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AN EXPLICIT APPROACH TO DESIGN OF STAINLESS STEEL COLUMNS

by

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Abstract: The paper describes a design procedure for stainless steel columns failing by flexural buckling. In the approach, the stress strain-curve is assumed to be expressed as a Ramberg-Osgood curve, defined in terms of the initial modulus (E_0), the 0.2% proof stress ($\sigma_{0.2}$) and the parameter n. It is shown that the column curve can be described in terms of the Ramberg-Osgood parameters by adopting a Perry-curve as basic strength curve and expressing the imperfection parameter in terms of E_0, $\sigma_{0.2}$ and n.

By using a Perry-curve, the design procedure is explicit. This contrasts the iterative approach described in the ASCE-LRFD Specification for the Design of Cold-formed Stainless Steel Structural Members. The proposed strength curves are compared with tests of stainless steel columns. It is shown that the coefficient of variation of the ratio of test strength to design strength is lower using the proposed design approach than using the ASCE Specification. Thus, the proposed approach is more accurate than that described in the ASCE Specification. Being explicit it is also more efficient.

Using the comparison with test results, the resistance factor to be used with the proposed design procedure is derived.

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Introduction

Stainless steel alloys are characterised by having a nonlinear (or round-house type) stress-strain curve with low proportionality stress and an extensive strain-hardening range. In the absence of a yield plateau, an equivalent yield stress is defined for design purpose, usually chosen as the 0.2% proof stress, (or off-set stress). In contrast to ordinary carbon steel, a large number of stainless steel alloys are available with vastly different chemical compositions and consequently different mechanical properties. It should therefore be recognised that in designing stainless steel structural members, one is dealing with a range of different materials. This contrasts carbon steel alloys which for all compression member design purposes can be modelled as bi-linear materials, only having different yield plateaus depending on the steel grade. A further complication is the pronounced susceptibility of stainless steel to strain hardening which has a strong influence on the change of mechanical properties caused by cold-forming. For instance, research on stainless steel tubes (Rasmussen & Hancock, 1993) has shown that the 0.2% proof stress may be more than doubled by the cold-forming process. Such increases are unprecedented in carbon steel tubes, for which the cold-forming process enhances the strength by 10-30%.

The buckling strength of columns depends on the flexural rigidity and hence the stiffness of the material. While carbon steel maintains its initial stiffness until yielding, the stiffness of stainless steel decreases gradually as the stress level increases. Consequently, the ASCE Load and Resistance Factor Design Specification for the Design of Cold-formed Stainless Steel Structural Members (ASCE, 1990) uses a tangent modulus approach for the flexural design of columns. A different approach is specified in the draft Part 1.4 of Eurocode3 (1995) for stainless steel structural members which uses a Perry-curve and an imperfection parameter of the type $\eta = \alpha(\lambda - \lambda_0)$. The values of α and λ_0 are different from those for carbon steel columns specified in Part 1.1 of Eurocode3 to account for differences between the mechanical properties of carbon steel and stainless steel alloys.

There are shortcomings to both of these design approaches: The tangent modulus approach leads to an *iterative* design procedure, while the selection of α and λ_0 in Part 1.4 of Eurocode3 is limited to a few stainless steel alloys for which test results are available. Unlike the ASCE Specification, Part 1.4 of Eurocode3 is not applicable to a wide range of stainless steel alloys with significantly different mechanical properties.

The purpose of this paper is to describe a general approach to the design of stainless steel columns failing by flexural buckling. This is achieved by firstly defining the mechanical properties in terms of the Ramberg-Osgood parameters (E_0, $\sigma_{0.2}$, n), which are assumed to have been obtained from curve fits of measured stress-strain curves of the finished product. Secondly, a Perry curve is adopted as strength curve by modifying the imperfection parameter to be expressed in terms of E_0, $\sigma_{0.2}$ and n. This procedure was described in detail by Rasmussen & Rondal (1995) for metal columns in general, and is applied to stainless steel columns in the present paper. Using the approach, it proves possible to define the column curve for *any* stainless steel alloy in terms of its Ramberg-Osgood parameters. The proposed design procedure is compared with tests of stainless steel columns and shown to be more accurate than the tangent modulus approach used in the ASCE Specification.

