

Local stability of laser-welded stainless steel I-sections in bending

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Abstract

Design guidance for stainless steel structures has become more comprehensive and widely available in recent years. This, coupled with a growing range of structural products and increasing emphasis being placed on sustainable and durable infrastructure, has resulted in greater use of stainless steel in construction. A recent addition to the range of structural stainless steel products is that of laser-welded sections. Owing to the high precision and low heat input of the fabrication process, the resulting sections have smaller heat affected zones, lower thermal distortions and lower residual stresses than would typically arise from traditional welding processes. There currently exists very limited experimental data on laser-welded stainless steel members and their design is not covered by current design standards. The focus of this study is therefore to investigate the cross-sectional behaviour of laser-welded stainless steel I-sections in bending. The present paper describes a series of laboratory tests performed on laser-welded stainless steel I-sections, including tensile coupon tests, initial geometric imperfection measurements and in-plane bending tests. Results of the bending tests are used to validate finite element (FE) models, which are subsequently employed for parametric investigations. The obtained experimental and FE results are used to assess the applicability of the existing design provisions of EN 1993-1-4, AISC Design Guide 27 and the continuous strength method (CSM) to laser-welded stainless steel sections. It was found that the scope of application of these existing design provisions may be safely extended to laser-welded sections.

Keywords: Bending; Eurocode 3; Experiments; Laser-welding; Numerical modelling; Stainless steel; Testing

1. Introduction

Stainless steel is used in a wide range of engineering applications. In construction, typical applications include wall claddings, architectural components and structural elements in buildings and bridges. The broad use of stainless steel is largely attributed to the corrosion resistance, aesthetic appeal, favourable structural properties and a range of other beneficial characteristics. An innovative means of joining stainless steel is laser-welding. Laser-welding generally involves relatively low heat input, producing smaller heat affected zones, lower thermal distortions and lower residual stresses than would typically arise from traditional welding processes. The welding seams are also small, resulting in sections featuring sharp edges and corners. The use of lasers as a production tool first emerged in the late 1960s, with laser-welding maturing into an economic and practical process in the 1980s, but it is only recently that the fabrication method has gained traction in the construction industry. Hence, very limited experimental data on laser-welded stainless steel members have been reported to date. This paper presents a series of laboratory tests performed on laser-welded stainless steel I-sections in bending, together with a numerical study and the assessment of the applicability of existing design rules to these sections.

2. Material properties, geometric imperfections and residual stresses

In addition to the beam tests, which are described in Section 3, a series of complementary material tensile coupon tests, stub column tests, initial geometric imperfection measurements and residual stress measurements have been carried out; a summary of these measurements is provided herein, while a more comprehensive description is given in [1].

2.1. Tensile coupon tests

The laser-welded I-section profiles investigated in this paper were fabricated from hot-rolled austenitic stainless steel plates (Grades 1.4307, 1.4404, 1.4571). Tensile coupon tests were conducted on the plate material and the key measured properties obtained are presented in Table 1. The adopted specimen designation system can be explained by means of an example - e.g. specimen I-102×68×5×5 indicates an I-section of dimensions (in mm) 102 (section height h) × 68 (section width b_f) × 5 (web thickness t_w) × 5 (flange thickness t_f), with the final letter (W=web; F=flange) indicating the component from which the coupon was extracted. This notation is shown in Figure 1. All tensile coupon tests performed herein were

conducted using an Instron 8802 250 kN hydraulic testing machine, following the procedure set out in EN ISO 6892-1 [2]. The reported material properties are Young's modulus E , 0.2% proof stress f_y , 1% proof stress $f_{1.0}$, ultimate tensile stress f_u , strain at ultimate stress ε_u , strain at fracture measured over the standard gauge length ε_f [2] and the strain hardening exponents for the compound Ramberg-Osgood model n , $n'_{0.2,1.0}$ and $n'_{0.2,u}$ [3-6]. For I-sections comprising plates of the same thickness t , a single coupon test was performed, while for sections made up of plates of different thicknesses, tensile tests on coupons extracted from both the flange (F) and the web (W) were conducted. The tested austenitic stainless steel material exhibited high ductility, with strains at fracture in excess of 50%.

Table 1: Measured material properties from tensile coupon tests.

Specimen	t (mm)	E (N/mm ²)	f_y (N/mm ²)	$f_{1.0}$ (N/mm ²)	f_u (N/mm ²)	ε_u (%)	ε_f (%)	Compound R-O coefficients		
								n	$n'_{0.2,1.0}$	$n'_{0.2,u}$
I-50×50×4×4	4.01	190700	270	361	694	61	73	4.0	3.2	3.0
I-102×68×5×5	4.99	186800	222	331	580	50	64	3.2	3.9	3.8
I-150×75×7×10-W	6.88	197300	274	344	596	58	68	5.0	3.1	3.0
I-150×75×7×10-F	9.88	197200	267	323	560	50	66	5.0	2.7	2.7
I-160×82×10×12-W	9.81	198500	264	341	618	53	64	5.2	3.0	3.1
I-160×82×10×12-F	11.69	197500	286	342	619	52	65	7.5	2.7	2.5
I-203×133×6×8-W	5.90	192500	251	320	576	55	67	4.1	2.9	2.9
I-203×133×6×8-F	7.85	192500	281	365	597	47	64	4.2	3.4	3.3
I-220×110×6×9-W	6.02	193000	275	349	670	61	71	6.0	2.9	2.8
I-220×110×6×9-F	8.91	197200	292	355	671	55	65	6.2	2.5	2.6

2.2. Stub column tests

Stub column tests were conducted in accordance with the provisions of [7, 8] to assess the compressive response and susceptibility to local buckling of the laser-welded I-sections, as reported in [1]. The average material properties obtained from the tests are used in this study as compressive material properties and assigned to the upper halves of the beams in the numerical simulations described in Section 4.2. The measured load-end shortening (N - δ) responses from the stub column tests were converted into a stress-strain (σ - ε) curves by means of the cross-sectional area A_{sc} ($\sigma = N/A_{sc}$) and length L_{sc} ($\varepsilon = \delta/L_{sc}$). The compound Ramberg-Osgood model that utilises the 0.2% proof stress f_y and 1.0% proof stress $f_{1.0}$ [16]

was then fitted to the available stress-strain data up to the point at which local buckling initiated; beyond this point, the stress-strain curve was extrapolated in parallel with the corresponding tensile stress-strain curve up to the ultimate tensile stress. A comparison between typical tensile and corresponding compressive (extrapolated stub column) stress-strain curves (up to 10% strain) is shown in Figure 2, while the key compressive material properties for all the tested cross-sections are summarized in Table 2 in which E_c is the compressive Young's modulus, f_{yc} is the compressive 0.2% proof strength and n_c and $n'_{0.2,1.0,c}$ are the compound Ramberg-Osgood exponents.

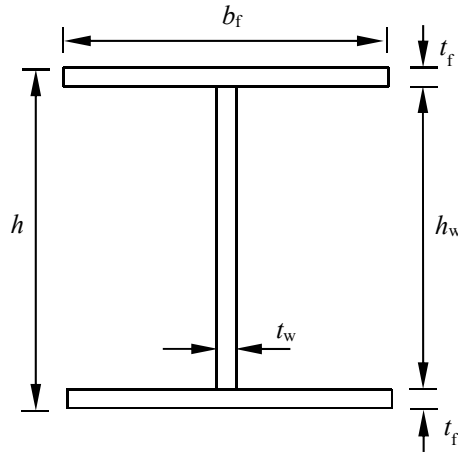


Figure 1: Cross-section geometry and notation of test specimens

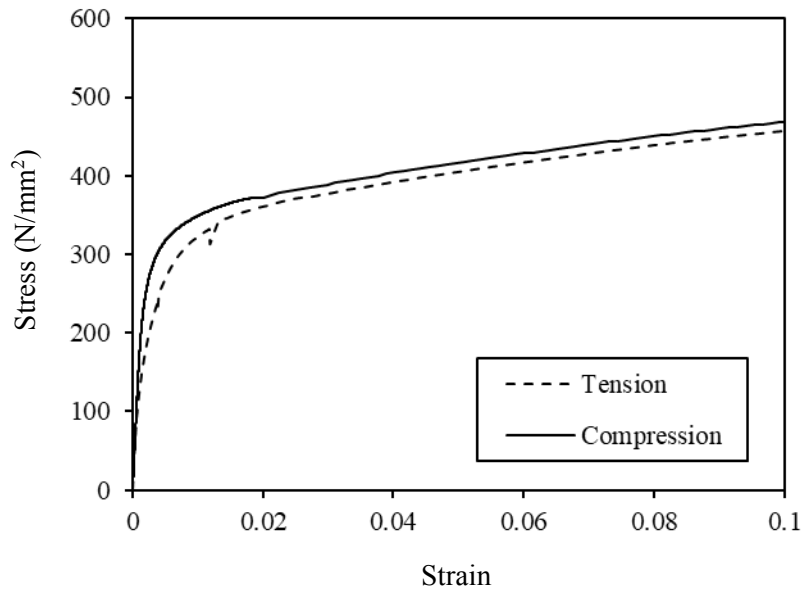


Figure 2: Typical tensile and compressive stress-strain curves

Table 2: Measured compressive (stub column) material properties.

Cross-section	A_{sc} (mm ²)	L_{sc} (mm)	E_c (N/mm ²)	f_{yc} (N/mm ²)	Compound R-O coefficients	
					n_c	$n'_{0.2,1.0,c}$
I-50×50×4×4	569.4	150.3	206900	332	7.4	3.2
I-102×68×5×5	1153.1	304.7	190800	291	6.4	3.9
I-140×140×10×12	4455.8	420.3	193300	306	7.0	2.3
I-150×75×7×10	2423.0	450.1	202600	298	6.5	2.9
I-152×160×6×9	3655.3	456.1	201900	270	7.8	2.9
I-160×82×10×12	3287.6	480.2	204100	339	7.5	2.9
I-203×133×6×8	3214.5	612.1	208100	301	7.5	3.3
I-220×110×6×9	3192.3	660.0	197300	304	7.5	2.7
I-300×150×7×11	5434.3	840.0	204100	295	8.2	2.8

2.3. Initial geometric imperfection measurements

Local geometric imperfections of the test specimens were measured, following the procedure reported in [9], to aid in the assessment of the structural behaviour and for later use in the numerical models. Each section size was measured to determine a representative value of local imperfection amplitude. The test piece was affixed to the flat bed of a milling machine (see Figure 3) while a displacement transducer, attached to the head of the milling machine, was moved along a representative 600 mm length of the specimen at the cross-section locations shown in Figure 3(b), recording displacements at 5 mm intervals longitudinally. The same approach has been successfully adopted in previous studies [10, 11]. The maximum imperfection amplitude was defined as the maximum deviation from a linear trend line fitted to the data set. These maximum measured values of the local imperfections w_0 for each cross-section size, which were well within the tolerances set out in EN 1090-2 [12], are reported in Section 3.

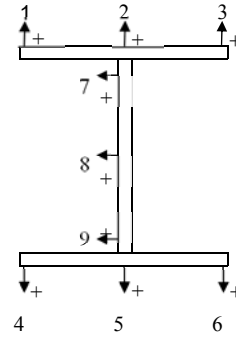
2.4. Residual stress measurements

The residual stress patterns in two of the tested laser-welded stainless steel cross-sections (I-140×140×10×12 and I-102×68×5×5) were measured using the sectioning method following the procedures recommended by the Structural Stability Research Council [7]. Residual stress measurements on laser-welded stainless steel T-sections [13] and conventionally welded stainless steel I-sections [14]

have also been reported.



(a) Imperfection measurement setup



(b) Imperfection measurement locations

Figure 3: Measurement of initial local geometric imperfections.

Based on the measurements, a predictive residual stress model, shown in Figure 4, was developed for laser-welded austenitic stainless steel I-sections. The key parameters are given in Table 3, with equivalent predictive models for conventionally welded carbon steel [15, 16] and austenitic stainless steel [14] I-sections also listed for comparison. This residual stress pattern is adopted in the numerical study presented in Section 4 of this paper.

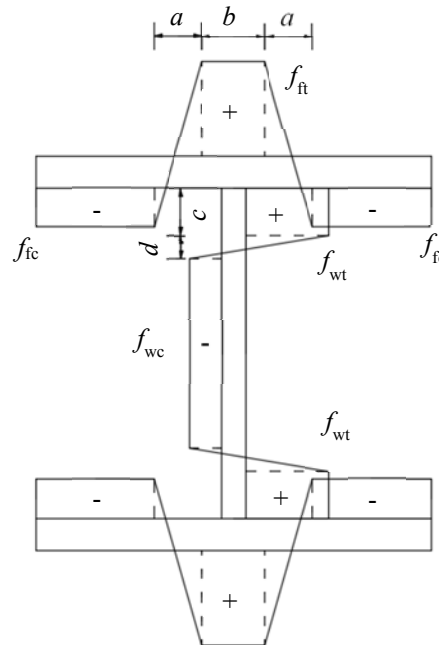


Figure 4: Residual stress distribution for welded I-sections.

