AS/NZS 1664.1
Supplement 1:1997

Aluminium structures

Part 1: Limit state design—Commentary

(Supplement 1 to AS/NZS 1664.1:1997)

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Part 1: Limit state design—Commentary

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PREFACE

This Commentary was prepared by the Standards Australia/Standards New Zealand Committee BD/50, Aluminium Structures. It is intended to be read in conjunction with AS/NZS 1664.1, *Aluminium Structures*, Part 1: *Limit state design*, but it does not form an integral part of that Standard.

This Commentary is not intended to provide a general primer to probability-based limit state design (LSD) criteria. This is provided in Reference 2 and in the further references which are cross-referenced numerically in the text and listed at the end of this document.

The objective of this Commentary is to give an explanation of the reasons for the recommended capacity factors in AS/NZS 1664.1 and to provide background material to the requirements of that Standard.

The clause numbers and titles used in this Commentary are the same as those in AS/NZS 1664.1 except that they are prefixed by the letter C.

Gaps in the numerical sequence of this Commentary's clause numbering means that no explanation of or background to the missing clause(s) is necessary.

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STANDARDS AUSTRALIA/STANDARDS NEW ZEALAND

Australian/New Zealand Standard

Aluminium structures

Part 1: Limit state design—Commentary

(Supplement 1 to AS/NZS 1664.1: 1997)

SECTION C1 GENERAL

C1.4 REFERENCED DOCUMENTS The Standards listed in Clause 1.4 are subject to revision from time to time. A check should be made with Standards Australia or Standards New Zealand, as appropriate, as to the currency of any document referenced in the text.

SECTION C3 GENERAL DESIGN RULES

INTRODUCTION The general procedure of applying the limit state design (LSD) method for aluminium building structures consists of the following steps:

(a) Determine the stress due to the factored loads, f, by conventional elastic structural analysis. The factored loads are the specified dead, live, wind, rain, snow or earthquake loads multiplied by the load factors given in Clause 2.4.

(b) Compute the factored limit state stress ϕF_L from Clause 3.4 and verify that—

$$\phi F_L \geq f$$

Clause 3.4 gives the capacity factor ϕ and the limit state stress F_L for a variety of commonly encountered aluminium structural members and elements. The limit state stress F_L is dependent on the material properties and the member geometry. It reflects the ultimate load-carrying capacity of the member or element, be that yield, fracture, plastification, buckling or crippling. The limit state stresses in these LSD criteria are identical to those given in AS/NZS 1664.2 by setting the factors of safety equal to unity in the various formulas given in Clause 3.4 of AS/NZS 1664.2.

The capacity factor ϕ accounts for the uncertainties of determining the limit state stress. It is computed by the method of first-order second-moment probabilistic analysis presented in Reference 2 for a target reliability index of $\beta_T = 2.5$ for the yield limit state and $\beta_T = 3.0$ for the fracture limit state. Below is a detailed account presenting the background for each of the capacity factors used in Clause 3.4 of the LSD criteria.

Prior to this detailed account it will be instructive to discuss in a simple manner the basic concepts of probabilistic design. Failure is defined when the resistance, as characterized by a limit state, is less than or equal to the load effect on the structural element. The load effect in these LSD criteria for aluminium structures is characterized by the stress computed by elastic analysis from the forces acting on the structure. Both the resistance R and the load effect Q are random quantities (see Figure C1).

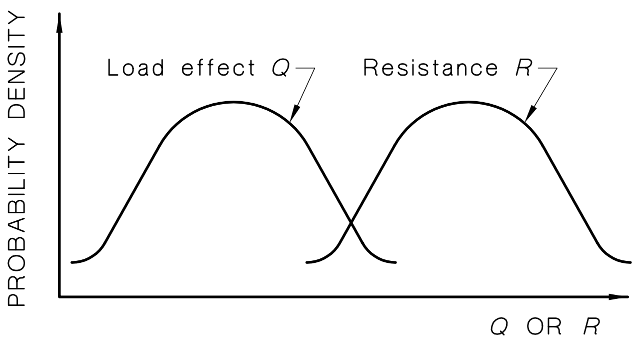


FIGURE C1 SCHEMATIC REPRESENTATION OF PROBABILITIES OF THE LOAD EFFECT AND THE RESISTANCE

Limit states are either ultimate or serviceability limit states. These LSD criteria pertain to the ultimate limit states of yield, fracture, plastification, buckling and crippling, although the serviceability limit states of deflection and the appearance of buckling are also features (see Section 4).

Failure is then not necessarily the total collapse of the member, but the reaching of a practically defined ultimate limit state. It occurs when $R < Q$. Alternatively, failure also is defined as in $ln(R/Q) \leq 0$, as shown in Figure C2. The probability of exceeding a limit state is the shaded area. According to current practice, it is not necessary to define a desired probability of failure, but a 'reliability index' β is determined such that the 'target reliability index' β_T for a new code is approximately equal to the value of β inherent in the traditional specification for standard design situations (Ref. 2). This process of selecting a target reliability index is called 'code calibration'. It will be illustrated for the simple case of tension members.