Explicit design approach

It is shown in Rasmussen & Rondal (1995) that the nondimensional column strength for flexural buckling (χ) can be calculated using the Perry-curve,

$$\chi = \frac{1}{\varphi + \sqrt{\varphi^2 - \lambda^2}} \tag{1}$$

$$\varphi = \frac{1}{2}\left(1 + \eta + \lambda^2\right) \tag{2}$$

where the imperfection parameter,

$$\eta = \alpha\left(\left(\lambda - \lambda_1\right)^{\beta} - \lambda_0\right) \tag{3}$$

is expressed in terms of the parameters $e = \sigma_{0.2}/E_0$ and n as follows:

$$\alpha(n,e) = \frac{1.5}{\left(e^{0.6} + 0.03\right)\left(n^{\left(\frac{0.0048}{e^{0.55}}\right)+1.4} + 13\right)} + \frac{0.002}{e^{0.6}} \tag{4}$$

$$\beta(n,e) = \frac{0.36\exp(-n)}{e^{0.45} + 0.007} + \tanh\left(\frac{n}{180} + \frac{6 \times 10^{-6}}{e^{1.4}} + 0.04\right) \tag{5}$$

$$\lambda_0(n,e) = 0.82\left(\frac{e}{e + 0.0004} - 0.01n\right) \geq 0.2 \tag{6}$$

$$\lambda_1(n,e) = 0.8\frac{e}{e + 0.0018}\left(1 - \left[\left(\frac{n - 5.5}{n + \dfrac{6e - 0.0054}{e + 0.0015}}\right)^2\right]^{0.6}\right) \tag{7}$$

In eqns (1,2), χ and λ are defined as

$$\chi = \frac{\sigma_u}{\sigma_{0.2}} \tag{8}$$

$$\lambda = \sqrt{\frac{\sigma_{0.2}}{\sigma_{E_0}}} \tag{9}$$

$$\sigma_{E_0} = \frac{\pi^2 E_0}{(L/r)^2} \tag{10}$$

where σ_u , L and r are the ultimate stress, effective length and radius of gyration respectively.

Equations (3-7) were derived by adjusting the functions α, β, λ_0 and λ_1 to produce close fits to column strength curves obtained using advanced finite element analyses of square hollow sections. The functions have been shown to be accurate within the ranges $e \in [0.001.0.008]$ and $n \in [3,\infty[$, which cover the structural stainless steel alloys used in practice.

The finite element analysis used in Rasmussen & Rondal (1995) was a geometric and material nonlinear analysis (Clarke, 1994) which allowed the stress-strain curve to be defined by a Ramberg-Osgood curve. The analysis has previously been shown (Rasmussen & Hancock, 1992) to be in close agreement with tests of beams. In Appendix III, the analysis is compared with tests of rectangular stainless steel columns. For the six columns analysed in Appendix III, the maximum discrepancy between the finite element strengths and test strengths is 4.3%. The average discrepancy is 2.8%. Thus, the finite element strengths and hence the proposed strength curves are considered accurate.

In the finite element analyses, the material properties were assumed to be the same through-out the cross-section. Thus, the stress-strain curve was assumed to have been derived from a stub column test which included the effect of residual stress. This approach circumvented the complexities associated with modelling accurately variations of proof stress around the tube and incorporating residual stresses in the finite element model. The columns were assumed to have sinusoidal overall geometric imperfections of magnitude 1/1500 times the length. This magnitude corresponded to the statistical mean of imperfections of carbon steel columns, as suggested by Bjorhovde (1972). The finite element analysis precluded local buckling deformations.

Comparison with tests

The design strength defined by eqns (1-10) and the design strengths obtained using the ASCE-LRFD Specification for the Design of Cold-formed Stainless Steel Structural Members (ASCE, 1990) are compared with tests of stainless steel columns in this section. The philosophy adopted in the ASCE Specification is to base the column strength on tests of concentrically loaded members and to incorporate the effect of geometric imperfections by using a relatively low resistance factor. This is consistent with using the tangent modulus approach for column design. Accordingly, the design strengths are compared with test strengths of concentrically loaded columns (Johnson & Winter 1966, Rasmussen & Hancock 1993, Hyttinen 1994).

According to the ASCE Specification, the nondimensional column strength shall be determined as,

$$\chi = \frac{\pi^2 E_t}{(L/r)^2 \sigma_{0.2}} \leq 1 \tag{11}$$

where the tangent modulus (E_t) may be expressed as a function of the buckling stress (σ_u) and the Ramberg-Osgood parameters,

$$E_t = \frac{E_0}{1 + 0.002n \dfrac{E_0}{\sigma_{0.2}} \left(\dfrac{\sigma_u}{\sigma_{0.2}} \right)^{n-1}} \tag{12}$$

The Ramberg-Osgood parameters of the specimens of each test series are summarised in Table 1. The parameters were obtained from stub column tests and so included the effect of residual stress. The specimens tested by Johnson & Winter (1966) were produced from austenitic AISI 304 annealed and skin-passed cold-formed sheets. The specimens tested by Rasmussen & Hancock (1993) and Hyttinen (1994) were cold-rolled from annealed flat sheets. Rasmussen & Hancock tested austenitic 304L alloy specimens while Hyttinen tested austenitic 304 and ferritic 409 specimens. In all tests, local buckling did not occur prior to the ultimate load.