Table 3: Parameters in predictive models for welded I-sections.

Predictive model	$f_{ft}=f_{wt}$	$f_{fc}=f_{wc}$	a	b	c	d
Gardner et al. [1]	$0.5f_y$	From equilibrium	$0.1b_f$	$0.075b_f$	$0.0375h_w^*$	$0.1h_w^*$
Yuan et al. [14]	$0.8f_y$	From equilibrium	$0.225b_f$	$0.05b_f$	$0.025h_w$	$0.225h_w$
ECCS [15]	f_y	$0.25f_y$	$0.05b_f$	$0.15b_f$	$0.075h_w$	$0.05h_w$
BSK 99 [16]	$0.5f_y$	From equilibrium	$0.75t_f$	$1.5t_f$	$1.5t_w$	$1.5t_w$

* Note that these are corrected values, replacing those originally presented in [1].

3. Beam tests

3.1. Major axis bending tests

Eight major axis in-plane bending tests were carried out to assess bending resistance and rotation capacity of laser-welded stainless steel I-sections. Four-point (4PB) and three-point (3PB) bending configurations were used to achieve constant moment (between the loading points) and a moment gradient, respectively. All beams were simply supported between two steel rollers placed 100 mm from the ends of the beams. Lateral restraints were provided near the loading points and end supports to prevent lateral-torsional buckling. The geometry of the specimens and local imperfections were measured prior to the tests, as given in Table 4, where L is the span of the beams, L_m is the distance between the two loading points in the four-point bending configuration and w_0 is the local imperfection amplitude measured as reported in Section 2.3.

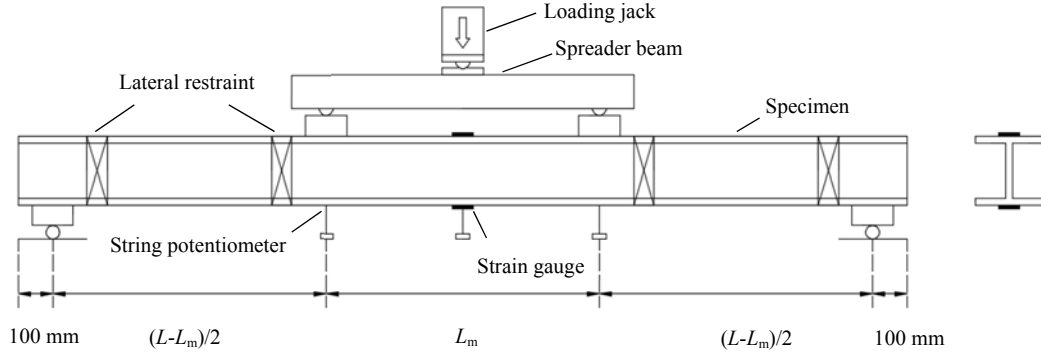
Table 4: Measured dimensions of specimens in bending about the major axis.

Cross-section	Specimen ID	Span L	3/4PB	L_m	h	b_f	t_w	t_f	w_0
		(mm)		(mm)	(mm)	(mm)	(mm)	(mm)	(mm)
I-102×68×5×5	B1	1600	4	600	102.31	67.98	5.03	5.08	0.22
I-102×68×5×5	B2	1600	3	-	101.81	67.80	4.97	5.03	0.22
I-160×82×10×12	B5	1700	4	600	160.26	83.35	9.83	11.79	0.16
I-160×82×10×12	B6	1700	3	-	160.12	82.48	9.81	11.86	0.16
I-50×50×4×4	B9	900	4	400	50.39	50.52	4.00	3.98	0.23
I-50×50×4×4	B10	900	3	-	50.74	50.53	4.07	4.03	0.23
I-150×75×7×10	B17	1700	4	600	150.77	75.90	6.92	9.87	0.16
I-150×75×7×10	B18	1500	3	-	151.10	75.98	6.97	9.90	0.16

The non-dimensional lateral-torsional buckling slendernesses of the tested members, as defined in EN 1993-1-1 [17], varied between 0.27 and 0.37, which are less than the EN 1993-1-4 [18] limit of 0.4, below which lateral-torsional buckling can be ignored. For the four-point bending tests, a spreader beam was used to evenly apply the load at two points. For the three-point bending tests, the load was applied at mid-span. In both cases, the load was applied through 40 mm wide and 8 mm thick steel bearing plates. Wooden blocks were also placed between the flanges of the I-sections at the locations of the applied loads, the lateral restraints and the end supports to prevent local web bearing failure. To monitor the progression of strain during testing, strain gauges were attached to the top and bottom flanges at mid-span for the four-point bending setup and at a distance of 60 mm from the mid-span to provide sufficient space for the introduction of the load in the three-point bending configuration. Three string potentiometers were placed at mid-span and at the two loading points, for the four-point bending configuration and one string potentiometer was placed at mid-span for the three-point bending configuration. For the three-point bending tests, inclinometers were also positioned at each end of the beam specimens to measure the end rotations. The test setup for the four-point bending and the three-point bending tests are shown in Figures 5 and 6, respectively. The tests were carried out using an INSTRON 750 kN testing machine under displacement control at a rate of 3.0 mm/min for all specimens, with the exception of specimens B9 and B10, which had shorter spans and were tested at a rate of 1.5 mm/min. All measured values were recorded using the DATASCAN data acquisition system.

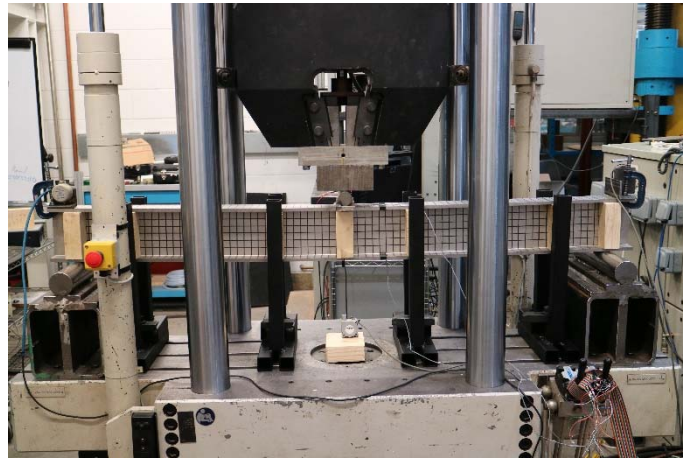


(a) Experimental setup

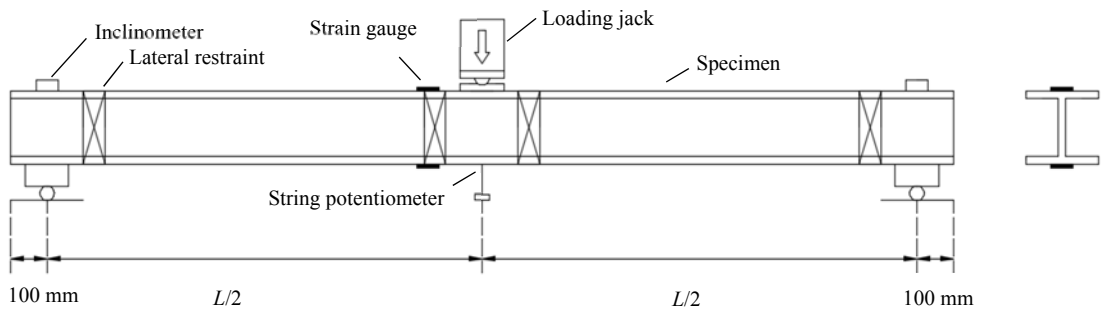


(b) Schematic diagram

Figure 5: Experimental setup for the major axis four-point bending tests.



(a) Experimental setup



(b) Schematic diagram

Figure 6: Experimental setup for the major axis three-point bending tests.

The ultimate bending moment M_u , the cross-section rotation capacity R and the other key experimental results are summarised in Tables 5 and 6, for the four-point and three-point major axis bending tests,

respectively, where M_{el} is the elastic moment capacity and M_{pl} is the plastic moment capacity. The elastic and plastic moment capacities M_{el} and M_{pl} were calculated by multiplying the relevant measured section modulus by the average of the tensile and compressive 0.2% proof stress of the section.

Table 5: Key experimental results from major axis four-point bending tests.

Cross-section	Specimen ID	M_{el} (kNm)	M_{pl} (kNm)	M_u (kNm)	κ_{pl} (mm ⁻¹)	κ_u (mm ⁻¹)	R
I-102×68×5×5	B1	9.83	11.34	15.19	30.6×10 ⁻⁶	0.38×10 ⁻³	>11.4
I-160×82×10×12	B5	49.78	59.10	73.04	22.7×10 ⁻⁶	0.44×10 ⁻³	>18.4
I-50×50×4×4	B9	2.89	3.35	4.46*	69.4×10 ⁻⁶	1.15×10 ⁻³	>15.6
I-150×75×7×10	B17	32.95	38.42	46.57	22.0×10 ⁻⁶	0.27×10 ⁻³	>11.3

* M_u affected by premature failure of lateral restraints.

Table 6: Key experimental results from major axis three-point bending tests.

Cross-section	Specimen ID	M_{el} (kNm)	M_{pl} (kNm)	M_u (kNm)	θ_{pl} (rad)	θ_u (rad)	R
I-102×68×5×5	B2	9.67	11.15	17.47	0.025	0.304	>11.3
I-160×82×10×12	B6	49.45	58.75	85.77	0.019	0.300	>14.5
I-50×50×4×4	B10	2.95	3.42	>5.36	0.031	0.485	>14.3
I-150×75×7×10	B18	33.17	3.42	55.75*	0.016	0.214	>12.0

* M_u affected by premature failure of lateral restraints.

The mid-span moment-deflection curves obtained from the four-point and three point major axis bending tests are shown in Figures 7 and 8, respectively, while the normalised mid-span moment-curvature curves from the four-point bending tests and mid-span moment-rotation curves from the three-point bending tests are shown in Figures 9 and 10, respectively. For four-point bending, κ is the curvature in the uniform bending region, which can be derived from the deflection of the beam, thus:

$$\kappa = \frac{8(D_{\text{mid-span}} - D_{\text{average}})}{4(D_{\text{mid-span}} - D_{\text{average}})^2 + L_m^2} \quad (1)$$

where $D_{\text{mid-span}}$ is the vertical deflection at mid-span, D_{average} is the average of the readings of the two string potentiometers at the loading points and L_m is the distance between the two loading points. Additionally,

κ_u is the total curvature at the plastic hinge when the moment-curvature curve falls back below M_{pl} , and κ_{pl} is the elastic curvature corresponding to M_{pl} on the ascending branch (defined as $\kappa_{pl} = M_{pl}/EI$, where I is the second moment of area of the cross-section). For the case of three-point bending, θ is the rotation at mid-span, taken as the sum of the two end rotations, θ_u is the total rotation at mid-span when the moment-rotation curve falls back below M_{pl} as obtained from the test results and θ_{pl} is the elastic component of the rotation when M_{pl} is reached on the ascending branch (defined as $\theta_{pl} = M_{pl}L/2EI$). The rotation capacity R is calculated using Equations (2) and (3) for four-point and three-point bending respectively:

$$R = \frac{\kappa_u}{\kappa_{pl}} - 1 \quad (2)$$

$$R = \frac{\theta_u}{\theta_{pl}} - 1 \quad (3)$$

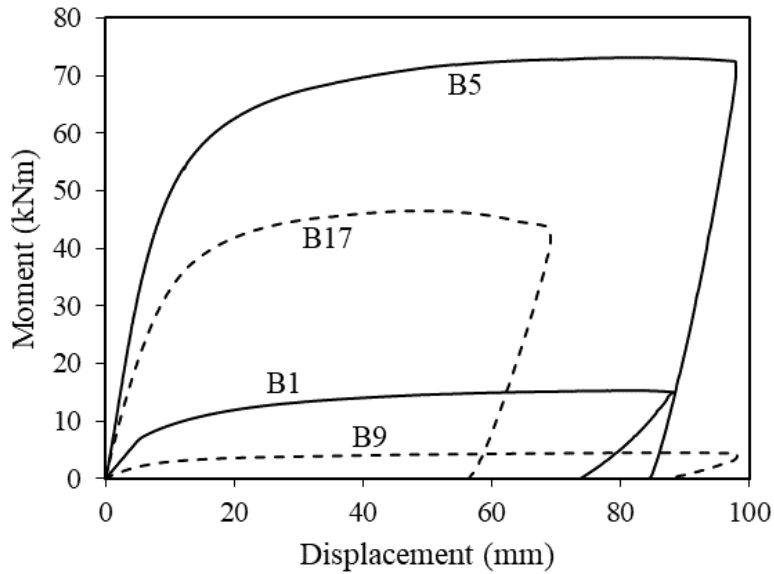


Figure 7: Mid-span moment-deflection curves from major axis four-point bending tests.