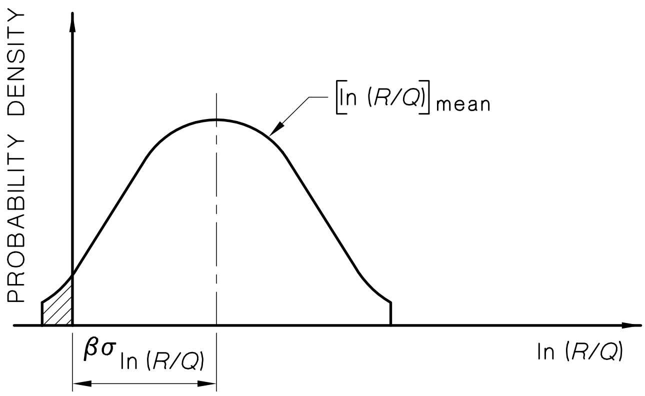


FIGURE C2 DEFINITION OF THE RELIABILITY INDEX β

According to first-order statistical derivations, the value of β from Figure C2 is expressed by the following formula:

$$\beta = \frac{\ln(\bar{R}/\bar{Q})}{\sqrt{V_R^2 + V_Q^2}} \qquad \text{. . . C3(1)}$$

In this equation $\bar{R}$ and $\bar{Q}$ are the mean values of the resistance R and the load effects Q, respectively, and V_R and V_Q are the corresponding coefficients of variation.

The resistance of a tension member for the limit state of yielding is—

$$R = A F_{ty} \qquad \text{. . . C3(2)}$$

and thus

$$\bar{R} = \bar{A} \bar{F}_{ty} \qquad \text{. . . C3(3)}$$

and

$$V_R = \sqrt{V_A^2 + V_{F_{ty}}^2} \qquad \text{. . . C3(4)}$$

The available data on dimensions and yield stress of aluminium structures were evaluated in Reference 3, and the following conservative estimates of the statistical properties were suggested:

$$\bar{F}_{ty} = 1.10\ F_{tyn}, \quad V_{F_{ty}} = 0.06, \quad \bar{A} = A_n, \quad V_A = 0.05$$

where

F_{tyn} is the minimum specified yield stress and A_n is the handbook area. These are the 'nominal' values the designer uses. With these values—

$$\bar{R} = 1.10\,R_n \quad \text{and} \quad V_R = \sqrt{0.05^2 + 0.06^2} = 0.08$$

R_n is the 'nominal' resistance $R_n - A_n\,F_{tyn}$.

The load effect Q is the tensile force in the member due to the applied loads. For purposes of illustration only dead and live load will be used, i.e.

$$Q = D + L \qquad \text{. . . C3(5)}$$

$$\bar{Q} = \bar{D} + \bar{L} \qquad \text{. . . C3(6)}$$

$$V_Q = \frac{\sqrt{(\bar{D}\,V_D)^2 + (\bar{L}\,V_L)^2}}{\bar{D} + \bar{L}} \qquad \text{. . . C3(7)}$$

The following statistical data about load are taken from Reference 2:

$$\bar{D} = 1.05\ D_n,\quad \bar{L} = L_n,\quad V_D = 0.1,\quad V_L = 0.25$$

where

D_n and L_n are the 'nominal' specified loads. Rearrangement of Equations C3(6) and (7) leads to the following equations:

$$\bar{Q} = L_n(1.05\,D/L + 1) \qquad \text{. . . C3(8)}$$

$$V_Q = \frac{\sqrt{(1.05 \times 0.1 \times D/L)^2 + 0.25^2}}{1.05\,D/L + 1} \qquad \text{. . . . C3(9)}$$

where

D/L is the nominal dead-to-live load ratio.

The process of calibrating to the ASD specification is performed as follows:

$$A_n\,F_{tyn}/FS = D_n + L_n \qquad \text{. . . C3(10)}$$

or

$$R_n = FS(D_n + L_n) = FS(L_n)\,(D/L + 1) \qquad \text{. . . C3(11)}$$

FS is the specified factor of safety, which is equal to 1.65 given in Reference 1 for the limit state of yield.

Substitution of FS = 1.65 into Equation 11, and use of Equation C3(11) in the relationship of $\bar{R}/\bar{Q}$ gives—

$$\frac{\bar{R}}{\bar{Q}} = \frac{1.0 \times 1.65(D/L + 1)}{1.05\ D/L + 1} \qquad \text{. . . C3(12)}$$

$\bar{R}/\bar{Q}$ and V_Q (Equation C3(9)), and thus also β (Equation C3(1)), depend on the dead-to-live load ratio. Aluminium structures usually have a low dead-to-live load ratio. Values of β determined from Equation C3(1) for the limit state of yield (FS = 1.65) and the limit state of fracture (FS = 1.95) are as follows; for this latter case $\bar{R}$ = 1.10 R_n and V_R = 0.08, as for the limit state of yield (Ref. 3):

