

1     **EXPERIMENTAL ANALYSIS OF STEEL BEAMS SUBJECTED TO FIRE ENHANCED BY BRILLOUIN**  
2                                     **SCATTERING BASED FIBER OPTIC SENSOR DATA**

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5     **ABSTRACT**

6         This paper presents high temperature measurements using a Brillouin scattering based fiber  
7     optic sensor and the application of the measured temperatures and building code recommended  
8     material parameters into enhanced thermo-mechanical analysis of simply-supported steel beams  
9     subjected to combined thermal and mechanical loading. The distributed temperature sensor  
10    captures detailed, non-uniform temperature distributions that are compared locally with  
11    thermocouple measurements with less than 4.7% average difference at 95% confidence level.  
12    The simulated strains and deflections are validated using measurements from a second  
13    distributed fiber optic (strain) sensor and two linear potentiometers, respectively. The results  
14    demonstrate that the temperature-dependent material properties specified in the four investigated  
15    building codes lead to strain predictions with less than 13% average error at 95% confidence  
16    level, and that the EN1993-1-2 building code provided the best predictions. However, the  
17    implicit consideration of creep in the EN1993-1-2 is insufficient when the beam temperature  
18    exceeds 800°C.

19    **Keywords:** Steel beams; Thermo-mechanical analysis; Non-uniform temperature distribution;  
20    Distributed fiber optic sensors; Fire.

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21 **INTRODUCTION**

22 When a steel structural component is subjected to elevated temperatures, both its material  
23 properties and geometry change. The Young's modulus and yield strength of steel degrade  
24 quickly with increasing temperature for temperatures greater than 400°C (Usmani *et al.* 2003).  
25 The degradation of mechanical properties directly reduces the load carrying capacity of a  
26 structure. Additionally, thermal expansion can cause changes in connection conditions leading to  
27 structural instability and collapse (Sunder 2005). Restraint of thermal expansion can result in  
28 large stresses that can cause buckling or yielding of structural members. Therefore, thermal  
29 effects can substantially influence the performance of steel structures in fire (Huang and Tan  
30 2003; Tan *et al.* 2007).

31 To improve the safety of buildings in a fire, extensive experimental investigations of large-  
32 scale steel structures have been carried out in the past. Typical measurements in these  
33 investigations included temperature, strain, displacement, and load. With the exception of  
34 temperature, almost all measurements were obtained from locations outside of the heated zone.  
35 For example, Tan *et al.* (2007) and Dwaikat *et al.* (2011) investigated the behavior of steel  
36 columns in a furnace with displacements and loads measured using transducers placed outside of  
37 the heated zone. Dwaikat *et al.* (2011) applied strain gauges to a section of each steel test  
38 specimen located outside of a test furnace to measure localized strains. Li and Guo (2008)  
39 subjected steel beams to heating in fire and subsequent cooling and measured loads and  
40 deformations outside of the high-temperature zone. Strain gauges were installed on auxiliary  
41 members to indirectly determine forces based on force equilibrium. High-temperature resistance-  
42 based strain gauges have been reported to be unreliable in structural applications with fire  
43 (McAllister *et al.* 2012).

44 Fiber Bragg grating (FBG) sensors were used by Zhang and Kahrizi (2007) to measure strain  
45 and temperature; however, the sensors began to degrade when heated over 300°C and the fiber  
46 gratings were erased completely around 600°C. Similarly, Huang *et al.* (2010) used long period  
47 fiber grating sensors inscribed in optical fibers for strain and temperature measurement up to  
48 700°C. Both types of gratings had limited thermal stability for fire applications (Venugopalan  
49 2010; Huang *et al.* 2013). Regenerated FBG sensors with enhanced thermal stability were used  
50 to successfully monitor temperature changes in fire by Rinaudo *et al.* (2015). Nevertheless,  
51 grating sensors provide measurements only at discrete points. Therefore, fully-distributed fiber  
52 optic sensors that utilize Brillouin optical time domain analysis (BOTDA) or Brillouin optical  
53 time domain reflectometer (BOTDR) technology (Bao and Chen 2011) and provide multiple  
54 measurements along a fiber have recently attracted attention in the research community. The  
55 application of BOTDR and BOTDA, however, is limited due to low spatial resolution of the  
56 measurements, often as large as 15 cm (Bao and Chen 2011). Recently, a pulse pre-pumped  
57 BOTDA (PPP-BOTDA) technology has been commercialized for simultaneous strain and  
58 temperature measurement with 2 cm spatial resolution over a measurement distance of 0.5 km  
59 (Kishida and Li 2006; Bao *et al.* 2015). However, implementation of distributed fiber optic  
60 sensors for structural fire research has not been fully explored.

61 Based on experimental investigations, analytical (Usmani *et al.* 2001; Huang and Tan 2003)  
62 and numerical (Choi 2008; Zhang *et al.* 2012, 2013) studies were carried out, and various  
63 computational models were proposed to predict the thermal and mechanical responses of steel  
64 beams and columns in fire, including the spatial and temporal temperature distributions and  
65 structural deflections.

66 Thermo-mechanical analysis procedures for structures or structural components in the

67 literature can be categorized as sequentially-coupled or fully-coupled. When the thermal  
68 responses can be considered independent of the mechanical responses, a sequentially-coupled  
69 thermo-mechanical analysis (Jeffers and Sotelino 2012) can be performed. In this case, thermo-  
70 dynamic and heat transfer analyses are first conducted to predict the temperature distributions.  
71 Then, the predicted temperature distributions are applied to determine the thermal expansion and  
72 temperature-dependent material properties. Finally, mechanical analysis is carried out to predict  
73 the structural performance.