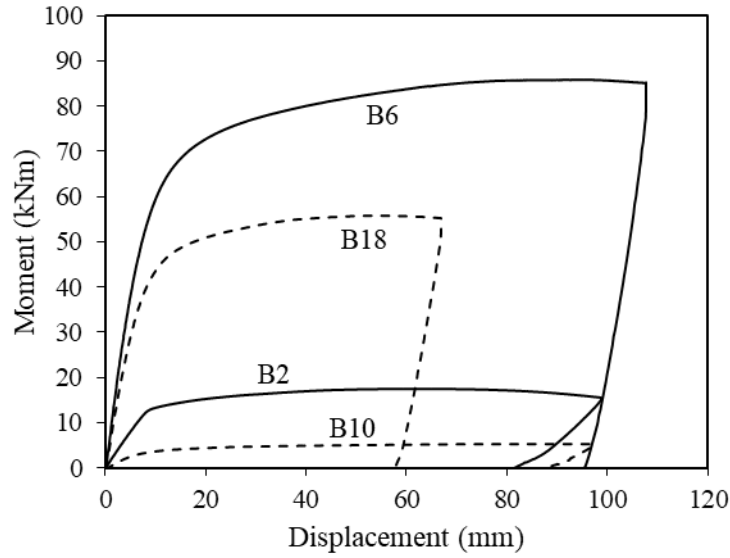


Figure 8: Mid-span moment-deflection curves from major axis three-point bending tests.

In some tests, the peak moment capacity was not reached due to physical limitations in the experimental setup -- in these instances, the maximum moments reached during the tests are recorded in Tables 5 and 6, preceded by a ‘>’ symbol. In tests B9 and B18, premature failure of the lateral restraints prevented the attainment of the full in-plane bending capacity. Similarly, in all tests, the moment did not fall back below M_{pl} and hence the rotation capacity is presented in Tables 5 and 6 as the maximum recorded values, preceded by a ‘>’ symbol. A typical failure mode for the beams in bending about the major axis is shown in Figure 11; the beams failed predominantly in-plane, experiencing a combination of inelasticity and local buckling.

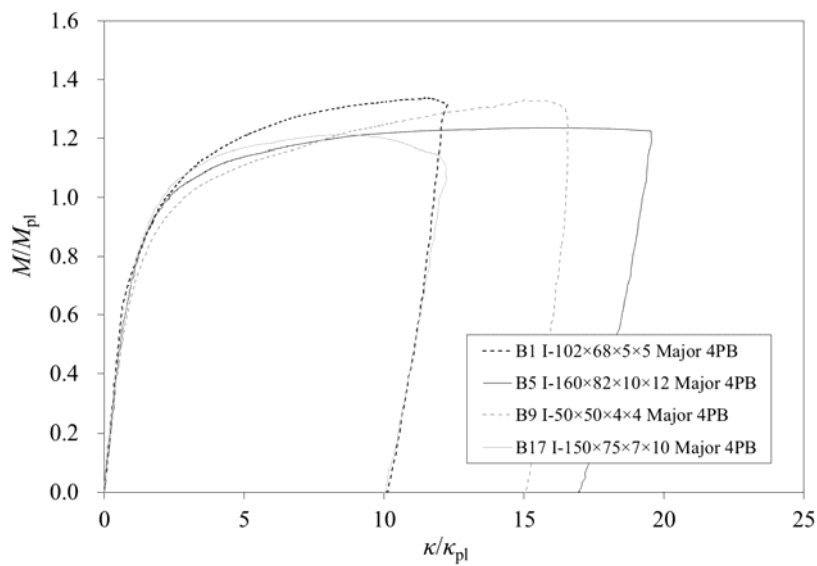


Figure 9: Normalised moment-curvature curves from major axis four-point bending tests.

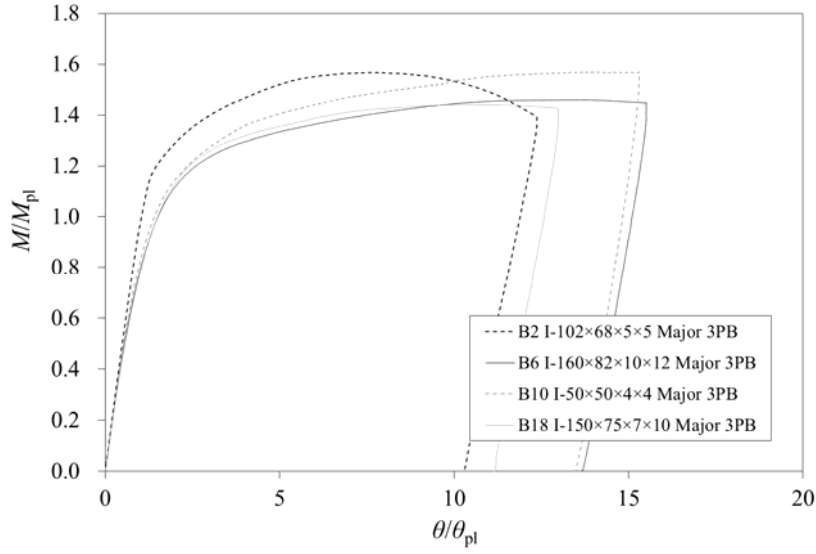


Figure 10: Normalised moment-rotation curves from major axis three-point bending tests.

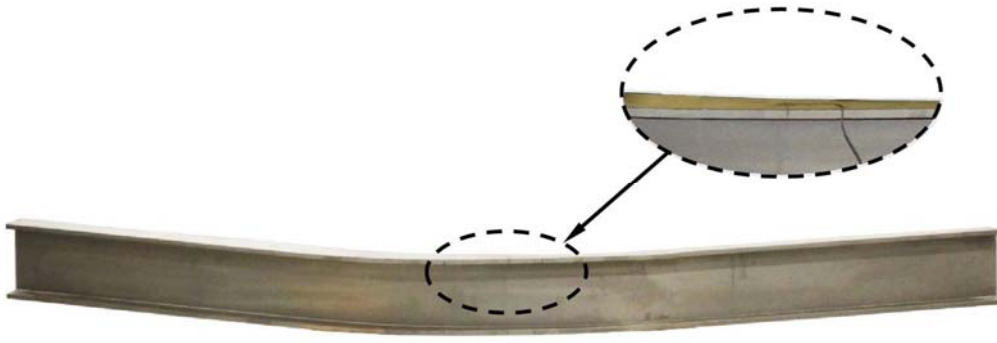


Figure 11: Typical failure mode for major axis bending, shown for specimen I-150×75×7×10 (B17).

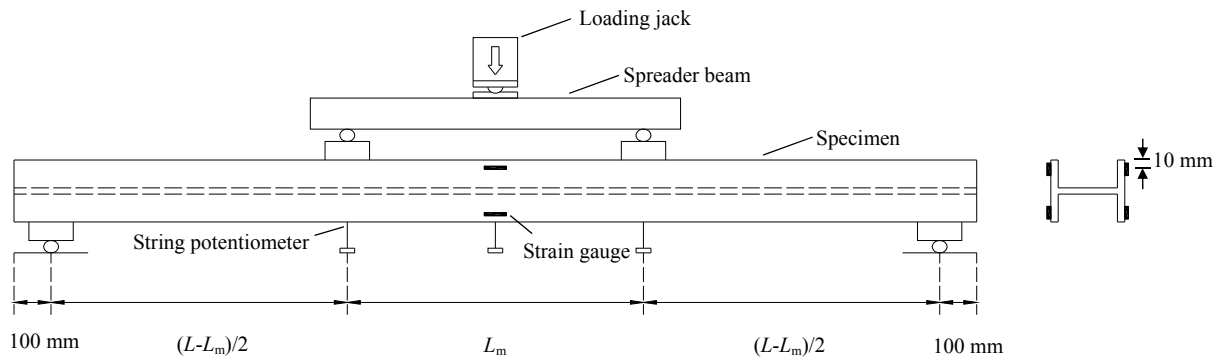
3.2. Minor axis bending tests

Twelve minor axis bending tests were carried out on the laser-welded I-sections following the same general approach as described in Section 3.1. Four strain gauges were attached to each specimen, two on the compressive side and two on the tensile side, all positioned at 10 mm from the flange tips. The setup for the minor axis four-point and three-point bending tests are shown in Figures 12 and 13, respectively. As for the major axis tests, load was applied through 40 mm wide and 8 mm thick steel bearing plates, and wooden blocks were placed between the flanges of the I-sections at the locations of the applied loads and the end supports to prevent local web bearing failure. The geometry and local imperfections of the test specimens were measured prior to testing and are reported in Table 7. All tests were carried out using

an INSTRON 750 kN testing machine and displacement controlled at a rate of 3.0 mm/min, with the exception of specimen B11, which was tested at a rate of 1.5 mm/min. All measured values were recorded using the DATASCAN data acquisition system.

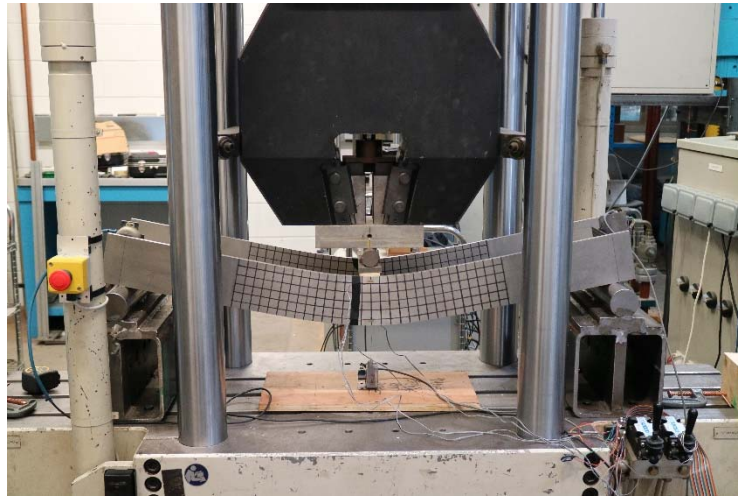


(a) Experimental setup

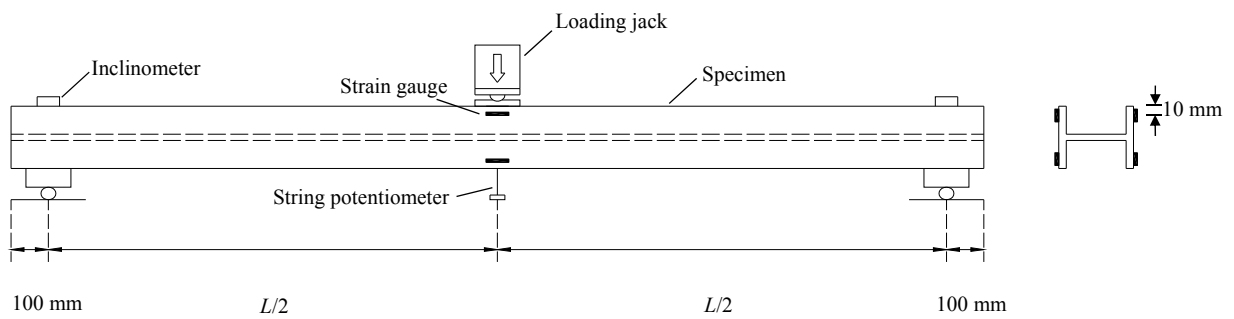


(b) Schematic diagram

Figure 12: Experimental setup for the minor axis four-point bending tests.



(a) Experimental setup



(b) Schematic diagram

Figure 13: Experimental setup for the minor axis three-point bending tests

Table 7: Measured dimensions of specimens in bending about the minor axis.