D/L	**β**	
	Yield	**Fracture**
0.2	2.6	3.4
0.1	2.5	3.2

A similar exercise can also be performed for the proposed LSD method. According to this approach—

$$\phi\, A_n F_{tyn} = \gamma_D D_n + \gamma_L L_n \qquad \text{. . . C3(13)}$$

Again, using $R_n = A_n F_{tyn}$, and γ_D = 1.2 and γ_L = 1.6 as recommended in Reference 2:

$$R_n = \frac{L_n}{\phi}(1.2\, D/L + 1.6) \qquad \text{. . . C3(14)}$$

from which

$$\bar{R}/\bar{Q} = \frac{1.10}{\phi}\left[\frac{1.2\, D/L + 1.6}{1.05\, D/L + 1}\right] \qquad \text{. . . C3(15)}$$

The calculations show the following results:

ϕ	***D/L***	β	
0.95	0.2	2.5	limit state yield
0.95	0.1	2.5	
0.85	0.2	3.1	limit state fracture
0.85	0.1	2.9	

The values of ϕ were rounded off to the nearest 0.05, and comparison of β indicates that for typical dead-to-live load ratios of aluminium structures (i.e. D/L of 0.2 to 0.1) the values of β are near the target of 2.5 for the limit state of yield, and the target of β_T = 3.0 for the fracture limit state. This difference reflects the greater reliability demanded for the more serious type of limit state, as already recognized in Reference 1 with its two kinds of safety factors, i.e. 1.65 and 1.96. These target reliability indices are similar to those used by the AISI for cold-formed steel.

Based on the results presented above ϕ = 0.95 is recommended for the limit state of yield, and ϕ = 0.85 for the limit state of fracture. Methods are available to easily check the consequences of changing ϕ as regards reliability. The economic consequences can also be ascertained by comparing designs required by the ASD and the LSD method, as follows:

$$(R_n)_{ASD} = L_n(D/L + 1)\,(FS) \qquad \text{. . . C3(16)}$$

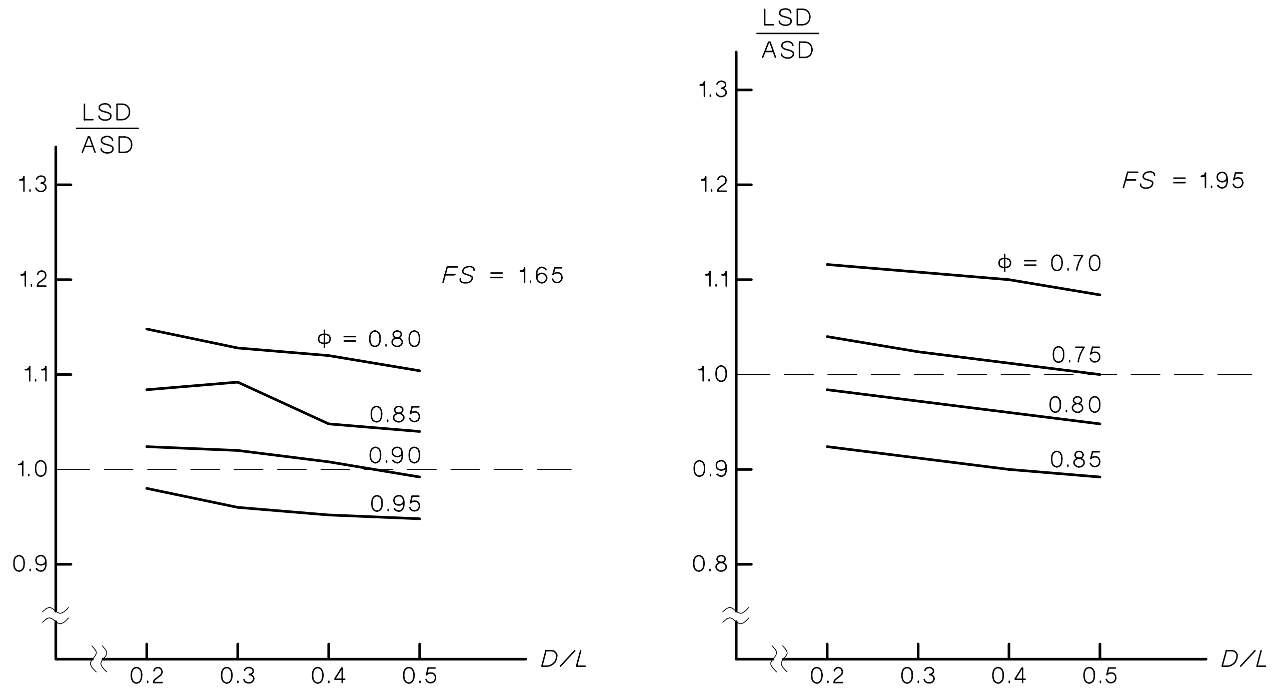
$$(R_n)_{LSD} = L_n(1.2\, D/L + 1.6)\,(1/\phi) \qquad \text{. . . C3(17)}$$

where

$(R_n)_{ASD}$ is the nominal design requirement according to AS/NZS 1664.2, and $(R_n)_{LSD}$ is the requirement of the LSD criteria. The ratio LSD/ASD is then—

$$\frac{1.2\,D/L + 1.6}{\phi(FS)\,(D/L + 1)} \qquad \text{. . . C3(18)}$$

The curves in Figure C3 show the variation of this ratio for various values of ϕ and for FS = 1.65 and 1.95 for the range D/L = 0.2 to 0.5. It can be seen that the ratio decreases with an increase of the dead-to-live load ratio.