The design strengths are compared with test strengths in Table 2, in which the ratio

$$P = \frac{\sigma_u / \sigma_{0.2}}{\chi} \tag{13}$$

is the nondimensional test strength divided by the nondimensional design strength. The reductions of test data were made using the measured values of section and material properties. It follows from the table that the coefficient of variation of P is 0.115 and 0.145 for the present design procedure and the procedure specified in the ASCE Specification respectively. Consequently, the present design procedure is more accurate.

The mean of P is 1.23 and 1.14 for the present design procedure and the procedure specified in the ASCE Specification respectively, as also shown in Table 2. Thus, the design strengths derived using the present design procedure are generally lower than the test strengths, and are lower than those obtained using the ASCE Specification. This is a consequence of the fact that the present design procedure is based on analyses of members with overall geometric imperfections of 1/1500 of the length, while the tangent modulus approach used in the ASCE Specification is based on geometrically perfect members. However, from a design point of view, the mean value of P is less significant than the coefficient of variation of P. As shown in the following section, the resistance factor (Φ) is proportional to the mean value of P for a given value of reliability index (β_r), and so an increase of the mean value (P_m) simply leads to a proportional increase of Φ. In contrast, greater values of the coefficient of variation (V_P) lead to a reduction of Φ.

Reliability analysis

The LRFD calibration of the ASCE Specification is described in Lin et al. (1988). Consistent with this reference, the present reliability analysis is based on a load combination of $1.2\,D_n + 1.6 L_n$, where D_n and L_n are the nominal values of dead and live load respectively, and the resistance factor (Φ) is determined at $D_n/L_n = 0.2$. Consequently (see Commentary of ASCE (1990) or Lin et al. (1988)), the reliability index (β_r) is calculated as

$$\beta_r = \frac{\ln\left(\dfrac{1.52 M_m F_m P_m}{\Phi}\right)}{\sqrt{V_R^2 + V_Q^2}} V_R \tag{14}$$

where M_m and F_m are the mean values of the random variables M and F which account for variability of nominal material and geometric properties respectively, and V_R and V_Q are given by

$$V_R^2 = V_M^2 + V_F^2 + V_P^2 \tag{15}$$

$$V_Q^2 = \frac{\sqrt{\left(1.05D_n / L_n V_D\right)^2 + V_L^2}}{1.05D_n / L_n + 1} \tag{16}$$

In eqns (15,16), V_M, V_F, V_D and V_L are the coefficients of variation of the random variables M, F, D and L respectively. The values used in calibrating the ASCE Specification were, M_m=1.1, F_m=1.0, V_M=0.1, V_F=0.05, V_D=0.1 and V_L=0.25. The random variable P accounts for variability in the design model such that its mean value (P_m) and coefficient of variation (V_P) are those summarised in Table 2. It follows from eqn. (14) that by scaling the mean P_m, the same value of reliability index (β_r) is achieved by scaling the resistance factor (Φ) by the same value.

The reliability index is drawn against the ratio D_n/L_n in Figs 1 and 2 using the values of P_m and V_P obtained from the present design procedure and the ASCE Specification respectively. In calibrating the ASCE Specification, the target reliability index (for members) was chosen as 3.0 (Lin et al., 1988). This value produced a resistance factor of 0.85. It follows from Fig. 1 that the target reliability index of 3.0 can be achieved for the present design procedure at D_n/L_n=0.2 by choosing a resistance factor of 0.9. Similarly, it follows from Fig. 2 that a resistance factor of 0.8 should be selected for the ASCE Specification in order to achieve a target reliability index of 3.0. This value is less than the value of 0.85 specified in the ASCE Specification because the calibration described herein is based on tests with slightly greater variability than those used in the calibration described in Lin et al. (1988).