Cross-section	Specimen ID	Span L (mm)	3/4PB	L_m (mm)	h (mm)	b (mm)	t_w (mm)	t_f (mm)	w_0 (mm)
I-102×68×5×5	B3	1100	4	400	101.24	67.99	5.07	5.05	0.22
I-102×68×5×5	B4	1100	3	-	102.00	67.50	4.80	4.80	0.22
I-160×82×10×12	B7	1100	4	400	160.63	82.26	10.04	11.96	0.16
I-160×82×10×12	B8	1100	3	-	160.37	82.58	9.83	11.78	0.16
I-50×50×4×4	B11	900	4	400	50.21	50.61	4.04	4.10	0.23
I-50×50×4×4	B12	1100	3	-	50.55	50.47	3.98	3.96	0.23
I-203×133×6×8	B13	1700	4	600	205.21	133.92	5.97	7.91	0.11
I-203×133×6×8	B14	1700	3	-	205.30	133.97	6.01	7.88	0.11
I-220×110×6×9	B15	1500	4	500	219.36	110.51	6.09	8.97	0.15
I-220×110×6×9	B16	1100	3	-	219.46	110.47	6.10	8.94	0.15
I-150×75×7×10	B19	1100	4	400	150.77	75.90	6.96	9.90	0.16
I-150×75×7×10	B20	1100	3	-	149.93	75.80	7.01	9.97	0.16

The key experimental results are summarised in Tables 8 and 9 for the minor axis four-point bending and three-point bending tests, respectively. The mid-span moment-deflection curves obtained from the four-point and three-point minor axis bending tests are shown in Figures 14 and 15, respectively, while the normalised mid-span moment-curvature curves from the four-point bending tests and mid-span moment-rotation curves from the three-point bending tests are shown in Figures 16 and 17, respectively. A typical failure mode for a specimen bending about the minor axis, featuring in-plane bending with inelasticity and local buckling is shown in Figure 18.

Table 8: Key experimental results from minor axis four-point bending tests.

Cross-section	Specimen	M_{el}	M_{pl}	M_u	κ_{pl}	κ_u	R
	ID	(kNm)	(kNm)	(kNm)	(mm ⁻¹)	(mm ⁻¹)	
I-102×68×5×5	B3	2.00	3.14	4.44	62.7×10 ⁻⁶	1.60×10 ⁻³	>24.5
I-160×82×10×12	B7	8.40	13.53	17.85	60.0×10 ⁻⁶	1.25×10 ⁻³	>19.8
I-50×50×4×4	B11	1.05	1.63	2.06	92.4×10 ⁻⁶	1.70×10 ⁻³	>17.4
I-203×133×6×8	B13	13.52	20.74	27.69	32.7×10 ⁻⁶	0.80×10 ⁻³	>23.5
I-220×110×6×9	B15	10.78	16.70	>20.93	42.0×10 ⁻⁶	0.59×10 ⁻³	>13.0
I-150×75×7×10	B19	5.42	8.55	11.91	59.0×10 ⁻⁶	1.32×10 ⁻³	>21.4

Table 9: Key experimental results from minor axis three-point bending tests.

Cross-section	Specimen	M_{el}	M_{pl}	M_u	θ_{pl}	θ_u	R
	ID	(kNm)	(kNm)	(kNm)	(rad)	(rad)	
I-102×68×5×5	B4	1.88	2.94	5.91	0.035	0.860	>23.8
I-160×82×10×12	B8	8.33	13.39	24.65	0.033	0.898	>26.4
I-50×50×4×4	B12	1.01	1.57	>2.50	0.051	0.585	>10.5
I-203×133×6×8	B14	13.48	20.68	30.48	0.028	0.380	>12.7
I-220×110×6×9	B16	10.74	16.64	28.05	0.023	0.420	>17.2
I-150×75×7×10	B20	5.45	8.59	>15.59	0.032	0.875	>25.9

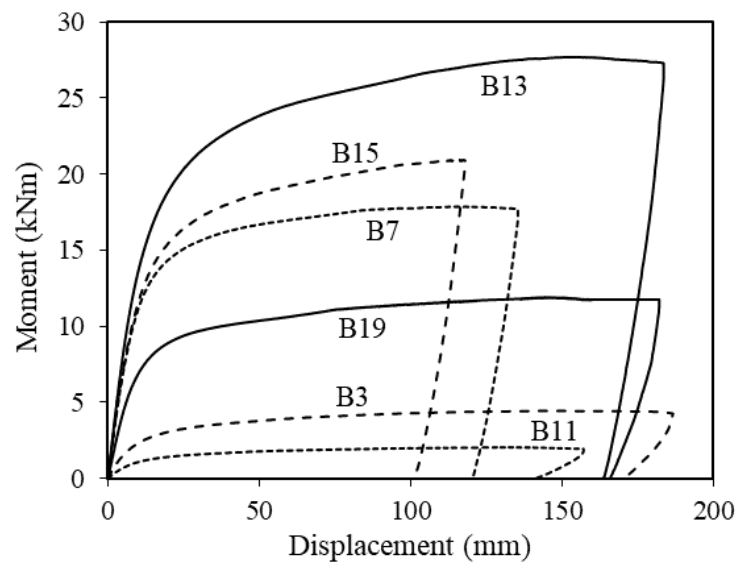


Figure 14: Mid-span moment-deflection curves from minor axis four-point bending tests.

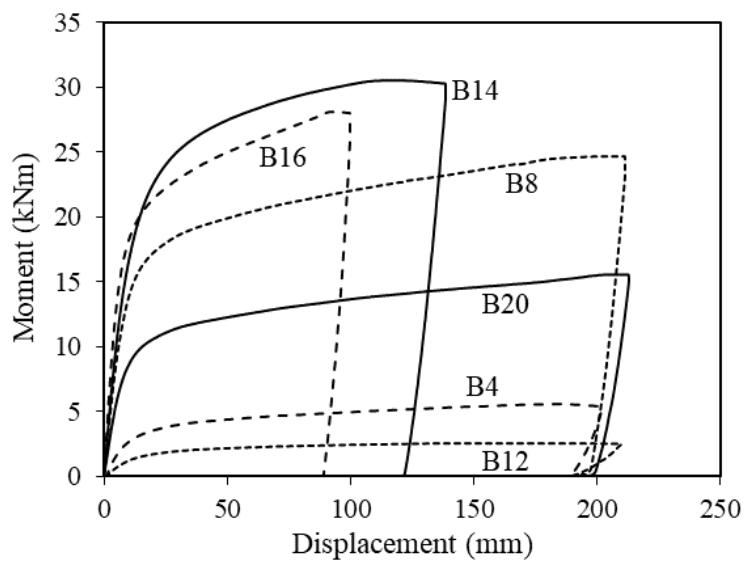


Figure 15: Mid-span moment-deflection curves from minor axis three-point bending tests.

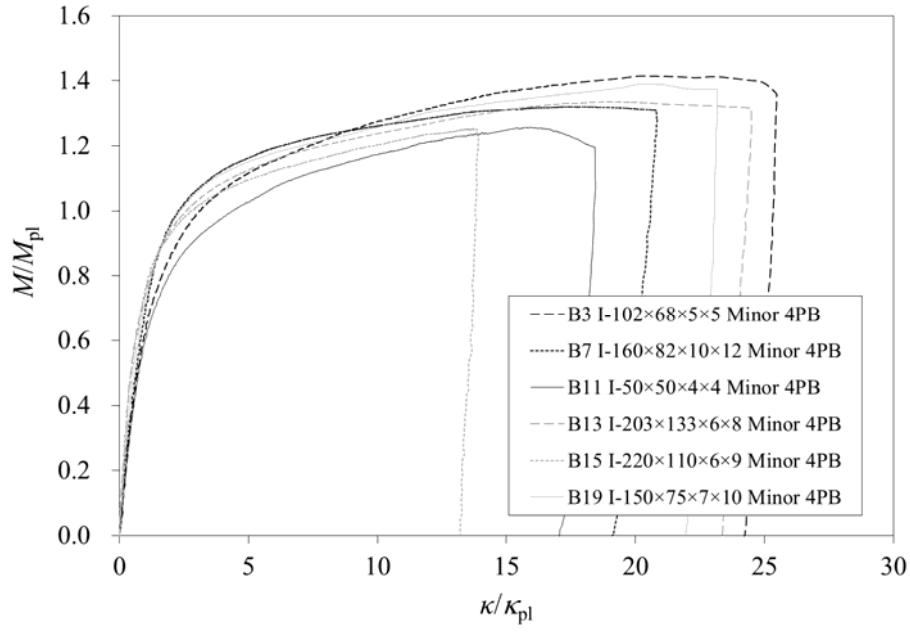


Figure 16: Normalised moment-curvature curves from minor axis four-point bending tests.

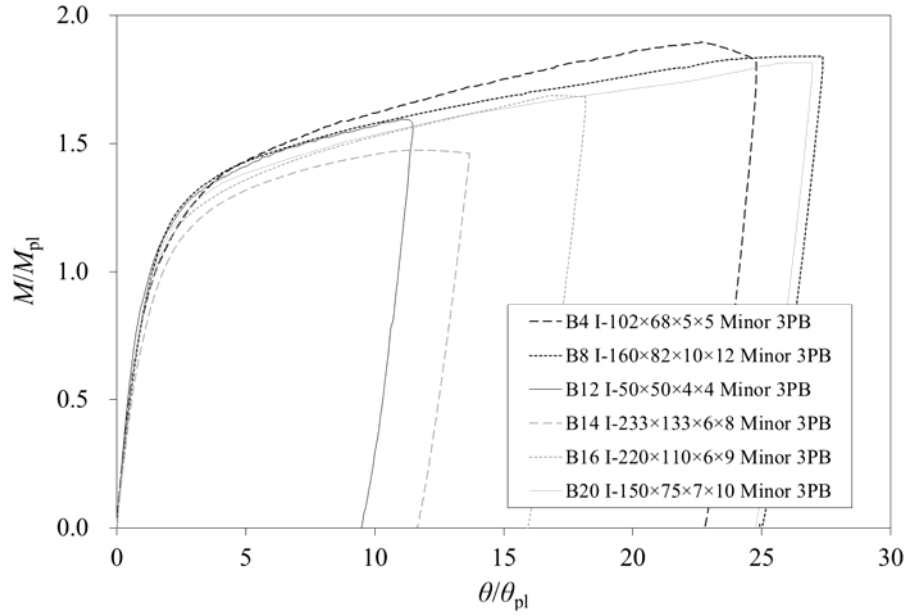


Figure 17: Normalised moment-rotation curves from minor axis three-point bending tests.



Figure 18: Typical failure mode for minor axis bending, shown for specimen I-220×110×6×10 (B16).

4. Numerical modelling

4.1. Introduction

A numerical modelling study of the laser-welded stainless steel beams was also conducted, using the general-purpose finite element analysis package ABAQUS [19]. The experimental results were used to validate the FE models, which were subsequently employed in a parametric study to assess the influence of the key parameters on the structural response of laser-welded stainless steel I-sections.

4.2. Modelling assumptions

The S4R element [19], which is a four-noded doubly curved general-purpose shell element, was adopted in the developed FE models, where S denotes stress/displacement shell elements and R denotes reduced integration. The element S4R is suitable for the modelling of both thick- and thin-walled structures [19] and has been successfully employed in many similar applications [10, 20, 21].

The measured tensile material properties (see Section 2.1) obtained from the tensile coupon tests were assigned to the lower half (below the neutral axis) of the beam models while the measured compressive material properties obtained from the stub column tests (see Section 2.2) were assigned to the upper half (above the neutral axis) of the models for the validation study. The measured engineering stress-strain response ($\sigma_{\text{nom}} - \epsilon_{\text{nom}}$) was first represented by the two stage Ramberg-Osgood model described in [4] to provide a smooth curve, then converted into true stress-log plastic strain ($\sigma_{\text{true}} - \epsilon_{\text{ln}}^{\text{pl}}$) using Equations (4)

and (5) and finally input into ABAQUS in multi-linear form with 50 intervals. For compressive material properties, either the engineering strain values ε_{nom} should be taken as negative or, if using absolute values, the $(1+\varepsilon_{\text{nom}})$ term should be changed to $(1-\varepsilon_{\text{nom}})$.

$$\sigma_{\text{true}} = \sigma_{\text{nom}} (1 + \varepsilon_{\text{nom}}) \quad (4)$$

$$\varepsilon_{\text{ln}}^{\text{pl}} = \ln(1 + \varepsilon_{\text{nom}}) - \frac{\sigma_{\text{true}}}{E} \quad (5)$$

For both the four-point and three-point bending FE models, vertical displacement was restrained at the end supports of the beams, while lateral movement was restrained at the end supports, the loading points and at the position of the lateral restraints, following the setup employed in the tests. In the parametric studies, lateral displacement was restrained at the end supports and loading points only, since this was found to be sufficient to prevent lateral-torsional buckling. Longitudinal displacement was prevented at the mid-span of the beams. Tie constraints were employed such that the cross-sections of the beams remained undeformed at the loading points and support points.