(a) FS = 1.65 (b) FS = 1.95

FIGURE C3 THE EFFECTS OF CHANGING THE CAPACITY FACTOR ϕ ON THE REQUIRED AREA FOR TENSION MEMBERS

This Commentary gives the basic data used to arrive at the recommended ϕ-factors in Clause 3.4.

C3.4 FACTORED LIMIT STATE STRESSES

C3.4.2 Tension, axial, net section The selection of $\phi_y = 0.95$ and $\phi_u = 0.85$ is discussed in the Introduction to this Section C3.

C3.4.3 to C3.4.5 Tension in extreme fibres of beams Two limit states apply to the tension flange: limit state of yield when the strain is that corresponding to the yield stress F_{ty}, and limit state of fracture. The resistance is the bending moment M, and its mean value and coefficient of variation is, for the yield limit state—

$$\bar{R} = \bar{S}_{xt}\,\bar{g}\,\bar{F}_{ty} \qquad \text{. . . C3(19)}$$

and

$$V_{\mathrm{R}} = \sqrt{V_{S_{xt}}^2 + V_g^2 + V_{F_{ty}}^2} \qquad \text{. . . C3(20)}$$

where

S_{xt} is the elastic section modulus on the tension side, g is the 'shape factor', and F_{ty} is the tensile yield stress. The same expressions hold for the limit state of fracture, with the exception that F_{ty} is replaced by F_{tu}. The shape factor accounts for partial plastification due to the non-linear nature of the stress-strain curves the nominal resistance is—

$$R_{\mathrm{N}} = S_{xtn} g_{n} F_{tyn} \qquad \text{. . . C3(21)}$$

and so

$$\bar{R} = R_{n}\left(\frac{\bar{S}_{xt}}{S_{xtn}}\right)\left(\frac{\bar{g}}{g_{n}}\right)\left(\frac{\bar{F}_{ty}}{F_{tyn}}\right) \qquad \text{. . . C3(22)}$$

Reference 3, as noted before for the tension member, gives the following values:

$$\bar{S}_{xt} = S_{xt}, \quad V_{S_{xt}} = 0.05, \quad \bar{F}_{ty} = 1.10\,F_{tyn}, \quad V_{F_{ty}} = 0.06$$

It will be assumed that g_n equals the shape factors in AS/NZS 1664.2, and $\bar{g}$ equals the values given in Reference 4, which were also corroborated for some sections and alloys in Reference 5. It will be assumed that $V_g = 0.0$. From these data $\bar{R}$ and V_{R} can be determined as—

$$\bar{R} = R_{n}(1.1\bar{g}/g_{n}) \text{ and } V_{\mathrm{R}} = \sqrt{0.05^2 + 0.06^2} = 0.08$$

The results of the analysis for the recommended ϕ-factors are given in Table C3.4.3. The values of β are near the target values.

TABLE C3.4.3

DATA FOR TENSION IN EXTREME FIBRES OF BEAMS

Cross-section and flexure plane	Clause No. in LSD criteria	Limit state	g_a	$\bar{g}$ (Ref. 5)	$\bar{R}/R_n$	Φ	β (D/L = 0.2)
I and C shapes, major axis flexure	3.4.3	Yield	1.0	1.07	1.18	0.95	2.9
		Fracture	1.0	1.16	1.28	0.85	3.7
I shapes, major axis flexure	3.4.5	Yield	1.30	1.30	1.10	0.95	2.5
		Fracture	1.42	1.50	1.16	0.85	3.3
Box shapes	3.4.3	Yield	1.0	1.10	1.21	0.95	3.0
		Fracture	1.0	1.22	1.34	0.90	3.7
Circular tubes	3.4.4	Yield	1.17	1.17	1.10	0.95	2.5
		Fracture	1.24	1.35	1.20	0.85	3.4
Solid rectangular bars	3.4.5	Yield	1.30	1.30	1.10	0.95	2.5
		Fracture	1.42	1.50	1.16	0.85	3.3

C3.4.6 and C3.4.7 Bearing In the absence any statistically significant data on bearing capacities, it was decided to use $\phi_y = 0.95$ and $\phi_u = 0.85$, giving essentially the same requirements as in Reference 1. Table 5.1.2.4 was prepared on this basis.