Proposed design procedure

General

In formulating design rules for cold-formed stainless steel members, a distinction needs to be made as to whether the forming process significantly alters the mechanical properties or not. For instance, if the section is brake-pressed, the mechanical properties are unchanged, (excepting the corner regions which are usually small), and hence the properties can be assumed to be those of the sheet from which the product was pressed. In this case, the properties provided by the ASCE Specification can be used for design. Conversely, if the section is cold-rolled, the mechanical properties are significantly changed by the forming process, and hence an accurate and economical design procedure requires that the properties be based on the finished product. In this case, the mechanical properties should be based on stub column tests of the finished product, or, if these are affected by local buckling, the properties should be based on compression tests of coupons cut from the finished product.

Material properties obtained from the ASCE Specification

In cases where the mechanical properties can be based on those of the sheet from which the section was formed, the mechanical properties listed in the ASCE Specification can be used to calculate values of the α, β, λ_0 and λ_1 parameters defining the imperfection parameter. For this type of product, the material statistical data is as used above (M_m=1.1, V_M=0.1) and consequently, the resistance factor derived above is directly applicable.

The Ramberg-Osgood parameters of the most common structural alloys are shown in Table 3, as obtained from the ASCE Specification. The corresponding values of α, β, λ_0 and λ_1 are also shown in the table.

It is proposed that the column strength (ΦP_m) be determined using

$$\Phi = 0.9 \tag{17}$$

$$P_n = \chi \sigma_{0.2} A_e \tag{18}$$

where A_e is the effective area and χ is determined using eqns (1-3) in conjunction with the values of α, β, λ_0 and λ_1 specified in Table 3.

For the purpose of comparing the nominal strengths produced using the proposed design procedure and the ASCE Specification, the nondimensional column strength (χ) is shown in Figs 3 and 4 for annealed AISI 304 alloy and AISI 409 alloy respectively, as obtained using the two design approaches. It appears from the figures that the proposed design curves are lower than the curves obtained using ASCE Specification. As explained previously, this is a result of the fact that the proposed design procedure is based on analyses of geometrically imperfect columns while the ASCE Specification is based on tests for which the geometric imperfections were negligible. As also explained, this difference is taken into account in selecting the resistance factor. The variations of the strength curves with λ are similar for the 304 alloy, as shown in Fig. 3. Some difference between the strength curves is observed for the 409 alloy in the intermediate slenderness range, as shown in Fig. 4. The main difference between the alloys is the exponent n which equals 4.1 and 9.7 for annealed 304 and 409 alloy respectively. These values are the minimum and maximum values of n specified in the ASCE Specification.

Material properties obtained from stub column tests

When a section is produced by a process that significantly changes the mechanical properties, it may become uneconomical to base the design on the properties of the virgin plate or coil. In this case, it is proposed that stub column tests or compression coupon tests be performed to determine the Ramberg-Osgood parameters of the finished product. Having obtained the properties of the finished product, the nondimensional column strength (χ) can be obtained using eqns (1-10), and the nominal column strength can be obtained using eqn. (18).

However, it is likely that the mechanical properties will be based on only a few tests. In the reliability analysis described above, the mean and coefficient of variation of the variable M were assumed to be 1.10 and 0.1 respectively. The mean value was higher than unity because the mechanical properties listed in the ASCE Specification were selected (Johnson, 1966) as 90 % fractile values obtained from a large number of coupon tests. If the mechanical properties are based on the mean of a few stub column tests only, it is reasonable to choose values of M_m and V_M of 1.0 and 0.1 respectively. In this case, the reliability index varies as shown in Fig. 5, and it follows that the resistance factor should be chosen as,

$$\Phi = 0.85 \tag{19}$$

in order to obtain a target reliability index of 3.0 at a value of D_n/L_n of 0.2.

Conclusions

A design procedure has been described for stainless steel columns failing by flexural buckling. The procedure adopts a Perry-curve as strength curve and expresses the imperfection parameter in terms of Ramberg-Osgood parameters. The procedure is explicit and applicable to any stainless steel alloy. It is shown that the procedure leads to a smaller coefficient of variation than the iterative procedure currently used in the ASCE Specification when compared with tests. Thus, the proposed procedure is advantageous on two counts: It is explicit and more accurate than the current method of the ASCE Specification.

The resistance factor to be used with the proposed design method has been derived using a target reliability index of 3.0. Depending on whether the mechanical properties are based on those provided by the ASCE Specification or obtained from stub column or coupon tests, the resistance factor was obtained as 0.9 and 0.85 respectively. The proposed design procedure provides a simple way of incorporating the effect of cold-forming in design. It requires only that stub column tests be performed to obtain the Ramberg-Osgood parameters of the finished product.