4.3. Initial geometric imperfections and residual stresses

Initial local geometric imperfections were introduced into the FE models in the form of the lowest elastic buckling mode shape obtained from a prior elastic buckling analysis. This approach has been widely used in previous studies [10, 22-24]. The elastic buckling mode shapes were scaled with four imperfection amplitudes: (1) the measured imperfection amplitude w_0 , as reported in Tables 4 and 7, (2) $t_f/100$, (3) $t_f/1000$ and (4) $w_{D\&W}$, the modified Dawson and Walker imperfection amplitude [25, 26] as given by Equation (6).

$$w_{D\&W} = 0.023 \left(\frac{f_{yc}}{\sigma_{cr,\min}} \right) t \quad (6)$$

where f_{yc} is the compressive 0.2% proof stress and $\sigma_{cr,\min}$ is the elastic critical buckling stress of the most slender constituent element in the cross-section. The residual stress pattern for laser-welded stainless steel I-sections developed in [1] and summarised in Section 2.4 was also introduced into the numerical models. A preliminary load step to allow equilibration of the residual stresses was performed before the external loads were applied to the beam.

4.4. Validation of the numerical models

Having incorporated the initial geometric imperfections and residual stress into the FE models, the external loads were applied in a geometrically and materially nonlinear analysis, solved using the Riks method [19]. The measured geometrical and material properties of the test specimens were employed in the FE analysis. The general load-deformation responses, failure modes and key results from the numerical models were compared to those obtained from the experiments presented in Section 3 to assess accuracy of the numerical models and are discussed in the following sub-sections.

4.4.1. Major axis bending

The ultimate moments M_u from the numerical models in bending about major axis, with the four considered imperfection amplitudes, are compared to the corresponding experimental results in Table 10. It may be observed that the ultimate moments M_u obtained from the numerical models generally exhibit relative insensitivity to changes in the imperfection amplitude and generally good agreement is achieved with all considered imperfection amplitudes. It should be noted that for the test specimen that did not reach its peak moment (B10), the maximum moment achieved is compared with the corresponding FE result at the same level of deformation. Premature failure of the lateral restraints occurred in tests B9 and B18, therefore the results were excluded from the calculation of the mean and COV of the FE M_u /Test M_u ratio.

Table 10: Comparison of ultimate moments from major bending tests and FE simulations for varying imperfection amplitudes

Cross-section	Specimen ID	3/4PB	FE M_u /Test M_u			
			Measured amplitude	$t_f/100$	$t_f/10$	$\omega_{D\&W}$
I-102×68×5×5	B1	4	1.01	1.02	1.00	1.01
I-102×68×5×5	B2	3	0.97	1.01	0.94	1.03
I-160×82×10×12	B5	4	1.00	1.00	1.00	1.00
I-160×82×10×12	B6	3	1.02	1.02	1.00	1.02
I-50×50×4×4	B9	4	1.11*	1.24*	1.08*	1.33*
I-50×50×4×4	B10	3	0.96	1.00	0.95	1.01
I-150×75×7×10	B17	4	1.04	1.04	1.03	1.04
I-150×75×7×10	B18	3	1.11*	1.12*	1.07*	1.13*
Mean**			1.00	1.02	0.99	1.02
COV**			0.03	0.01	0.03	0.01

*Values excluded from determination of mean FE M_u /Test M_u ratio and COV due to premature failure of lateral restraints in tests.

**Mean/COV values calculated without specimens B9 and B18 due to premature failure of lateral restraints in tests.

Typical comparisons of normalised moment-curvature curves (for 4PB) and the normalised moment-rotation curves (for 3PB) from the experiments and corresponding FE models (using measured imperfection amplitudes) are shown in Figures 19 and 20, where the solid lines represent the test curves while the dashed lines are the numerical counterparts. Both the initial stiffness and the general shape of the experimental curves are accurately captured by the FE simulations. A comparison of a typical failure mode between the laboratory test and the FE model is shown in Figure 21, where it can be seen that the lateral supports effectively prevented global buckling and the beams therefore failed predominantly in-plane, experiencing a combination of inelasticity and local buckling. Overall, the numerical models were found to be capable of capturing the general load-deformation responses, failure modes and key results obtained in the experiments accurately.

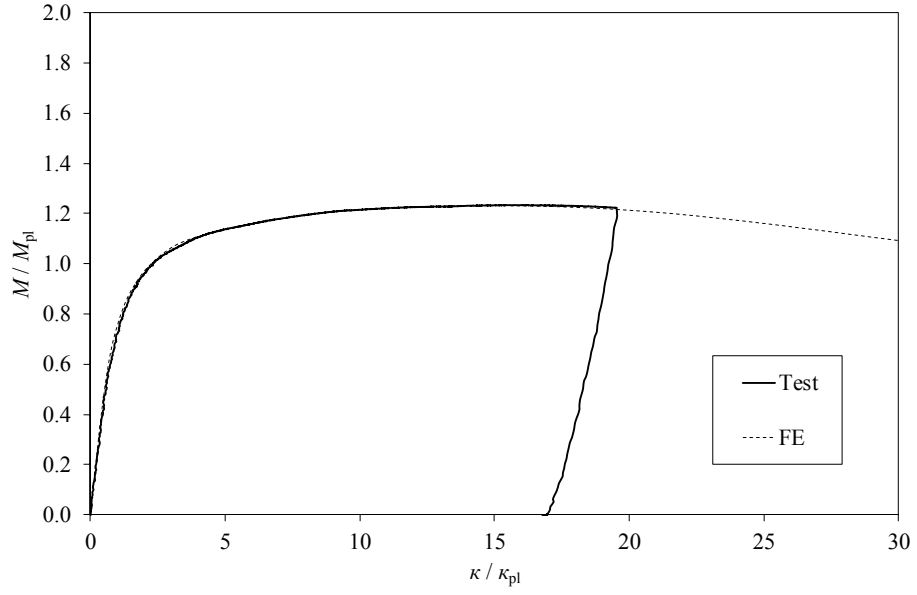


Figure 19: Experimental and numerical normalised moment-curvature curves for specimen I-160×82×10×12 (B5) in four-point major axis bending.

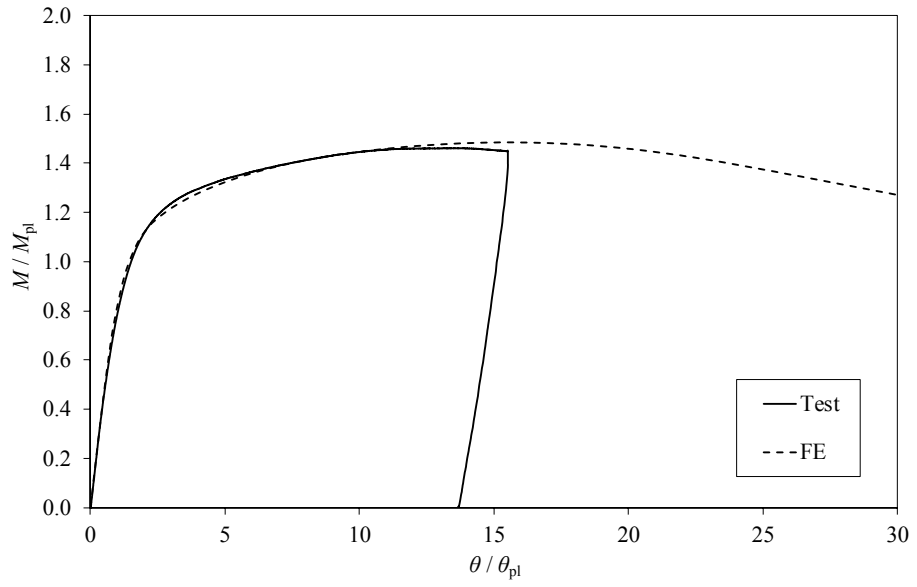


Figure 20: Experimental and numerical normalised moment-rotation curves for specimen I-160×82×10×12 (B6) in three-point major axis bending.

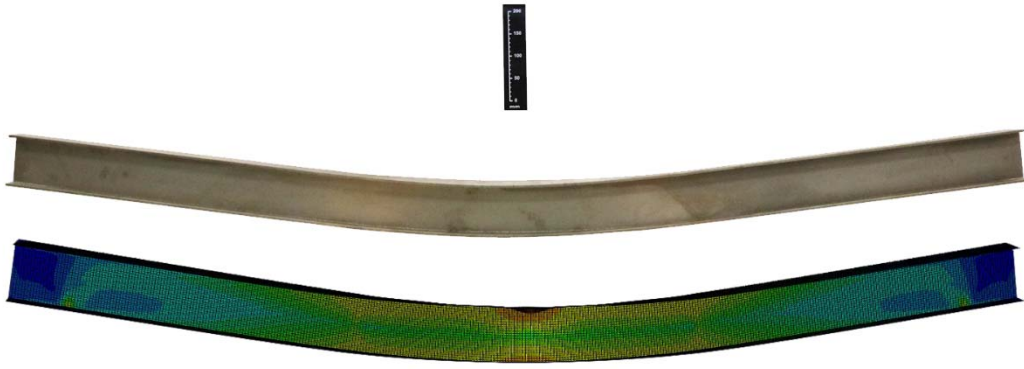


Figure 21: Typical test and FE failure modes for major axis bending, shown for specimen I-102×68×5×5 (B1).

4.4.2. Minor axis bending

Similar comparisons as for major axis bending are made in this subsection for minor axis bending. The ultimate test and FE capacities are compared in Table 11 for the four considered imperfection amplitudes, typical test and FE (with measured imperfection amplitudes) normalised moment-curvature (4PB) and moment-rotation (3PB) curves are compared in Figures 22 and 23, respectively, while typical test and FE failure modes are shown in Figure 24.

As for the case of major axis bending, the best agreement between the test and FE results for minor axis bending is achieved when the measured geometric imperfection amplitude is adopted in the numerical simulations, but good agreement is achieved for all considered imperfection amplitudes. The simulated moment-deformation curves and failure modes also match well with those from the tests.

Table 11: Comparison of ultimate moments from minor bending tests and FE simulations for varying imperfection amplitudes.

Cross-section	Specimen ID	3/4PB	FE M_u /Test M_u			
			Measured amplitude	$t_f/100$	$t_f/10$	$\omega_{D\&W}$
I-102×68×5×5	B3	4	1.05	1.05	1.05	1.05
I-102×68×5×5	B4	3	0.97	0.97	0.94	0.98
I-160×82×10×12	B7	4	0.96	0.96	0.98	0.98
I-160×82×10×12	B8	3	0.98	0.98	0.97	0.98
I-50×50×4×4	B11	4	1.11	1.14	1.14	1.14
I-50×50×4×4	B12	3	1.01	1.02	0.98	1.02
I-203×133×6×8	B13	4	1.00	0.99	0.96	1.00
I-203×133×6×8	B14	3	0.98	0.99	0.96	0.99
I-220×110×6×9	B15	4	1.01	1.01	1.01	1.01
I-220×110×6×9	B16	3	0.94	0.94	0.93	0.96
I-150×75×7×10	B19	4	1.00	1.00	1.00	1.00
I-150×75×7×10	B20	3	1.04	1.04	1.01	1.04
Mean **			1.00	1.01	0.99	1.01
COV**			0.04	0.05	0.06	0.05

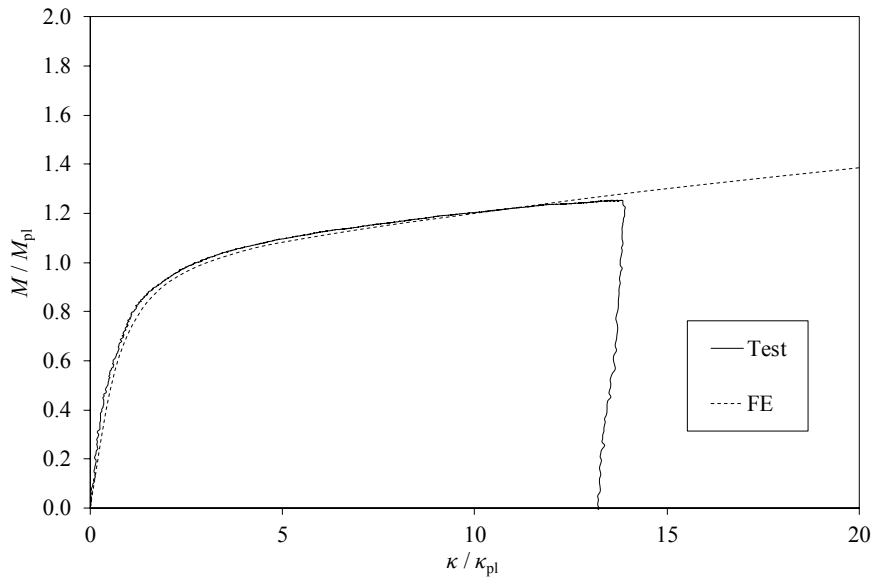


Figure 22: Experimental and numerical normalised moment-curvature curves for specimen I-220×110×6×9 (B15) in four-point minor axis bending.