C3.4.8 Compression in columns, axial, gross section

C3.4.8.1 *General* The nominal column strength equations of AS/NZS 1664.2 were retained, i.e.

$$F_L = B_c - D_c kL/r \leq F_{cy}/k_c \qquad \text{. . . C3(23)}$$

for $kL/r \leq S_2 = C_c$, and

$$F_L = \frac{\pi^2 E}{(kL/r)^2} \qquad \text{. . . C3(24)}$$

for $kL/r \geq C_c$

It was found convenient in the background research to introduce a non-dimensional slenderness ratio—

$$\lambda = \frac{kL}{r}\left(\frac{l}{\pi}\right)\sqrt{F_{cy}/E} \qquad \text{. . . C3(25)}$$

The equations actually given in Clause 3.4.8 are in terms of λ rather than the effective slenderness ratio. The definitions of B_c, D_c, S_2 and C_c remain the same as in AS/NZS 1664.2. The relationship between the nominal limit state stress F_L and the factored limit state stress ϕF_L, and the slenderness parameter λ, is shown in Figure C4 for one particular alloy.

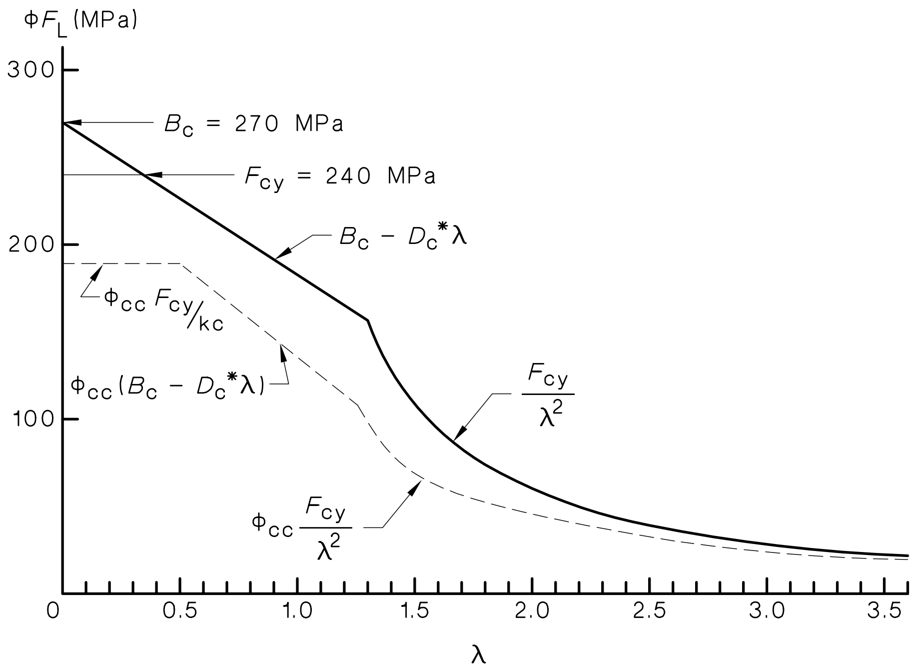


FIGURE C4 COLUMN CURVE FOR 6061-T6 ALLOY

The capacity factor ϕ_{cc} varies with the slenderness parameter. The particular equation for ϕ_{cc} given in Clause 3.4.8 is similar to, but not identical to, the capacity factors recommended in References 3 and 5, where considerable work was done in the development of LSD provisions for columns, and therefore a detailed accounting is presented on the way ϕ_{cc} was selected.

The mean resistance of an ideally pinned-end but initially crooked column was shown to be equal to (Refs 3 and 5):

$$\bar{R} = \bar{A}\,\bar{\sigma}_{TM}\,\bar{B}_T\,\bar{B}_u \qquad \text{. . . C3(26)}$$

The coefficient of variation is then—

$$V_R = \sqrt{V_A^2 + V_{\sigma_{TM}}^2 + V_{B_T}^2 + V_{B_u}^2} \qquad \text{. . . C3(27)}$$

The terms in Equation C3(26) are defined as follows:

$\bar{A}$ = mean cross-sectional area of column

In accordance with previous usage, $\bar{A} = A_n$ and $V_A = 0.05$, where A_n is the nominal area.

$\bar{\sigma}_{TM}$ = mean buckling stress of an ideally straight column as determined by the tangent modulus theory, i.e.

$$\sigma_{TM} = \frac{\pi^2 E_t}{(kL/r)^2} \qquad \text{. . . C3(28)}$$

In the derivation of References 3 and 5 a Ramberg-Osgood type stress-strain curve was assumed, and thus the tangent modulus E_t, is equal to—

$$E_t = \frac{E}{1 + 0.002\,n\left(\frac{E}{\sigma_{0.2}}\right)\left(\frac{\sigma}{\sigma_{0.2}}\right)^{n-1}} \qquad \text{. . . C3(29)}$$

In this Equation E is the elastic modulus, σ is the average stress under this buckling load, $\sigma_{0.2}$ is the compressive stress when the strain is equal to 0.2 percent, and n is the strain-hardening parameter. The coefficient of variation of σ_{TM}, $V_{\sigma_{TM}}$, was shown to be 0.06 in Reference 5.

$\bar{B}_T$ = mean value of the ratio of test results of straight columns to the tangent modulus load. Analysis of the available test results in Reference 3 resulted in the following statistics:

$$\bar{B}_T = 1.0 \text{ and } V_{B_T} = 0.05$$

This means that the tangent modulus theory is indeed a very good predictor for straight columns.

