–log linear regression. These values were derived for all pole sizes represented in the ANSI data base and thus apply to both transmission and distribution poles. For the minor distribution pole species, the value of $B$ is set equal to zero and $A$ represents the 5% tolerance value based on a log normal distribution for test results of poles classed 1 through 10.[1] The statistical evaluation of the tolerances for Douglas-fir, southern pine, and western redcedar poles based solely on poles in classes 1 through 9 gave values of 5,226, 5,220, and 3,790 $\text{lb/in}^2$ (36.03, 35.99, and 26.13 MPa), respectively. For consistency, we preferred to use the regression equations to estimate values for 40-ft (12-m) class 3 poles for these species. For other species, the values were derived using a probability distribution function consisting of poles classed 1 through 10.

**Table 2—Constants of Equations (6) and (8) derived for species in ANSI O5 data base**

| | Parameters for nominal stress | | Parameters for moment capacity | |
|---|---|---|---|---|
| Species | $A$ | $B$ | $A'$ | $B'$ |
| | Transmission poles | | | |
| Douglas-fir | 13,313 | −0.267 | 42.2 | 2.730 |
| Southern pine | 16,359 | −0.320 | 51.8 | 2.680 |
| Western redcedar | 30,515 | −0.593 | 96.6 | 2.407 |
| | Distribution poles[a] | | | |
| Douglas-fir | 13,313 | −0.267 | 42.2 | 2.730 |
| Southern pine | 16,359 | −0.320 | 51.8 | 2.680 |
| Western redcedar | 30,515 | −0.593 | 96.6 | 2.407 |
| Englemann spruce | 2,850 | 0 | 9.0 | 3 |
| Jack pine | 5,140 | 0 | 16.3 | 3 |
| Lodgepole pine | 3,600 | 0 | 11.4 | 3 |
| Northern white cedar, eastern cedar | 2,220 | 0 | 7.0 | 3 |
| Pacific silver fir | 3,980 | 0 | 12.6 | 3 |
| Port Orford cedar | 5,600 | 0 | 17.7 | 3 |
| Red pine | 4,070 | 0 | 12.9 | 3 |
| Western hemlock | 3,740 | 0 | 11.8 | 3 |
| Western larch | 6,900 | 0 | 21.8 | 3 |
| White spruce | 3,490 | 0 | 11.1 | 3 |

[a] Size effect is not considered for distribution poles.

A utility engineer responsible for designing pole structures is concerned primary with selecting the right size of pole for a given load condition. As pole tables give sizes in terms related to ground line circumference, if the pole loads can be equated to a controlling moment at the ground line, a required ground line circumference ($C_{\text{gl}}$) can be derived from the quotient of ground line moment ($M_{\text{gl}}$) and MORGL5%. For a round section, the section modulus ($S_{\text{gl}}$) is

$$S_{\text{gl}} = C_{\text{gl}}^{3} / (\pi^{2} 32) \qquad (7)$$

Substituting $M_{\text{gl}}/S_{\text{gl}}$ for MORGL5% into Equation (6) and incorporating Equation (7) for $S_{\text{gl}}$ allows the following solution for the moment at ground line:

$$M_{\text{gl}} = AC_{\text{gl}}^{B+3} / (32\pi^{2})$$

To simplify, set $A' = A/(32\pi^2)$ and $B' = B + 3$ to give

$$M_{\text{gl}} = A' C_{\text{gl}}^{B'} \qquad (8)$$

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[1] Although we chose a class 1 WRC for calibration, the definition of “distribution” pole usually starts at class 2, pole circumference ≤45 ft (≤14 m).

the required ground line circumference can be calculated as

$$C_{gl} = (M_{gl}/A')^{1/B'} \tag{9}$$

The $A'$ and $B'$ constants of Equation (8) are tabulated in Table 2. The numerical values of these constants were derived from the ANSI data base. Constants given for the minor pole species are derived as point estimates of a log normal distribution based on grouped data. Because no size effect has been assessed for these data, the value of $B$ is set to zero, giving $B'$ a value of 3.

Values determined for the minor distribution pole species should be revised as new data are collected. Test data for these pole species are limited in terms of sample size and range of pole circumference and length. Since the test data represent what is most widely used, values derived on the basis of a regression on size are not expected to vary much from those derived as point estimates.