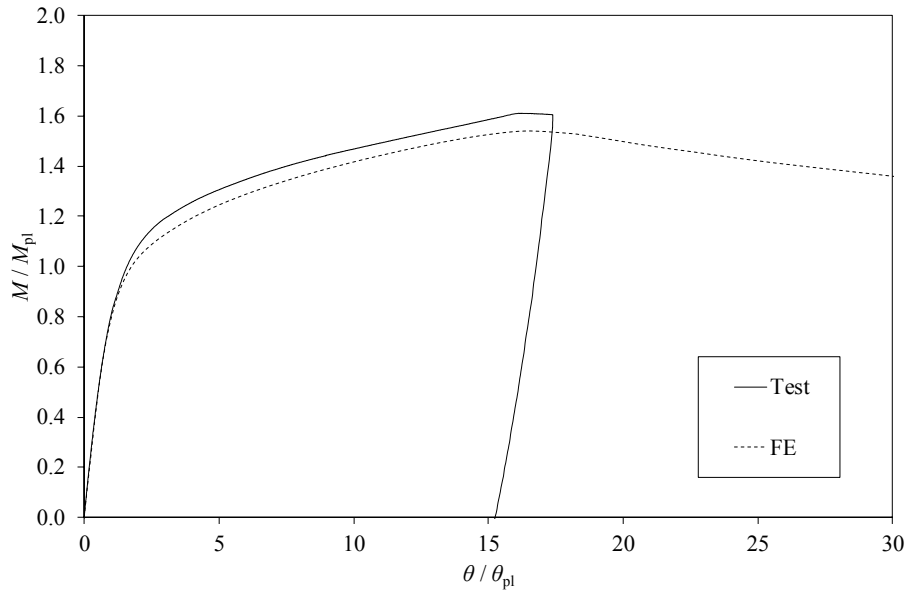


Figure 23: Experimental and numerical normalised moment-rotation curves for specimen I-220×110×6×9 (B16) in three-point minor axis bending.

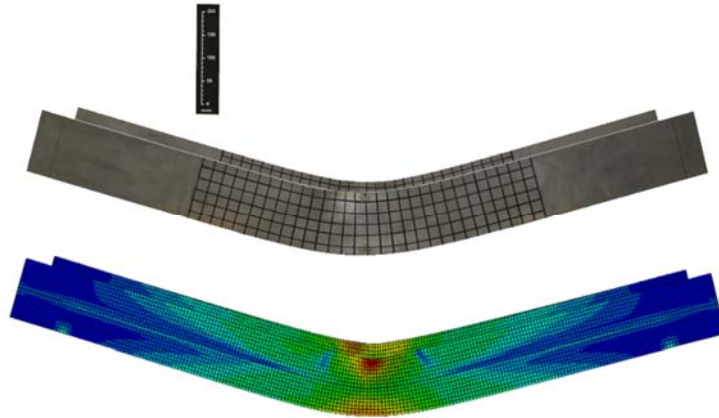


Figure 24: Typical test and FE failure modes for minor axis bending, shown for specimen I-220×110×6×10 (B16).

4.5. Parametric study

After the numerical models were validated, a parametric study was carried out. A wide range of cross-section slendernesses and aspect ratios were modelled to investigate the general structural behaviour of laser-welded stainless steel I-sections in bending, and in order to assess the design provisions of EN 1993-1-4 and the continuous strength method (CSM). In total, 108 parametric results were generated covering

bending about both the major and minor axes and four-point and three-point bending configurations. The span of the beams (1500 mm) and section height (100 mm) were kept constant, while the flange width was varied to generate three aspect ratios h/b (1.5, 2.0 and 3.0). In the four-point bending arrangement, the loads were applied at third points along the beam span, creating a central uniform bending moment region of 500 mm in length. The thicknesses of the flanges and web were varied to cover a range of slendernesses while, at the same time, retaining similar values for the flange slenderness $\bar{\lambda}_{p,f}$, and the web slenderness $\bar{\lambda}_{p,w}$, which are defined, in accordance with [18], by Equations (7) and (8).

$$\bar{\lambda}_{p,f} = \sqrt{f_y / \sigma_{cr,f}} \quad (7)$$

$$\bar{\lambda}_{p,w} = \sqrt{f_y / \sigma_{cr,w}} \quad (8)$$

where $\sigma_{cr,f}$ and $\sigma_{cr,w}$ are the elastic buckling stresses of the flange and web, considered in isolation. The tensile and compressive material properties of specimen I-102×68×5×5 were adopted throughout the parametric studies. Initial geometric imperfections of the form of the lowest elastic buckling mode shape and with the amplitude given by the modified Dawson and Walker model were included, together with the residual stress pattern from Section 2-4, in all simulations. The results are summarised in Section 5 and employed to assess the applicability of the considered design methods.

5. Analysis of results and design recommendations

The results generated from the parametric study, together with those obtained from the tests, are utilised in this section to assess the applicability of the current cross-section classification limits in Eurocode 3: Part 1.4 [18] and AISC Design Guide 27 [27] and the design predictions of the continuous strength method (CSM) [28] to laser-welded I-sections in bending. The comparisons are made based on the measured (or modelled) material properties (average of tension and compression) and geometric properties, and with all partial safety factors set equal to unity.

5.1. EN 1993-1-4 slenderness limits

The current European design code for stainless steel structures [18] uses the concept of cross-section classification for the treatment of local buckling. The classification of the cross-section is determined by its least favourable element. The plate elements from the cross-section are separated into four classes

based on the material properties, element support conditions, applied stress gradient and the width-to-thickness ratios $c/t\varepsilon$, where c is the flat element width, t is the element thickness and $\varepsilon=[(235/f_{yc})(E_c/210000)]^{0.5}$, as defined in EN 1993-1-4. The classification of the plates is determined by slenderness limits in EN 1993-1-4 [18], which has been recently updated in accordance with the limits proposed by [29]. Since the cross-sections in the parametric studies were chosen such that the flange slenderness $\bar{\lambda}_{p,f}$ and the web slenderness $\bar{\lambda}_{p,w}$ were similar, the numerical results were used to assess the slenderness limits for both outstand and internal elements. For some numerical simulations in bending about the minor axis (generally the stockier cross-sections in either four-point or three-point bending), the moment-deformation curve showed a continuously rising response with no peak; in such instances, the moment at the minimum slope of the moment-curvature (for four-bending) or moment rotation (for three-point bending) curves was taken as the ultimate moment M_u .

5.1.1. Outstand elements

The test and numerical results for the cross-sections in major axis bending were used to assess the Class 3 slenderness limit for outstand elements under compression, as shown in Figure 25. In the figure, the ultimate bending moment M_u from the tests and numerical models have been normalised with respect to the elastic moment capacity M_{el} and plotted against the element width-to-thickness ratio $c_f=t_f\varepsilon$ of the outstand flanges, taken as $c_f=(b-t_w)/2$ for the test specimens and $c_f=b/2$ for the FE models (due to the shell elements acting on the section centrelines). The Class 3 slenderness limit for outstand elements under a compressive stress gradient is assessed using the test and numerical results for the cross-sections in minor axis bending in Figure 26, where the influence of the stress gradient is considered through the plate buckling coefficient k_σ ($=0.57$ for a triangular stress distribution in an outstand flange [30]), with the corresponding slenderness parameter becoming $c_f / t_f \varepsilon \sqrt{k_\sigma}$. The comparisons of Figures 25 and 26 indicate that the capacity predictions are generally conservative and the current EN 1993-1-4 Class 3 limit can be safely applied to outstand elements in laser-welded sections.

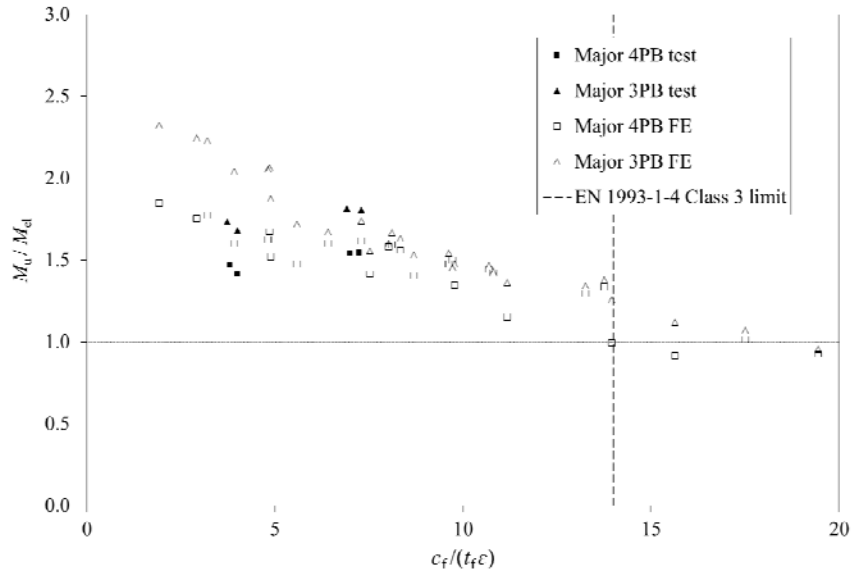


Figure 25: Assessment of Class 3 slenderness limit for outstand elements in compression.

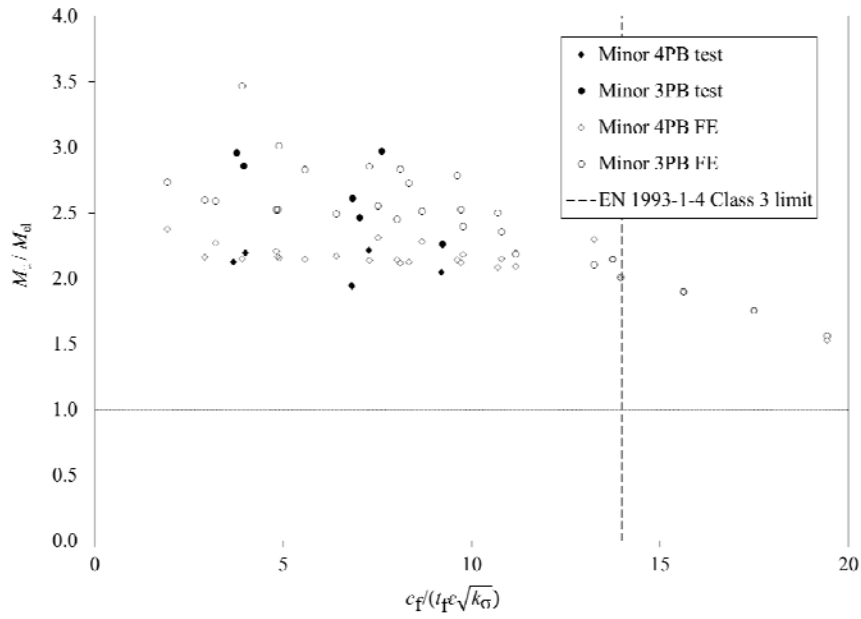


Figure 26: Assessment of Class 3 slenderness limit for outstand elements under a compressive stress gradient.

The ultimate moments M_u obtained from the tests and FE models are normalised by the plastic moment capacity M_{pl} and plotted against the slenderness parameter $\alpha c_f / t_f \epsilon$ in Figure 27, where α is the proportion of the outstand element in compression, assuming a plastic distribution of stresses. However, for bending

about either axis, the critical outstand elements are in pure compression and hence $\alpha=1$. The EN 1993-1-4 Class 2 slenderness limit is also shown in Figure 27 and may be seen to be safely applicable to laser-welded sections, although the capacity predictions are conservative.

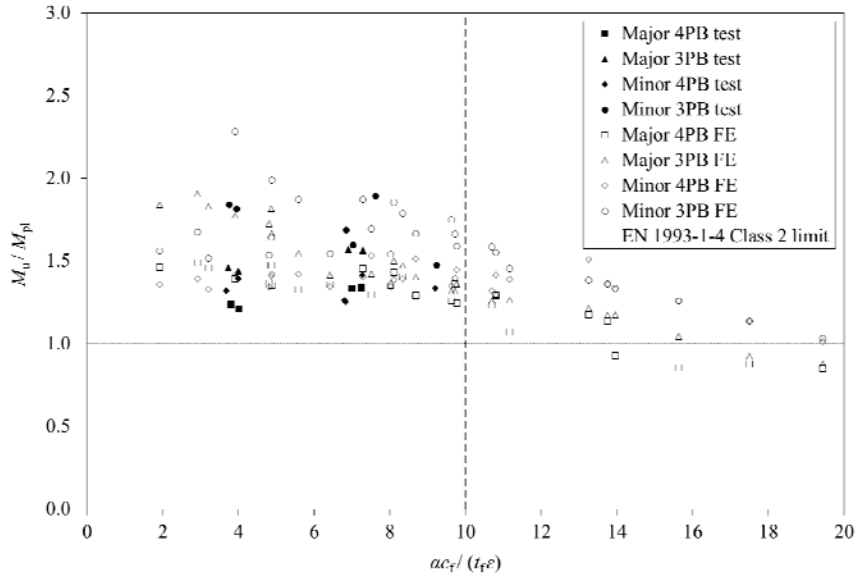


Figure 27: Assessment of Class 2 slenderness limit for outstand elements.