$\bar{B}_u$ = mean value of the ratio of the ultimate strength of an initially crooked pinned-end column to the strength predicted by the tangent modulus theory for straight columns. It was assumed that the initial crookedness of the column is a sine-wave with a maximum amplitude of one-thousandths of the length. This is in accordance with the procedure recommended in Chapter 3, Reference 6.

The following formulas were derived in Reference 5 for the ratio B_u:

$$\left.\begin{aligned}
\bar{B}_u &= 1.0 \text{ for } \lambda \le 0.263 \\
\bar{B}_u &= 1.05 - 0.19\,\lambda \text{ for } 0.263 \le \lambda \le 1.20 \\
\bar{B}_u &= 0.63 + 0.16 \le \text{ for } 1.20 \le \lambda \le 2.0 \\
\bar{B}_u &= 0.95 \text{ for } \lambda \ge 2.0 \\
V_{B_u} &= 0.10
\end{aligned}\right\} \qquad \text{. . . C3(30)}$$

A calibration study similar to that presented previously for tension members was performed, using Equation 1 to determine β, and employing Equations C3(23) and (24) as the nominal column strength. Four different kinds of alloys were investigated (Table C3.4.8). A number of types of relationship for ϕ were tried, and the expressions shown in Table C3.4.8 were finally selected as being reasonably accurate and yet still fairly simple.

TABLE C3.4.8

DATA USED IN COLUMN CALIBRATION STUDIES

Ref.	Material	Heat treatment	n	$\sigma_{0.2}$ MPa	E MPa	F_{cy} MPa	V_R‡
7	European	No	8	157	70 190	143*	0.14
8	—	Yes	18.55	277	69 640	252*	0.14
7	European	Yes	28.60	303	74 400	276*	0.14
9	6061-T6	Yes	15.5	281	69 640	241†	0.14

* $F_{cy} = \sigma_{0.2}/1.1$, assuming $\sigma_{0.2}$ to be the mean yield stress.

† Specified value.

‡ $V_R = \sqrt{0.05^2 + 0.06^2 + 0.05^2 + 0.10^2} = \sqrt{V_A^2 + V_{\sigma_{TM}}^2 + V_{B_T}^2 + V_{B_u}^2}$

$$\left.\begin{aligned} \phi_{cc} &= 1 - 0.21\,\lambda \le 0.95 \text{ for } \lambda \le 1.2 \\ \phi_{cc} &= 0.58 + 0.14\,\lambda < 0.95 \text{ for } \lambda > 1.2 \end{aligned}\right\} \quad \ldots \text{C3(31)}$$

The capacity factor thus varies linearly as the slenderness parameter λ. The β values resulting from the use of ϕ_{cc} (Equation C3(31)) is the LSD design criteria are shown as the solid curve in Figure C5. The target value of $\beta_T = 2.5$ is fairly closely approximated.

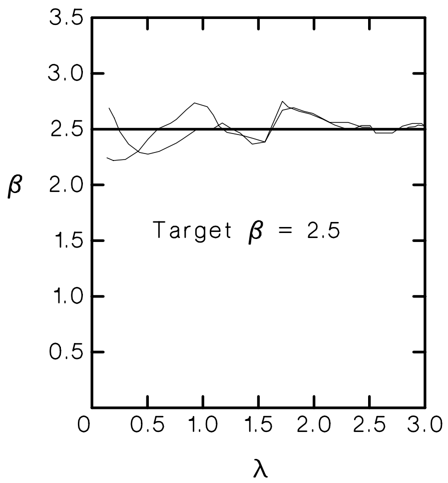


FIGURE C5 RELIABILITY WITH LINEARLY VARYING ϕ_{cc}

In Reference 5 considerable work was done on one additional aspect of column design. Real pinned-end columns rarely exist in practice. Even nominally pinned columns have some end restraint, and most columns are actually restrained by the connection to the base or to members framing into their ends. Furthermore, intentionally axially loaded members are also rare, most compression members being actually beam-columns subjected to both compression and bending. It was shown that each of these effects has a conservative influence and thus tends to increase β. A number of additional cases were studied, showing the same general trend of a somewhat increased value of β due to restraint.

C3.4.9 to C3.4.25 The statistical basis for selecting the ϕ values in Clauses 3.4.9 to 3.4.25 is presented in Reference 3. The same values of ϕ_y were recommended as for tension of the corresponding member types of Clauses 3.4.3 to 3.4.5, thus equating the reliability of short compressed members and elements to that underlying tension elements. The relevant data for choosing the ϕ values, which apply to buckling or crippling type limit states, are summarized in Tables C3.4.9.1, C3.4.9.2, C3.4.9.3, and C3.4.9.4.

For certain alloys and specifications sections, a negative S_1 slenderness limit may result from the equations given in Table 3.4(C). In such cases S_1 should be taken as 0.