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Appendix II: Notation

A	Cross-section area
A_e	Effective cross-section area
D	Dead Load
D_m,D_n	Mean and nominal values of D
e	Nondimensional yield stress, ($e=\sigma_{0.2}/E_0$)
E_0	Initial elastic modulus
E_t	Tangent modulus
F	Variable accounting for differences between measured and nominal *geometric* values
F_m	Mean value of F
I_x	Second moment of area about x-axis
L	Effective column length
L	Dead Load
L_m,L_n	Mean and nominal values of L
M	Variable accounting for differences between measured and nominal *material* values

M_m	Mean value of M
n	Exponent in Ramberg-Osgood expression
P	Ratio of test strength to design strength
P_m	Mean value of P
P_n	Nominal column strength
$P_{u,exp}$	Test strength
$P_{u,FE}$	Finite element strength
r	Radius of gyration
v	Lateral deflection at midspan
v_0	Geometric imperfection at midspan
V_M, V_F, V_P	Coefficients of variation of M, F and P
V_D, V_L	Coefficients of variation of D and L
V_Q, V_R	Coefficients of variation of Q and R, see eqns (15,16)
α	Parameter used to define the imperfection parameter (η)
β	Parameter used to define the imperfection parameter (η)
β_r	Reliability index
η	Imperfection parameter
λ	Column slenderness, ($\lambda = \sqrt{\sigma_{0.2} / \sigma_{E_0}}$)
λ_0	Parameter used to define the imperfection parameter (η)
λ_1	Parameter used to define the imperfection parameter (η)
σ_u	Ultimate stress
σ_{E_0}	Flexural buckling stress based on E_0
$\sigma_{0.2}$	0.2% proof (or off-set) stress
φ	Parameter used to define the nondimensional column strength (χ)
Φ	Resistance factor
χ	Nondimensional column design strength

Appendix III: Verification of finite element analysis for compression members

The finite element analysis described and applied by Clarke (1994) is compared with tests of rectangular hollow section (RHS) stainless steel columns (Talja & Salmi, 1995) in this appendix. The test program comprised three cross-sections, referred to as RHS-1, RHS-2 and RHS-3. Of these, only sections RHS-1 and RHS-3 have been included in the present comparison because section RHS-2 was slender and hence the strengths were influenced by local buckling. This phenomenon could not be modelled in the finite element analysis which precluded local buckling deformations.

The specimens were tested between pinned ends that only allowed rotations about the major x-axis. The geometric and material properties of the Series RHS-1 and RHS-3 specimens are shown in Table 4, in which A and I_x are the cross-section area and second moment of area about the x-axis respectively. In the finite element analysis, only half of the cross-section was modelled using symmetry. Five monitoring stations were used along each half-flat and each corner of the cross-section. Similarly, five monitoring stations were used through the thickness. No residual stress was included in the model except for its influence on the stress-strain curve. The columns were divided into 10 elements longitudinally. Three monitoring stations were used along each element using 3rd order Gaussian integration. This discretisation was the same as that used in Rasmussen and Rondal (1995). The overall geometric imperfections (v_0) were assumed to be sinusoidal with magnitudes at the centre equal to the measured values reported by Talja & Salmi (1995), as shown in Table 5. A magnitude of $L/1500$ was used for those specimens for which no geometric imperfection was reported.

The tests and finite element results are compared in Table 5 and Fig. 6. Table 5 details the ultimate loads obtained in the tests ($P_{u,exp}$) and using the finite element analysis ($P_{u,FE}$), while Fig. 6 shows the load-deflection curves (P vs v) for the six specimens analysed. The experimental curves shown in Fig. 6 were obtained from Appendix 3/1 of Talja & Salmi (1995). In Table 5, L is the pin-ended length equal to the distance between the axes of the end-bearings of the test rig.

It follows from Table 5 that the ultimate loads obtained from the finite element analysis are equal to the test strengths to within 4.3%. On average, the discrepancy between the test strengths and the finite element strengths is 2.8%. Thus, the ultimate strengths are in close agreement. As shown in Fig. 6b, there is some difference between the measured load-deflection curves and the finite element results for the Series RHS-3 specimens. The discrepancy is primarily attributed to differences between the measured and modelled geometric imperfections. Generally, the initial stiffness of the finite element curves is higher than the measured values. The agreement is good for all Series RHS-1 specimens, as shown in Fig. 6a.