In Figure 28, the rotation capacities R from the tests and FE models are plotted against the slenderness parameter $ac_T / t_f \epsilon$. In a number of the tests and FE simulations, the full rotation capacity R was not reached since the moment did not drop below M_{pl} on the descending path. In these instances, the rotation capacity was taken to be the maximum achieved rotation and the presented values are therefore conservative. In the absence of a codified deformation capacity requirement for Class 1 stainless steel cross-sections, the equivalent carbon steel rotation capacity requirement of $R=3$ [31] has been used herein. The codified Class 1 slenderness limit in EN 1993-1-4 is also indicated in the figure. All results below this slenderness limit show rotation capacities significantly in excess of the required value of 3, and the limit may therefore be considered to be acceptable.

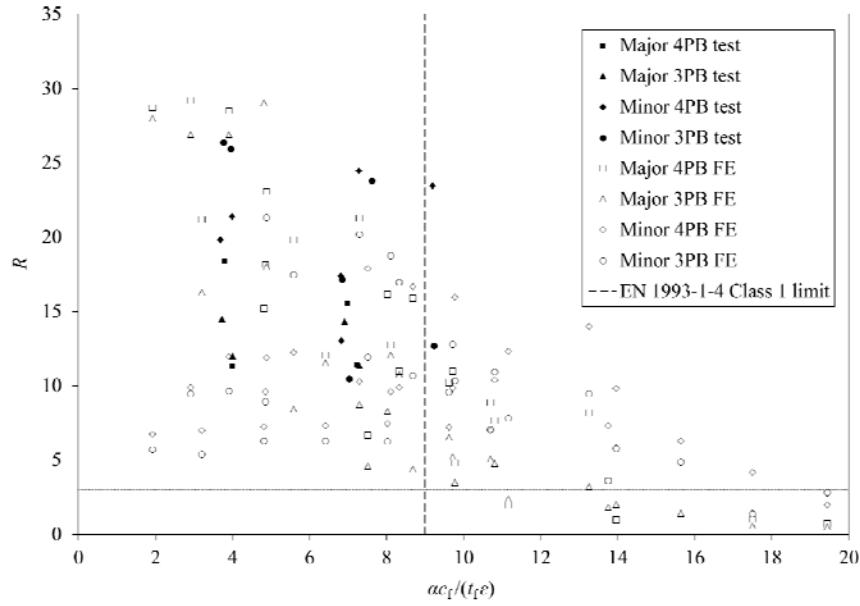


Figure 28: Assessment of Class 1 slenderness limit for outstand elements.

5.1.2. Internal elements

To assess the slenderness limits for internal elements, the results from the major axis bending tests and FE models were utilised. Under minor axis bending, the internal web element lies at the neutral axis and therefore has little bearing on the level of resistance or deformation capacity that can be achieved by the cross-section.

Similar to the assessment of the outstand element slenderness limits, the relevant response characteristics M_u/M_{el} , M_u/M_{pl} , and the rotation capacity R are plotted against the slenderness of the internal elements, $c_w/t_w \varepsilon$, where c_w is the flat width of the web (taken as $c_w = (h - 2t_f)/2$ for the test specimens and $c_w = (h - t_f)/2$ for the FE models) for the evaluation of the Class 3, 2 and 1 limits in Figures 29, 30 and 31, respectively. The same general conclusion is found for the internal elements: the slenderness limits in EN 1993-1-4 are safely applicable to laser-welded cross-sections, but the resistance predictions are rather conservative.

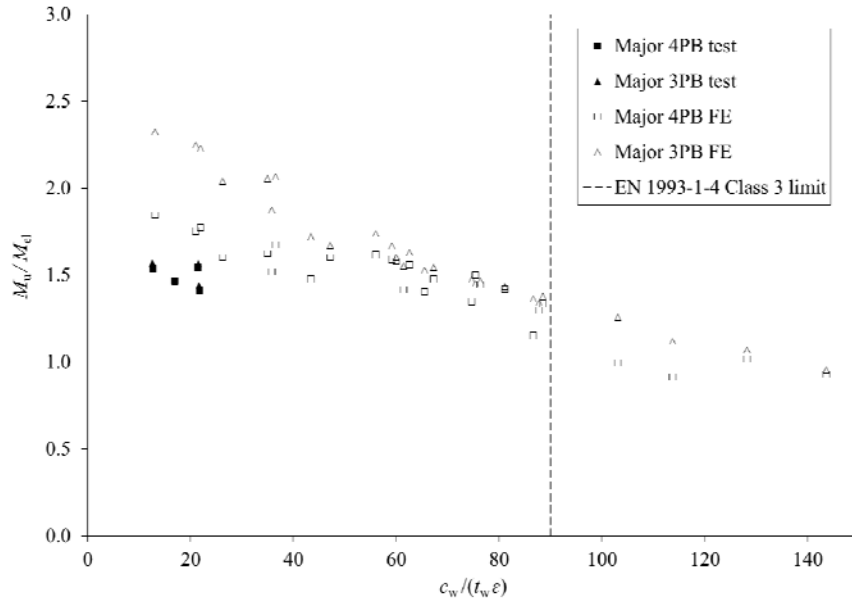


Figure 29: Assessment of Class 3 slenderness limit for internal elements.

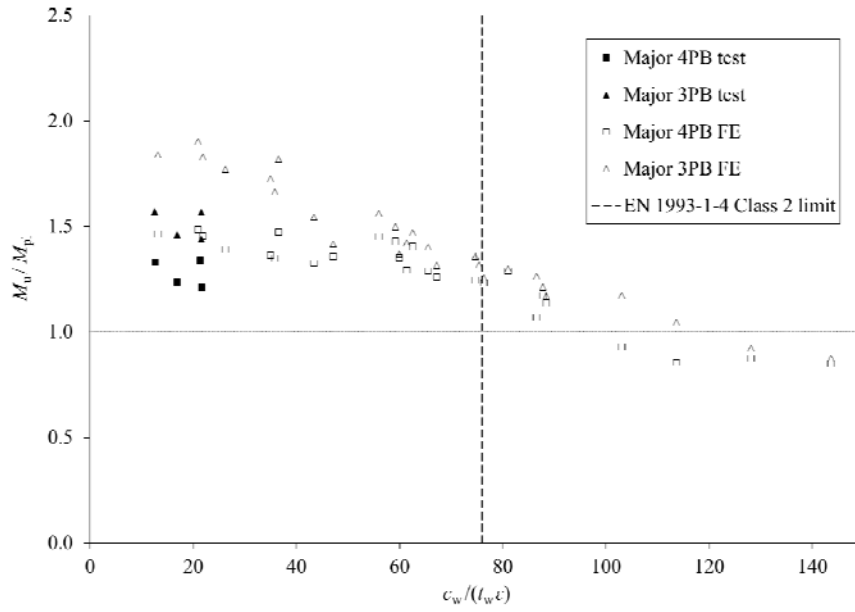


Figure 30: Assessment of Class 2 slenderness limit for internal elements.

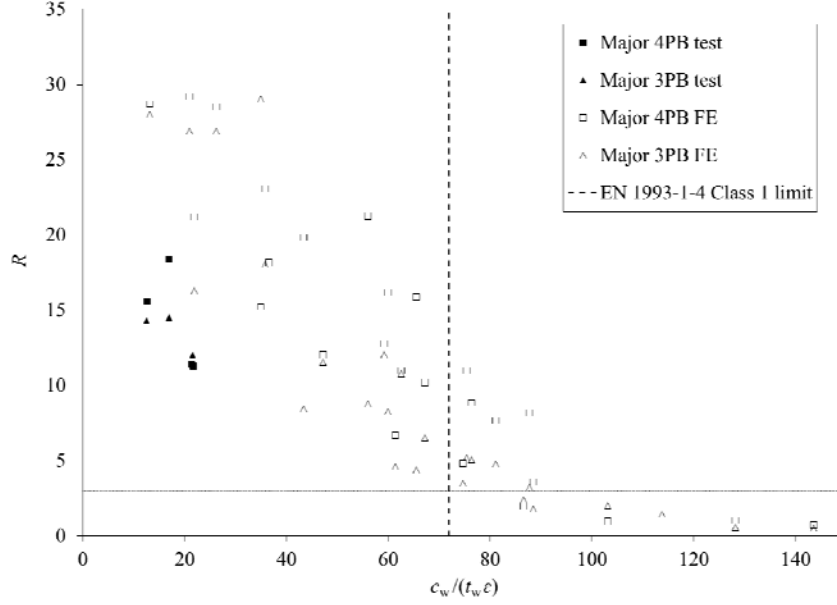


Figure 31: Assessment of Class 1 slenderness limit for internal elements.

5.2. AISC Design Guide 27

In AISC Design Guide 27 [27], I-sections are grouped into three categories: compact I-sections (corresponding to Class 1 and Class 2 cross-sections in EN 1993-1-4), non-compact I-sections (corresponding to Class 3 cross-sections in EN 1993-1-4) and slender I-sections (corresponding to Class 4 cross-sections in EN 1993-1-4). For the calculation of bending moment resistances, AISC Design Guide 27 adopts the plastic moment capacity for compact I-sections, considers partial plasticity in calculating bending moment resistances for non-compact I-sections, and employs an elastic local buckling moment approach to determine bending moment capacities for slender I-sections. A slight anomaly in the calculation of the bending moment resistance for I-sections with compact webs and non-compact flanges exists in the current version of the AISC design guide, which results in a discontinuity in the resistance function. This is resolved by replacing the current noncompact-slender limiting width-to-thickness ratio for outstand flanges of $0.47(E/f_y)^{0.5}$ with $1.13(k_c E/f_y)^{0.5}$ (where $k_c = 4/\sqrt{h/t_w}$, but with a lower limit of 0.35 and an upper limit of 0.76) for outstand flanges in I-sections under major axis bending (Case 9 in Table 3.2 of AISC Design Guide 27) and $0.99(E/f_y)^{0.5}$ for outstand flanges in I-sections under minor axis bending (Case 10 in Table 3.2 of AISC Design Guide 27); the revised AISC bending resistance functions are given by Equations (9) to (11) for I-sections under major axis bending, where $\lambda_{\text{AISC}} = b_f/(2t_f)$ is the local flange slenderness ratio,

$$M_{AISC} = M_{pl} = f_y W_{pl} \quad \text{for } \lambda_{AISC} \leq 0.33 \sqrt{\frac{E}{f_y}} \quad (9)$$

$$M_{AISC} = M_{pl} - (M_{pl} - 0.7 f_y W_{el}) \left(\frac{\lambda_{AISC} - 0.33 \sqrt{\frac{E}{f_y}}}{1.13 \sqrt{\frac{k_c E}{f_y}} - 0.33 \sqrt{\frac{E}{f_y}}} \right) \quad \text{for } 0.33 \sqrt{\frac{E}{f_y}} < \lambda_{AISC} \leq 1.13 \sqrt{\frac{k_c E}{f_y}} \quad (10)$$

$$M_{AISC} = \frac{0.9 E k_c W_{el}}{\lambda_{AISC}^2} \quad \text{for } \lambda_{AISC} > 1.13 \sqrt{\frac{k_c E}{f_y}} \quad (11)$$

and Equations (12) to (14) for I-sections under minor axis bending. A graphical illustration of the revised AISC resistance function and a comparison of the capacity predictions with the test and FE data are shown in Section 5.4.

$$M_{AISC} = M_{pl} = f_y W_{pl} \quad \text{for } \lambda_{AISC} \leq 0.33 \sqrt{\frac{E}{f_y}} \quad (12)$$

$$M_{AISC} = M_{pl} - (M_{pl} - 0.7 f_y W_{el}) \left(\frac{\lambda_{AISC} - 0.33 \sqrt{\frac{E}{f_y}}}{0.99 \sqrt{\frac{E}{f_y}} - 0.33 \sqrt{\frac{E}{f_y}}} \right) \quad \text{for } 0.33 \sqrt{\frac{E}{f_y}} < \lambda_{AISC} \leq 0.99 \sqrt{\frac{E}{f_y}} \quad (13)$$

$$M_{AISC} = \frac{0.69 E W_{el}}{\lambda_{AISC}^2} \quad \text{for } \lambda_{AISC} > 0.99 \sqrt{\frac{E}{f_y}} \quad (14)$$

5.3. Continuous strength method

The continuous strength method (CSM) is a deformation-based design approach that replaces traditional cross-section classification and enables a rational exploitation of strain hardening [28, 32, 33]. This allows higher bending resistances, beyond those attainable in traditional design methods, to be achieved in stainless steel beams. Note that the CSM is also provided as an alternative design approach in AISC Design Guide 27.