TABLE C3.4.9.1

SUMMARY OF STATISTICAL DATA

Section in Reference 1	Limit state	FS	P_m	M_m	F_m	$\frac{R_m}{R_n}$	V_p	V_M	V_F	V_R	Category
3.4.1, 2, 3, 4	Y	n_y	1.0	1.10	1.0	1.10	0	0.06	0.05	0.08	A
	U	$k_t\, n_u$	1.0	1.10	1.0	1.10	0	0.06	0.05	0.08	B
3.4.8, 9	Y	$k_c\, n_y$	1.0	1.10	1.0	1.10	0	0.06	0.05	0.08	C
	B	n_u	1.0	1.0	1.0	1.0	0.05	0.06	0.05	0.09	D
3.4.10	Y	$k_c\, n_y$	1.0	1.10	1.0	1.10	0	0.06	0.05	0.08	C
	IB	n_u	1.0	1.0	1.0	1.0	0.05	0.06	0.05	0.09	D
	EB	n_u	1.24	1.0	1.0	1.24	0.27	0.06	0.05	0.28	E
3.4.11, 13, 14	Y	n_y	1.0	1.10	1.0	1.10	0	0.06	0.05	0.08	A
	B	n_y	1.03	1.0	1.0	1.03	0.11	0.06	0.05	0.13	F
3.4.12, 16.1	Y	n_y	1.0	1.10	1.0	1.10	0	0.06	0.05	0.08	A
	IB	n_y	1.01	1.0	1.0	1.01	0.05	0.06	0.05	0.09	G
	EB	n_y	1.24	1.0	1.0	1.24	0.27	0.06	0.05	0.28	H
3.4.15, 16, 17	Y	n_y	1.0	1.10	1.0	1.10	0	0.06	0.05	0.08	A
	B	n_y	1.0	1.0	1.0	1.0	0.05	0.06	0.05	0.09	I
3.4.20	Y	n_y	1.0	1.10	1.0	1.10	0	0.06	0.05	0.08	A
	IB	n_y	1.07	1.0	1.0	1.07	0.09	0.06	0.05	0.12	J
	EB	n_y	0.93	1.0	1.0	0.93	0.09	0.06	0.05	0.12	K

TABLE C3.4.9.2
LIMIT STATE CATEGORIES

Category	*FS*	$\bar{R}/R_n$	V_R	Description
A	1.65	1.10	0.08	yield in tension
B	1.95	1.10	0.08	fracture in tension
C	1.82	1.10	0.08	yield in compression
D	1.95	1.00	0.09	buckling of column components inelastic column buckling
E	1.95	1.24	0.28	elastic column buckling
F	1.65	1.03	0.13	beam buckling, overall
G	1.65	1.01	0.09	inelastic local buckling
H	1.65	1.24	0.28	elastic local buckling
I	1.65	1.00	0.09	local buckling of beams
J	1.65	1.07	0.12	inelastic shear buckling
K	1.65	0.93	0.12	elastic shear buckling

TABLE C3.4.9.3
RELIABILITY INDICES FOR ASD SPECIFICATION

Category	β	
	for D/L = 0.1	for D/L = 0.2
A	2.46	2.64
B	3.16	3.40
C	2.87	3.09
D	2.72	2.92
E	2.44	2.51
F	2.01	2.13
G	2.08	2.22
H	1.98	2.03
I	2.04	2.18
J	2.20	2.34
K	1.65	1.75

TABLE C3.4.9.4

CAPACITY FACTORS FOR LSD SPECIFICATION

Category	Target β	Φ			Target β	Φ_{II} *
		for D/L = 0.1	for D/L = 0.2	Rounded off†		for D/L = 0.2
	For primary structural elements				For secondary elements	
A	2.5	0.94	0.96	0.95	2.0	1.07
B	3.0	0.83	0.86	0.85	2.5	0.96
C	2.5	0.94	0.96	0.95	2.0	1.07
D	2.5	0.85	0.86	0.85	2.0	0.97
E	2.5	0.78	0.79	0.80	2.0	0.94
F	2.5	0.83	0.85	0.85	2.0	0.96
G	2.5	0.85	0.87	0.85	2.0	0.98
H	2.5	0.78	0.79	0.80	2.0	0.94
I	2.5	0.85	0.86	0.85	2.0	0.97
J	2.5	0.88	0.89	0.90	2.0	1.01
K	2.5	0.76	0.78	0.80	2.0	0.88

* $\frac{\Phi_{II}}{\Phi} = 1.1$ where $\psi\Phi \leq 1.0$ for secondary elements.

† recommended for use in LSD Spec.

SECTION C5 MECHANICAL CONNECTIONS

C5.1 BOLTED AND RIVETED CONNECTIONS

C5.1.1 General For mechanical connections of pre-painted aluminium structures see Paragraph C6.6.1.

C5.1.2 Factored limit state loads The ϕ-factors used for bearing stresses in Table 5.1.2.4 were adopted from Clause 3.4.6. The value of $\phi = 0.65$ for shear stress on rivets and bolts was determined by the following derivation. It was assumed that the 'typical' shear strength values for rivets given in Reference 10 represent mean values. The ratio of the mean to the 'minimum expected' values was found to be 1:15. A coefficient of variation of 0.1 was assumed. It was also assumed that the nominal rivet area is equal to the mean, with a coefficient of variation of 0.1. The mean shear capacity of a rivet is thus—

$$\bar{R} = \bar{A}\bar{F}_{su} = 1.0 \times 1.5\, A_n F_{sun} \qquad \text{. . . C5(1)}$$

and

$$V_R = \sqrt{V_A^2 + V_{F_{su}}^2} = \sqrt{0.1^2 + 0.1^2} = 0.14 \qquad \text{. . . C5(2)}$$

With these statistics a calibration was performed using Equation 1, and for a $D/L = 0.2$ it was found that ASD design gave $\beta = 3.9$. The LSD design with $\phi = 0.65$ gave $\beta = 4.0$.