## Design Format

The National Electrical Safety Code has proposed developing an LRFD design format for utility structures that takes the form of Equation (10):

$$\Phi R_n \geq \lambda Q \tag{10}$$

where

$\Phi$ is resistance factor,

$R_n$ nominal resistance,

$\lambda$ load factor, and

$Q$ load effect.

The current value assigned to $\Phi$ by the NESC for grade B combined ice and wind load is 0.65. This value evolved as an adjustment to a design value considered to be close to the mean strength. With the change of focus to a nominal resistance closer to the 5% exclusion limit for a given species, this adjustment should increase by roughly the ratio of the mean to the L5%EL.

Nominal resistance $R_n$ could be chosen as the value for the L5%EL stress for each material or as a reliability index $\beta$, which basically represents the probability that the pole strength will exceed the load-imposed stress. The L5%EL is a value that can easily be provided in a materials specification such as the ANSI O5.1 standard. On the other hand, $\beta$ requires knowledge of the load distribution and must be calculated as a function of the distributions of both load and resistance.

The L5%EL for MORGL (MORGL5%), estimated using Equation (6) and the constants provided in Table 2, must be adjusted for a specific design case to give the $R_n$ value for design. This requires the use of a conversion factor ($K$) which, in the case of poles, accounts for effects such as size and conditioning.

$$R_n = K(\text{MORGL5\%}) \tag{11}$$

Substituting Equation (11) into Equation (10) and rearranging gives

$$\Phi K(\text{MORGL5\%}) \geq \lambda \text{Q} \tag{12}$$

To calculate the left side of Equation (12),

$$K(\text{MORGL5\%}) = K_s K_c K_h \, A C_{gl}^{B} \tag{13}$$

where

$K_s$ is adjustment for class size effect—a value of 1.1 adopted as a conservative estimate of values calculated for $K_{cl}$ (Eq. (1)),

$K_c$ adjustment for conditioning—1.0 for air drying, 0.9 for kiln drying and Boulton drying, and 0.85 for steaming, and

$K_h$ calibration to historic precedent reflected in conventional design practice.

No adjustment is necessary for form because the proposed revision is based only on full-size pole test data.

## Calibration Adjustment

The calibration to historic precedent supports the premise that current design practice has given satisfactory performance in most cases. Laboratory tests are not intended to mimic boundary conditions and loads actually experienced by poles. Rather, they give a relative evaluation that can be used for comparing across species, size, and even materials. Any new design procedure should result in minimal change in a design that experience suggests is sufficiently reliable. If a reference condition can be identified as having borderline reliability, then that condition should be used as a calibration point (that is, design values will decrease for conditions deemed less reliable and increase for conditions deemed more reliable.)

An example of a design calibration reference condition would be the selection of a western redcedar (WRC) 65-ft (20-m) class 1 (WRC–1–65′) pole as representative of borderline reliability. For this condition, the ratio of the L5%EL implied by the current designated fiber stress to that determined from tests of new green poles would be larger than that determined for Douglas-fir or southern pine, suggesting that the WRC designated fiber stress is less conservatively derived than that for the other two species. Pole producers and users judge the WRC–1–65′ pole to be the largest WRC transmission pole with a clean record of acceptable performance. The calibration to current practice ($K_h$) was therefore derived to keep design values for this pole unchanged. This adjustment is assumed to be equally applicable to all poles as a means of adjusting from test performance to field conditions.

The ANSI O5.1 designated fiber stress values are assumed to represent the acceptable green pole strengths adjusted for drying in service, class size, load sharing, and pretreatment conditioning. The calibration adjustment should be derived as a ratio of two fiber stress values: one implicit in the current designated fiber stress and one derived from pole test data. Both these values should represent the green single pole strength value for a WRC–1–65′ pole. The designated fiber stress value of 6,000 lb/in$^2$ (41.4 MPa) includes an adjustment of 1.16 for drying in service and an adjustment of 1.1 for load sharing. Removing these values results in a fiber stress value of 4,700 lb/in$^2$ (32.4 MPa).