Application of the CSM firstly requires the cross-section normalised deformation capacity $\varepsilon_{csm}/\varepsilon_y$ to be determined. This normalised deformation capacity is determined with reference to the CSM base curves, as given by Equations (15) and (16) [28, 34] for non-slender and slender sections, respectively, where ε_{csm}

is the maximum attainable compressive strain of a cross-section under the applied loading, ε_y is the yield strain, defined as f_y/E , and $\bar{\lambda}_p = \sqrt{f_y / \sigma_{cr}}$ is the cross-section slenderness, in which σ_{cr} is the elastic local buckling stress of the full cross-section.

$$\frac{\varepsilon_{\text{csm}}}{\varepsilon_y} = \frac{0.25}{\bar{\lambda}_p^{0.36}} \quad \text{but } \leq \min\left(15, \frac{C_1 \varepsilon_u}{\varepsilon_y}\right) \quad \text{for } \bar{\lambda}_p \leq 0.68, \quad (15)$$

$$\frac{\varepsilon_{\text{csm}}}{\varepsilon_y} = \left(1 - \frac{0.222}{\bar{\lambda}_p^{1.050}}\right) \frac{1}{\bar{\lambda}_p^{1.050}} \quad \text{for } \bar{\lambda}_p > 0.68. \quad (16)$$

The CSM design stress f_{csm} corresponding to the limiting strain ε_{csm} is then determined from the CSM bi-linear material model given by Equation (17). The CSM material model is depicted in Figure 32, where C_1 , C_2 , C_3 and C_4 are the CSM material coefficients, of which the values were calibrated based on material tensile coupon test data, as reported in [35]. The coefficient C_1 is utilised to define a cut-off strain in the CSM base curve for non-slender sections (see Equation (15)), to avoid over-predicting failure strength from the adopted CSM bi-linear material model. The coefficient C_2 , as adopted in Equation (18), defines the strain hardening slope E_{sh} , while $\varepsilon_u = C_3(1 - f_y/f_u) + C_4$ is the predicted strain at the material ultimate strength. For austenitic stainless steel, values of C_1 , C_2 , C_3 and C_4 are equal to 0.1, 0.16, 1.0 and 0.0, respectively.

$$\begin{aligned} f_{\text{csm}} &= E \varepsilon_{\text{csm}} & \text{for } \varepsilon_{\text{csm}} < \varepsilon_y, \\ f_{\text{csm}} &= f_y + E_{\text{sh}} (\varepsilon_{\text{csm}} - \varepsilon_y) & \text{for } \varepsilon_{\text{csm}} \geq \varepsilon_y. \end{aligned} \quad (17)$$

$$E_{\text{sh}} = \frac{f_u - f_y}{C_2 \varepsilon_u - \varepsilon_y}. \quad (18)$$

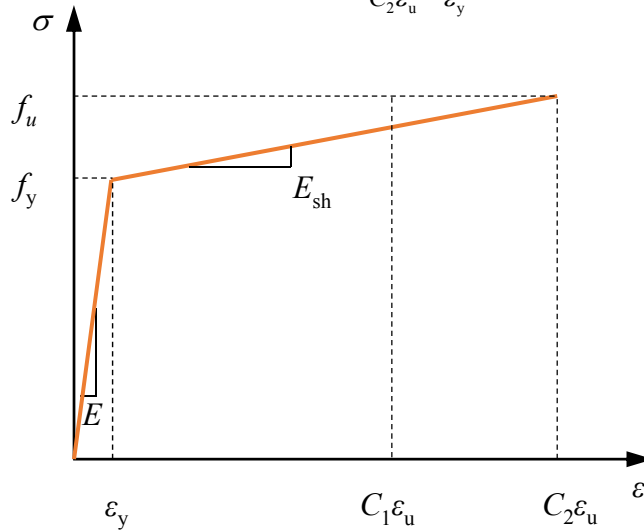


Figure 32: CSM elastic, linear hardening material model

The CSM bending moment resistance $M_{\text{csm,Rd}}$ is calculated using the simplified design formula [34], given by Equation (19), where α is the CSM bending coefficient, related to cross-section shape, aspect ratio and bending axis, with values of 2 and 1.2 for I-sections bent about the major and minor axis, respectively, and γ_{M0} is a partial factor for cross-section bending resistance, with a recommended value of 1.1 for stainless steel.

$$M_{\text{csm,Rd}} = \frac{W_{\text{pl}} f_y}{\gamma_{\text{M0}}} \left[1 + \frac{E}{E_{\text{sh}}} \frac{W_{\text{el}}}{W_{\text{pl}}} \left(\frac{\varepsilon_{\text{csm}}}{\varepsilon_y} - 1 \right) - \left(1 - \frac{W_{\text{el}}}{W_{\text{pl}}} \right) / \left(\frac{\varepsilon_{\text{csm}}}{\varepsilon_y} \right)^\alpha \right] \quad \text{for } \bar{\lambda}_p \leq 0.68, \quad (19)$$

$$M_{\text{csm,Rd}} = \frac{W_{\text{el}} f_{\text{csm}}}{\gamma_{\text{M0}}} \quad \text{for } \bar{\lambda}_p > 0.68.$$

5.4 Comparison of resistance predictions

Comparisons of the resistance predictions of EN 1993-1-4, the revised AISC Design Guide 27 and the continuous strength method with the test/ FE results are presented in Figure 33 for major axis bending and 34 for minor axis bending, where the ultimate moment capacities from the test and FE results are normalised with the plastic moment capacity M_{pl} and plotted against the cross-section slenderness $\bar{\lambda}_p$.

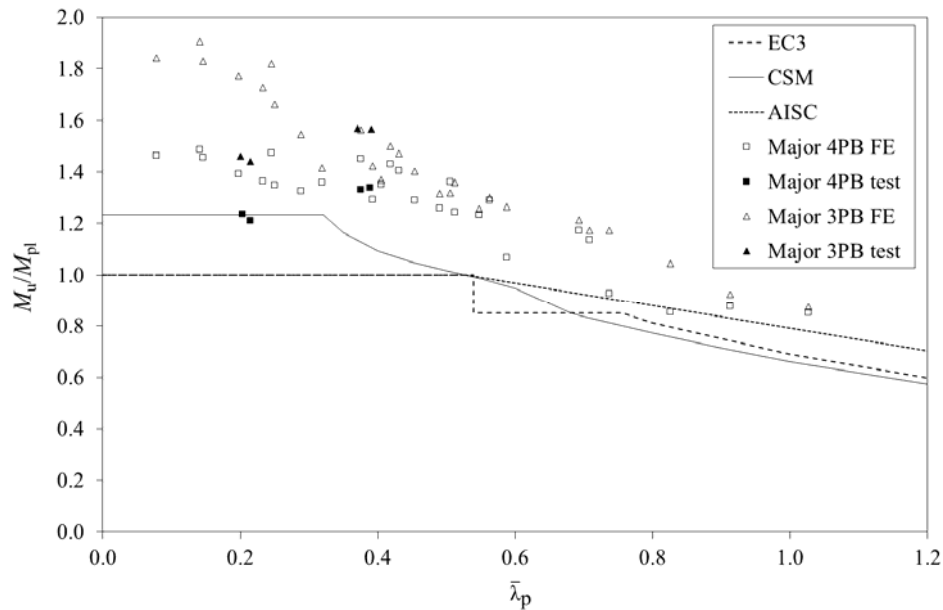


Figure 33: Comparison of test and FE data with moment resistance predictions for major axis bending

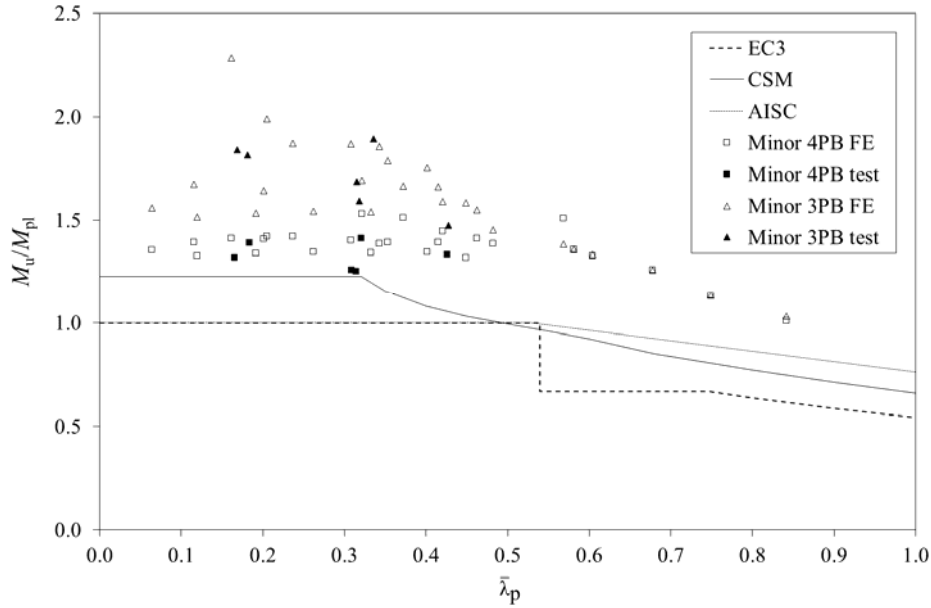


Figure 34: Comparison of test and FE data with moment resistance predictions for minor axis bending

Tabular comparisons of the test and FE capacities M_u with the ultimate moment predictions from EN 1993-1-4 (M_{EC3}), AISC Design Guide 27 (M_{AISC}) and the continuous strength method (M_{csm}) are presented in Table 12. Overall, the EN 1993-1-4 and AISC capacity predictions M_{EC3} may be seen to be safe-sided, but rather conservative. Application of the CSM may be seen to lead to more accurate capacity predictions than EN 1993-1-4, with average enhancements in design efficiency of almost 20%. The CSM remains conservative due principally to the inclusion of a strain limit equal to 15 times the yield strain, to avoid excessive deformations. Nonetheless, the increased resistances obtained for the CSM over EN 1993-1-4 and the AISC Design Guide are substantial.

Table 12: Comparison of EN 1993-1-4, AISC and CSM resistance predictions with test and FE results.

Configuration	Data source	No.	M_u/M_{EC3}		M_u/M_{AISC}		M_u/M_{csm}	
			Mean	COV	Mean	COV	Mean	COV
Major 4PB	Tests	4	1.28	0.05	1.28	0.05	1.09	0.10
	Tests+FE	31	1.37	0.07	1.30	0.10	1.19	0.09
Major 3PB	Tests	4	1.51	0.05	1.51	0.05	1.29	0.09
	Tests+FE	31	1.54	0.11	1.47	0.15	1.33	0.07
Minor 4PB	Tests	6	1.33	0.05	1.33	0.05	1.17	0.09
	Tests+FE	33	1.57	0.21	1.40	0.05	1.34	0.17
Minor 3PB	Tests	6	1.72	0.09	1.72	0.10	1.51	0.08
	Tests+FE	33	1.84	0.15	1.66	0.12	1.57	0.10

6. Conclusions

A series of experiments were conducted at Imperial College London to investigate the cross-sectional stability of laser-welded austenitic stainless steel I-sections in bending. The test programme included 8 major axis bending tests and 12 minor axis bending tests. Both four-point and three-point bending configurations were employed. The test setups, experimental procedures and key test results have been described. Tensile coupon tests, stub column tests, initial geometric imperfection measurements and residual stress measurements are also reported. Numerical models were developed and validated against the test results. A parametric study was carried out to further investigate the structural performance of laser-welded I-sections. Finally, the test and FE results were used to assess the applicability of existing slenderness limits to laser-welded sections and to assess the accuracy of moment capacity predictions from EN 1993-1-4, AISC Design Guide 27 and the continuous strength method (CSM). The existing provisions of EN 1993-1-4 and AISC Design Guide 27 were found to be safely applicable to laser-welded stainless steel sections in bending, but yielded rather conservative resistance predictions; considerable enhancements in design efficiency were achieved using the CSM.

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