SECTION C6 FABRICATION

C6.6 PAINTING

C6.6.1 General Mechanical connections of pre-painted aluminium structural members may be affected as follows:

(a) *Connecting by bolts* The total film build between the two painted aluminium members may affect the torque retention of the connecting bolts. The retention torque loss may lead to loose connection and subsequently premature fatigue failure of bolts and aluminium members in the localized join area.

In addition, the corrosion resistance of the localized bolting area may be reduced.

(b) *Connecting by rivets* The surface roughness and film build of the pre-painted aluminium underneath the rivet head may deteriorate due to long-term fretting, in particular when connecting aluminium members are undergoing either intermittent or constant service loading.

The overall result is that loose connection may occur and lead to premature fatigue failure of both rivet and aluminium members. In addition, the corrosion resistance in the localized riveted area may be reduced.

SECTION C7 WELDED CONSTRUCTION

C7.2 FILLER WIRE The factored limit state shear stress for fillet welds (Table 7.2) is based on a value of ϕ = 0.65. The reasoning used in arriving at this value is that the mean shear strength of a fillet weld is equal to—

$$\bar{R} = \bar{\tau}_u \bar{A} \qquad \text{. . . C7(1)}$$

where $\bar{\tau}_u$ is the mean shear strength and $\bar{A}$ is the weld throat area. The following data were obtained from Reference 11:

Filler alloy	$\bar{\tau}_{u/Fw}$	$V_{\tau u}$	Orientation of weld
1100	1.62	0.18	longitudinal
1100	1.78	0.23	transverse
4043	1.45	0.17	longitudinal

For the ratio of fillet weld areas required by LSD to that required by the allowable stress specification, see Table C7.

TABLE C7
RATIO OF FILLET WELD AREAS REQUIRED BY LSD TO THAT REQUIRED BY THE ALLOWABLE STRESS SPECIFICATION

Filler alloy	F_a Table 7.2 ASD MPa	F_w MPa	LSD/ASD for ϕ = 0.65 for		
			D/L = 0.1	D/L = 0.25	D/L = 0.5
1100	22	52	1.03	1.00	0.96
4043	34	79	1.05	1.02	0.98
5183	55	128	1.04	1.01	0.98
5356	48	117	0.99	0.96	0.93
5556	59	138	1.02	0.99	0.96
5654	34	83	1.00	0.97	0.94

SECTION C8 TESTING

C8.3 TEST REQUIREMENTS A load/deformation curve will serve not only as a check against observational errors, but also to indicate any irregularities in the behaviour of the structure under load. It is desirable that a minimum of 6 points, excluding the zero load point, be obtained to define the shape of the load/deformation curve if the curve is predominantly linear, and a minimum of 10 points if the curve is significantly non-linear.

C8.5 PROOF TESTING For a proof load test on a structure to be successful, it is necessary that the structure does not reach its ultimate limit state during the test and also it does not incur serious permanent structural damage. Suitable methods for detecting the onset of damage vary from one material to another, and include such techniques as the measurement of crack widths and acoustic emissions. One commonly used method is the measurement of recovery of the deformation on unloading the structure. A recovery value of 85 percent is recommended by Reference 12.

For the serviceability limit state, it is suggested that a 95 percent recovery of deformation after removal of the test load will ensure that the specimen was substantially elastic at the test load.

C8.6.2 Test load It should be noted that the variability factor is a function of the number of specimens to be tested, and the estimated coefficient of variation of the structural characteristics of the individual specimens. The values given are based on the assumption that the coefficient of variation of the capacities of aluminium structures and elements is about 10 percent, while the coefficient of variation of the deformation characteristics is about 5 percent.

Table C8.6.2 gives some guidance for the choice of a variability factor if an estimated variation is significantly different from one assumed in Table 8.6.1.

TABLE C8.6.2

VALUES OF VARIABILITY FACTOR FOR TEST LOADS FOR ESTIMATED COEFFICIENT OF VARIATION

Number of specimens	Coefficient of variation of structural characteristics					
	5%	10%	15%	20%	25%	30%
1	1.2	1.46	1.79	2.21	2.75	3.45
2	1.17	1.38	1.64	1.96	2.36	2.86
3	1.15	1.33	1.56	1.83	2.16	2.56
4	1.14	1.30	1.50	1.74	2.03	2.37
5	1.13	1.28	1.46	1.67	1.93	2.23
10	1.10	1.21	1.34	1.49	1.06	1.85

C8.6.3 Criteria for acceptance Further testing of additional specimens may show that the population is acceptable because the variability factor reduces as the sample size increases.

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