The pole test strength L5%EL for WRC is given in the form of equation parameters *A* and *B* in Table 2. The ground line circumference of a WRC–1–65′ pole is 54 in. (137 cm). Using the parameters in Table 2, the predicted value for MORGL5% of a WRC–1–65′ pole is

$$\text{MORGL5\%} = 3{,}0515 \times 54^{-0.59} = 2{,}900 \text{ lb/in}^2$$

Adjusting for the ANSI class size gives the adjusted (AMORGL) value:

$$\text{AMORGL5\%} = 1.1(\text{MORGL5\%}) = 3{,}190 \text{ lb/in}^2$$

This value is also derived without recognition for load sharing and drying adjustments. A calibration factor ($K_\text{h}$) derived as the ratio of the historic and test values is then

$$K_\text{h} = 4{,}700/3{,}190 = 1.47$$

The NESC provides for adjusting for load sharing in specific situations. This suggests that the load sharing effect inherent in the original derivation of designated fiber stress has been removed as part of the resistance factor (Φ in Eq. (10)). If the 0.65 resistance factor referenced in the NESC is assumed to be the product of 0.91 (1/1.1), which accounts for the inherent load sharing adjustment at the L5%EL and 0.72 for conservatism (resulting from lack of knowledge, safety, and unknown variability), then a new resistance factor, applied directly to the L5%EL for single poles with no load sharing adjustment, will be 0.72.

The adjustment (*K*, Eq. (13)) to account for class size, conditioning, and calibration of the WRC pole is then 1.62 (1.1 × 1.0 × 1.47). If Φ is 0.72 when the nominal resistance includes no adjustment for load sharing, the design value for the WRC–1–65′ pole (left side of Eq. (12)) is

$$\Phi K(\text{MORGL5\%}) = 0.72 \times 1.62 \times 2{,}900 = 3{,}380 \text{ lb/in}^2$$

This approach gives the same value derived following the current NESC without the 16% increase for drying below 20%. Should the designer consider the moisture adjustment appropriate, the design value will be 3,900 lb/in$^2$ (268 MPa), the same as the current design value. The numbers are the same—the only difference is that the designer has the option of designing for wet or dry conditions.

Table 3 provides a direct comparison of the design value based on the proposed nominal resistance derivation and that based on current ANSI O5.1 designated fiber stress. Parameters A and B from Table 2 have been adjusted for ANSI class size ($K_\text{s}$ = 1.1) and calibration to historic performance ($K_\text{h}$ = 1.47) to give an estimate of the L5%EL for new green ANSI-classified utility poles. These values divided by the ANSI designated fiber stress are adjusted to remove the effects of load sharing (0.91) and the drying adjustment (1/1.16) to provide the “design ratio” values. The design ratios represent the change to old values required to bring all pole nominal resistance values to a level of reliability considered acceptable for WRC–1–65′ transmission poles.

# Conclusions and Recommendations

This paper presents a systematic approach to the derivation of the nominal resistance of wood utility poles based on the LRFD format. The approach incorporates the state-of-the-knowledge on the performance of full-scale poles under both laboratory and field conditions. The proposed nominal resistance derivation, based on statistical probabilities, establishes standard procedures for the development of a pole resistance data base, can be easily updated, and is compatible with LRFD procedures being developed for the design of utility structures. This is part of an industry-wide effort to promote a uniform level of reliability of utility structures regardless of the construction material. Appendix B provides examples of how the proposed nominal stress would be applied to design of utility structures.

Specific recommendations are as follows:

1. Incorporate the more recent full-size test data on wood utility poles into the derivation of nominal resistance.
2. Convert the current designated fiber stress values to the lower 5% exclusion limit with a 50% confidence level.
3. Incorporate the effects of class size and method of pole conditioning into the derivation of nominal resistance.
4. For a unified approach, include the size effect in the derivation of nominal resistance for all pole sizes.
5. Transfer the in-service drying and load sharing adjustments from the material resistance side to the design side so that designers can justify their use on an individual design case basis.
6. To derive nominal resistance values for species for which no or insufficient full-size test data are available, use the small clear specimen ratios of the species to that of western redcedar.