Reference	Alloy	Section type	E_0 (MPa)	$\sigma_{0.2}$ (MPa)	n
Johnson & Winter (1966)	304	I	204100	238	4.1
Johnson & Winter (1966)	304	box	204100	238	4.1
Rasmussen & Hancock (1993)	304L	SHS	191000	440	3.0
Rasmussen & Hancock (1993)	304L	CHS	201000	380	6.0
Hyttinen (1994)	304	SHS-1	194000	482	2.61
Hyttinen (1994)	304	SHS-2	192000	478	2.92
Hyttinen (1994)	304	SHS-3	193000	585	2.43
Hyttinen (1994)	409	SHS-4	195000	482	3.05
Hyttinen (1994)	409	SHS-5	195000	463	3.14
Hyttinen (1994)	(ferritic)	SHS-6	204000	536	2.81
Hyttinen (1994)	(ferritic)	SHS-7	201000	508	3.00

Table 1. Ramberg-Osgood parameters of test specimens

Reference	Type	L/r	λ	σ_u (MPa)	Present paper χ	Present paper $(\sigma_u / \sigma_{0.2})/ \chi$	ASCE Specification χ	ASCE Specification $(\sigma_u / \sigma_{0.2})/ \chi$
Johnson & Winter (1966)	I	36.84	0.400	295	0.906	1.37	0.936	1.33
	I	45.74	0.497	255	0.794	1.35	0.829	1.29
	I	54.44	0.592	230	0.717	1.35	0.744	1.30
	I	59.68	0.649	204	0.677	1.27	0.702	1.22
	I	70.69	0.768	182	0.604	1.27	0.629	1.22
	I	79.88	0.868	164	0.550	1.25	0.577	1.19
	I	99.96	1.09	124	0.448	1.17	0.484	1.08
	I	130.03	1.41	93.7	0.328	1.20	0.374	1.05
	I	158.19	1.72	66.6	0.249	1.13	0.292	0.958
	I	177.03	1.92	56.1	0.209	1.13	0.247	0.954
Johnson & Winter (1966)	box	37.25	0.405	321	0.900	1.50	0.930	1.45
	box	55.66	0.605	250	0.707	1.48	0.733	1.43
	box	72.37	0.787	201	0.594	1.42	0.619	1.36
	box	81.62	0.887	195	0.541	1.51	0.568	1.44
Rasmussen & Hancock (1993)	SHS	46	0.703	434	0.820	1.20	0.783	1.26
	SHS	79	1.21	215	0.437	1.12	0.449	1.09
	SHS	111	1.70	107	0.262	0.925	0.286	0.848
Rasmussen & Hancock (1993)	CHS	42	0.581	360	0.792	1.20	0.837	1.13
	CHS	70	0.969	254	0.584	1.19	0.636	1.09
	CHS	99	1.37	187	0.396	1.24	0.466	1.06
	CHS	127	1.76	124	0.271	1.20	0.317	1.03
Hyttinen (1994)	SHS-1	196	3.11	50.3	0.0890	1.17	0.0985	1.06
	SHS-2	145	2.30	84.7	0.158	1.12	0.175	1.01
	SHS-3	108	1.89	138	0.214	1.10	0.233	1.02
	SHS-4	195	3.08	52.4	0.0939	1.16	0.103	1.06
	SHS-5	144	2.23	83.2	0.168	1.07	0.187	0.959
	SHS-6	194	3.17	53.5	0.0880	1.14	0.0964	1.04
	SHS-7	144	2.31	86.9	0.158	1.08	0.175	0.977
Mean (P_m)						1.23		1.14
Coefficient of variation (V_P)						0.115		0.145

Table 2. Comparison of design strengths with test strengths

Property	Alloy and hardness					
	201, 301, 304, 316				409	430,439
	annealed	1/16-hard	1/4-hard	1/2-hard		
E_0 (MPa)	193100	193100	186200	186200	186200	186200
$\sigma_{0.2}$ (MPa)	193.1	282.5	344.8	448.2	206.9	275.8
n	4.10	4.10	4.58	4.22	9.70	6.25
α	1.56	1.45	1.29	1.27	0.74	1.07
β	0.27	0.22	0.16	0.16	0.18	0.14
λ_0	0.55	0.61	0.64	0.67	0.52	0.59
λ_1	0.21	0.29	0.37	0.39	0.20	0.34

Table 3. Ramberg-Osgood parameters specified in ASCE-LRFD Specification and corresponding values of α, β, λ_0 and λ_1

Series	E_0 (MPa)	$\sigma_{0.2}$ (MPa)	e	n	A (mm^2)	I_x (mm^4)
RHS-1	192000	569	0.00296	3.39	999	4.896×10^5
RHS-3	192000	385	0.00201	3.33	2683	8.189×10^6

Table 4. Material and geometric properties of test specimens (Talja & Salmi, 1995)