**Table 3—Equation constants and design ratios for computation of adjusted nominal resistance for various species of poles[a]**

| Common name | Genus and species | 1992 fiber stress (lb/in$^2$) | 2000 nominal $A$[b] | 2000 nominal $B$ | WRC–1–65′ (design ratio) |
|---|---|---|---|---|---|
| | | Transmission poles | | | |
| Southern pine | | 8,000 | 26,450 | –0.325 | 1.21 |
| Loblolly | *Pinus taeda* | | | | |
| Longleaf | *Pinus palustris* | | | | |
| Shortleaf | *Pinus echinata* | | | | |
| Slash | *Pinus elliottii* | | | | |
| Douglas-fir, interior north | *Pseudotsuga menziesii* | 8,000 | 21,530 | –0.256 | 1.22 |
| Douglas-fir, coastal | *Pseudotsuga menziesii* | 8,000 | 21,530 | –0.256 | 1.22 |
| Western redcedar | *Thuja plicata* | 6,000 | 49,340 | –0.593 | 0.99 |
| | | Distribution poles | | | |
| Southern pine | | 8,000 | 8,480 | 0 | 1.35 |
| Loblolly | *Pinus taeda* | | | | |
| Longleaf | *Pinus palustris* | | | | |
| Shortleaf | *Pinus echinata* | | | | |
| Slash | *Pinus elliottii* | | | | |
| Douglas-fir, coastal | *Pseudotsuga menziesii* | 8,000 | 8,330 | 0 | 1.33 |
| Western larch | *Larix occidentalis* | 8,400 | 11,160 | 0 | 1.69 |
| Western redcedar | *Thuja plicata* | 6,000 | 5,580 | 0 | 1.18 |
| Alaska yellow cedar | *Chamaecyparis nootkatensis* | 7,400 | 7,680 | 0 | 1.32 |
| Jack pine | *Pinus banksiana* | 6,600 | 8310 | 0 | 1.60 |
| Lodgepole pine | *Pinus contorta* | 6,600 | 5,830 | 0 | 1.12 |
| Ponderosa pine | *Pinus ponderosa* | 6,000 | 6,110 | 0 | 1.30 |
| Red pine | *Pinus resinosa* | 6,600 | 6,580 | 0 | 1.27 |
| | | Species not covered by AWPA C4 standard for treated poles[c] | | | |
| Western fir (true fir) | | 6,600 | 6,440 | 0 | 1.24 |
| California red | *Abies magnifica* | | | | |
| Grand | *Abies grandis* | | | | |
| Noble | *Abies procera* | | | | |
| Pacific silver | *Abies amabilis* | | | | |
| White | *Abies concolor* | 6,600 | 5,650 | 0 | 1.09 |
| Redwood | *Sequoia sempervirens* | 6,600 | 7,100 | 0 | 1.37 |
| Sitka spruce | *Picea sitchensis* | 6,600 | 6,740 | 0 | 1.30 |
| White spruce | *Picea glauca* | 6,600 | 5,650 | 0 | 1.09 |
| Western hemlock | *Tsuga heterophylla* | 7,400 | 6,050 | 0 | 1.04 |
| Douglas-fir, interior north | *Pseudotsuga menziesi* | 8,000 | 8,330 | 0 | 1.33 |

[a] For species not represented in the ANSI data base, L5%EL values were derived on the basis of clear wood values. 1 lb/in$^2$ = 6.894 kPa.
[b] Values for factor A were adjusted for conditioning, ANSI size class (1.1), and historic precedent (1.47).
[c] Species are not covered by AWPA C4 because of non-use.

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7. Include strength degradation over time in the derivation of nominal resistance. This factor, which is currently housed in the NESC code, should be transferred to the resistance side and taken into consideration in the ANSI O5.1 standard.
8. To simplify the assignment of nominal resistance values, re-compute ANSI O5.1 pole class sizes to reflect the size effect on bending strength.
9. Derive nominal resistance values for other wood utility products such as glulam poles and cross arms using an LRFD format similar to that recommended for wood poles.

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# Appendix A—Derivation of RECIPE Program

The typical regression model and methodology assumes only one source of variation. However, if we assume the strength of pole *j* from source *i* follows a simple regression model with two components of variation, such as

$$Y = \theta_0 C^{\theta_1} \exp\{b_i + e_j\}$$

where $b_i$ is "between source" variation and is distributed as $N(0, \sigma_b^2)$ and $e_j$ is "within source" variation and is distributed as $N(0, \sigma_e^2)$, then special methods beyond the typical methods are necessary. For a fixed pole circumference, we can consider the distribution of Ln (MORGL) or Ln($Y$) as $N(X'\theta, \sigma_b^2 + \sigma_e^2)$.