Series	Specimen	L (mm)	v_0 (mm)	$P_{u,exp}$ (kN)	$P_{u,FE}$ (kN)	Error (%)
RHS-1	CC-2	1050	-	417	406	2.6
	CC-3	1700	-	235	225	4.3
	CC-4	2350	-	137	137	0
RHS-3	CC-2	2700	1	830	806	2.9
	CC-3	4350	-	488	470	3.7
	CC-4	6000	2	306	317	3.6

Table 5. Comparison of finite element strengths with test strengths

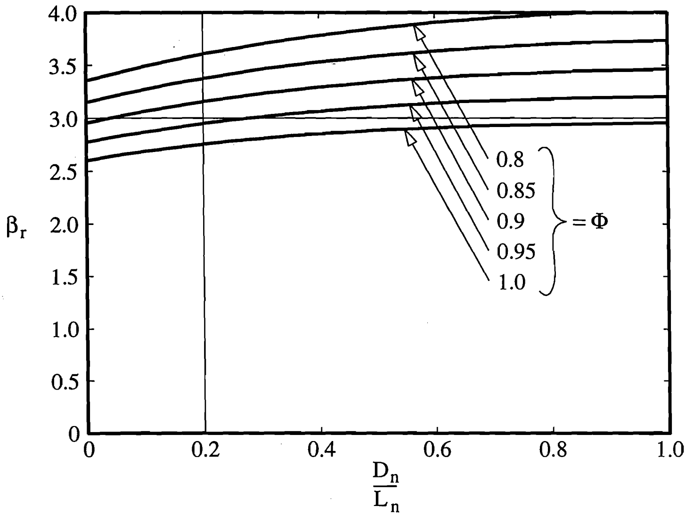


Fig 1: Reliability index, explicit formulation (M_m=1.1 and V_M=0.1)