The RECIPE program by Vangel (1994) calculates the ordinary least squares (OLS) estimates for the regression and decomposes the remaining error into between-source and within-source components. This method is ideally designed for a balanced situation where each source has an equal number of replicates, but Vangel provides additional programs to evaluate performance in unbalanced situations. As long as the within-source variability dominates, the intervals provided may be somewhat conservative (Vangel 1994).

The tolerance limit problem is to construct a $100\gamma\%$ lower confidence limit on the $100(1-\beta)$ percentile of Ln(MORGL). This means that there is a probability of $\gamma$ that the estimated exclusion is less than or equal to the true parent value given both between-source and within-source variation.

For the pole study, a 50% lower confidence limit ($\gamma = 0.50$) of the lower 5th percentile ($\beta = 0.95$) was calculated for each species and then exponentiated to provide a limit for MORGL.

# Appendix B—Examples of Application of Nominal Stress to Design

**Example 1:** Nominal resistance (design stress) of a class H2, 85-ft Boultonized Douglas-fir pole.

1. Determine the pole class size at 6 ft from the butt from table 8 in the current ANSI O5.1 standard.

   C6 = 61.5 in.
   Ground line = 10.5 ft
   Taper for Douglas-fir = 0.21 in/ft (Annex B)
   $C_{\text{gl}} = \text{C6} - 0.021(10.5 - 6) = 60.5$ in.

2. The regression parameters (*A* and *B*, Table 3) and $C_{\text{gl}}$, applied to Eq. (12), give the lower 5% exclusion value for an untreated utility pole:

   $$R_{\text{n-untreated}} = 20{,}775(60.5^{-0.256})$$
   $$= 7{,}268 \text{ lb/in}^2$$

3. Boultonizing reduces the pole strength 10%, so the nominal resistance for this pole becomes

   $$R_{\text{n}} = 6{,}541 \text{ lb/in}^2$$

   As this is the L5%EL with no adjustment for drying or load sharing, the NESC resistance factor ($\Phi$) should be 0.72, giving a design value of

   $$\Phi R_{\text{n}} = 4{,}710 \text{ lb/in}^2$$

   The current procedure, starting with a designated fiber stress of 8,000 lb/in$^2$ and adjusting by $\Phi$ of 0.65, gives

   $$\Phi R_{\text{n}}' = 0.65(8{,}000) = 5{,}200 \text{ lb/in}^2$$

   This value, however, includes a 16% adjustment for drying in service. This means the new design value will be 5% higher than the current one.

**Example 2:** Using a 50-ft steam-conditioned southern pine pole with a load equivalent to 3,300 lb applied in a horizontal orientation 2 ft from the top, what pole size should be used to provide satisfactory service?

1. The ground line for a 50-ft pole is 7 ft from the butt (ANSI O5.1, table 8), giving a moment arm of 41 ft.

2. If we assume that the 3,300-lb load is the value adjusted for the load factor $\lambda$ (Eq. (9)), then we know that $0.72R_{\text{n}}$ must exceed this value or $R_{\text{n}}$ must exceed 4,580 lb load applied at the top, which would give a ground-line moment of 187,780 ft lb.

3. Using the relationship

   $$M_{\text{gl}} = A\, C_{\text{gl}}^{B+3} / (32\pi^2)$$

   and solving for $C_{\text{gl}}$ gives

   $$C_{\text{gl}} = (M_{\text{gl}} 32\pi^2 / A)^{1/(B+3)}$$

4. The value of *A* for southern pine transmission poles (27,205, Table 3) does not include an adjustment for steam conditioning (0.85). Therefore, the value used in the expression for $C_{\text{gl}}$ should be 23,124 (0.85(27,025)).

   $$C_{\text{gl}} = [(187{,}780(12 \text{ in/ft})(32\pi^2))/23{,}124]^{0.374}$$

   $$C_{\text{gl}} = 47.62 \text{ in.}$$

5. The class H1 pole has a minimum circumference of 47.5 in. at 6 ft, giving a 47.25-in. minimum circumference at 7 ft. This is borderline and an H2 class should be selected.