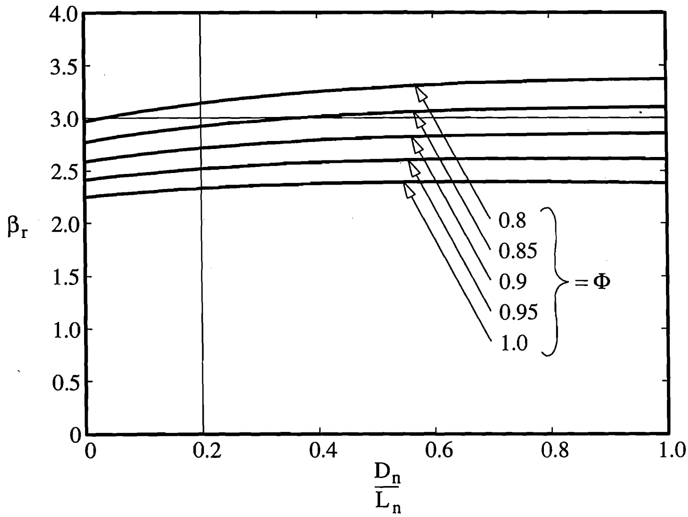


Fig 2: Reliability index, ASCE-LRFD Specification

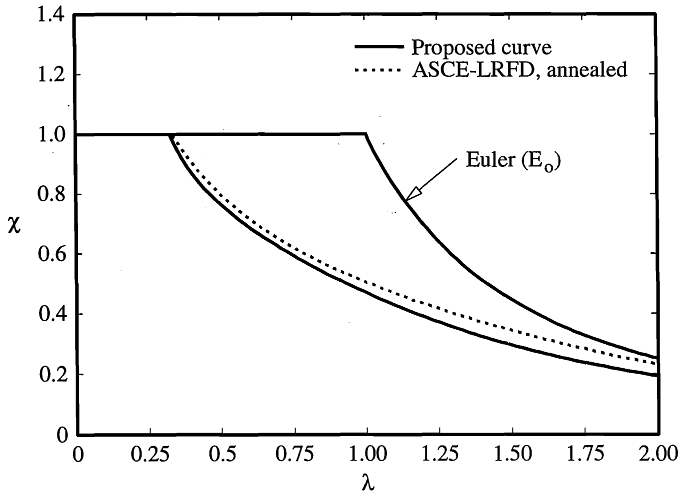


Fig 3: Proposed and ASCE-LRFD strength curves, annealed AISI 304 alloy

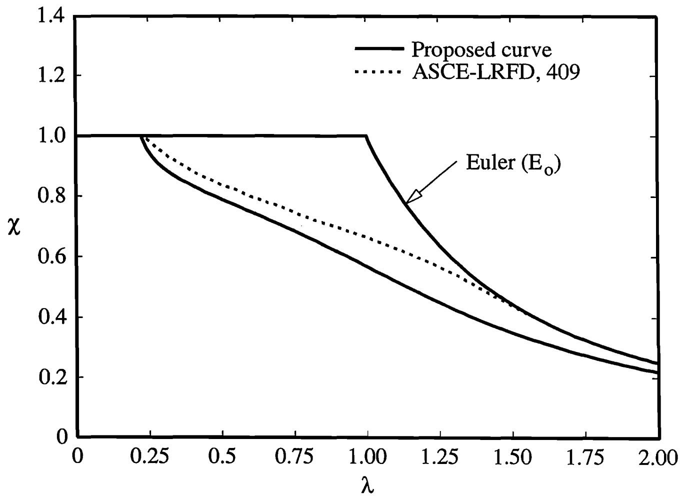


Fig 4: Proposed and ASCE-LRFD strength curves, AISI 409 alloy

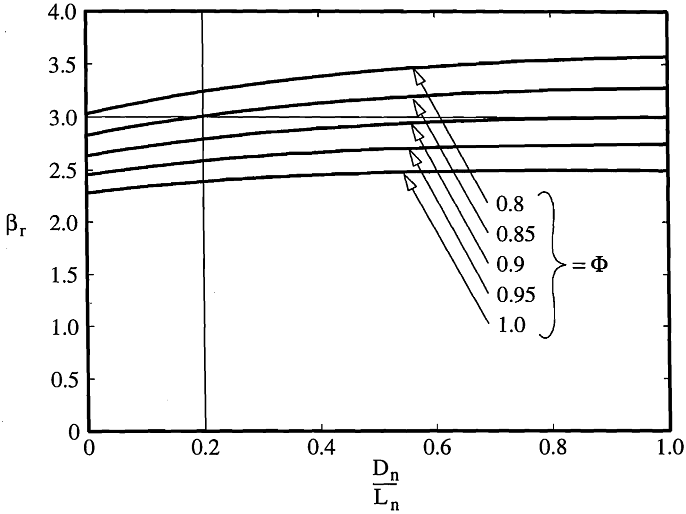
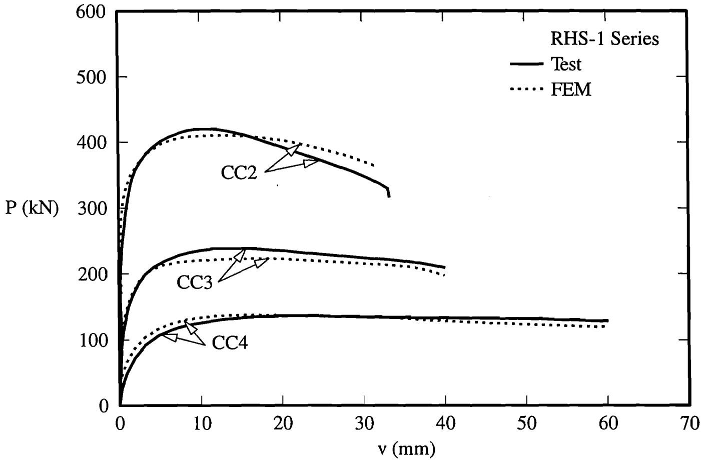
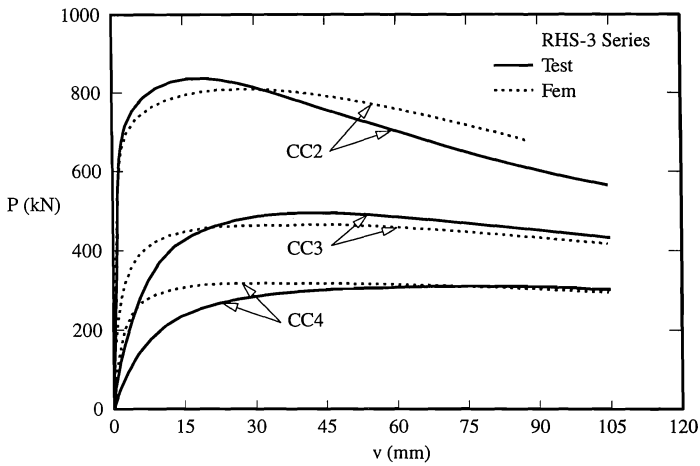


Fig 5: Reliability index, explicit formulation (M_m=1.0 and V_M=0.1)



(a) RHS-1 Series



(b) RHS-3 Series

Fig. 6: Experimental and finite element load-deflection curves for RHS stainless steel columns