74 When the mechanical response can significantly influence the thermal response, a fully-  
75 coupled thermo-mechanical analysis must be performed. In a fully-coupled analysis, the  
76 incremental results of the structural model are used to incrementally update the boundary  
77 conditions in the thermo-dynamic model.

78 Both sequentially-coupled and fully-coupled thermo-mechanical analysis require the  
79 prediction of temperature distributions. Even though zone models (Cadorin and Franssen 2003;  
80 Li and Zhang 2012), computational fluid dynamics models; e.g. the Fire Dynamics Simulator  
81 (McGrattan *et al.* 2010), and stochastic models (Bertola and Cafaro 2009) for fire have been  
82 developed, it is difficult to accurately predict the resulting temperature distributions. The error in  
83 the predicted temperature distribution can result in inaccurate mechanical response of the  
84 structure. Most importantly, the error in temperature distribution and the inaccuracy in  
85 mechanical response cannot be quantified without properly measured data, which is difficult to  
86 obtain when test objects and sensors are engulfed in flames.

87 In this study, we directly measure temperatures using a distributed fiber optic temperature  
88 sensor under fire conditions. The measured temperature distributions are then applied for  
89 enhanced thermal-mechanical analysis of steel beams under combined fire and static loading to

90 assess building code-specified relations for temperature-dependent mechanical properties of the  
91 steel. The measured temperatures from the distributed temperature sensor are compared to results  
92 obtained using thermocouples. The simulated strains and deflections from the structural analysis  
93 are validated using a distributed strain sensor that uses Brillouin scattering and two linear  
94 potentiometers, respectively.

## 95 **EXPERIMENTAL PROGRAM**

### 96 **Test Specimen and Setup**

97 Three S3×5.7 ASTM A36 low carbon steel “I-shaped” sections (AISC 2011), designated as  
98 Beam #1, #2, and #3, were tested under three-point loading in a reconfigurable compartment fire  
99 setup (“flame channel”) as shown in Figs. 1(a-c). Each beam was 76 mm deep, 59 mm wide, and  
100 1420 mm long. The cross-sectional area and moment of inertia about the strong axis were 1,077  
101 mm<sup>2</sup> and 10<sup>6</sup> mm<sup>4</sup>, respectively.

102 The flame channel, which was located under a 6 m × 6 m (plan) exhaust hood, included three  
103 subassemblies: a burner rack, an enclosure, and a specimen loading system. The burner rack (Fig.  
104 1(b)) had four independent natural gas diffusion burners made of sheet metal 300 mm × 300 mm  
105 × 140 mm (length × width × height) in dimension. After the natural gas entered a burner from the  
106 bottom, it filled the burner cavity and passed through a 20 mm thick ceramic fiber blanket to  
107 distribute the gas. The burners were manually regulated using a needle valve on the gas line, and  
108 the energy content of the supplied gas was measured with an expanded uncertainty of less than  
109 2.4% (Bundy *et al.* 2007).

110 An enclosure constructed of square tube steel, cold-formed steel C-profiles and gypsum  
111 board lined with thermal ceramic fiber enclosed the space above the burner rack. The enclosure  
112 was open at three faces: the bottom and the two ends in longitudinal direction of the beam,

113 creating the compartment flame dynamics depicted in Fig. 1(c). The heated “compartment”  
114 created by the enclosure was approximately 380 mm × 400 mm × 1830 mm (height × width ×  
115 length) in size and reduced radiative heat losses.

116 Each beam was simply supported at a clear-span of 1250 mm on two supports constructed of  
117 1-1/2” Schedule 40 pipe (outer diameter: 48 mm), which were supported on concrete blocks. The  
118 specimen was loaded by a U-shape 1/2” Schedule 40 pipe (outer diameter: 21 mm) “loading  
119 yoke” at the mid-span (Fig. 1(a)). Both the supporting and the loading pipes were water-cooled  
120 with the exiting water temperature controlled to less than 50°C, which limited undesired thermal  
121 movement of the boundary conditions. Load was transferred to the yoke with the pulley system  
122 depicted in Figs. 1(a) and 1(c).

123 **Instrumentation**

124 Data from the fuel delivery system, thermocouples, displacement sensors and a load  
125 transducer were measured continuously using a National Instruments data acquisition system (NI  
126 PXIe-1082). Thermocouple data were recorded using 24-bit Thermocouple Input Modules (NI  
127 PXIe-4353), and load and displacement data were recorded using a high-speed, 16-bit  
128 multifunction module (NI PXIe-6363). Data were sampled at 90 Hz with average values and  
129 standard deviations recorded in the output file at a rate of 1 Hz.

130 A Neubrescope data acquisition system (NBX-7020) for the distributed fiber optic sensors  
131 was used to perform PPP-BOTDA measurements (Kishida and Li 2006; Bao and Chen 2015,  
132 2016a). Using a pulse bandwidth of 0.2 ns, 2 cm spatial resolution was obtainable with  
133 accuracies of 0.75°C and 15 µε for temperature and strain at an average count of 2<sup>14</sup> (Neubrex  
134 2013). In this test, the sampling and spatial resolutions were set at 1 cm and 2 cm, respectively,  
135 meaning that data points were sampled at every 1 cm and the Brillouin frequency shifts of two

136 points spaced at no less than 2 cm could be distinguished. The measurement distance was set to  
137 50 m. The scanning frequency ranged from 10.82 GHz to 11.67 GHz, which approximately  
138 corresponded to a target temperature range from 20°C to 1100°C (Bao and Chen 2015). The  
139 reading time varied from 15 seconds to 40 seconds depending on the scanning frequency range.

#### 140 *Distributed Fiber Optic Sensors (DFOSs)*

141 Two single-mode optical fibers with dual-layer coating were used as a distributed  
142 temperature sensor (DFOS-T) and a distributed temperature and strain sensor (DFOS-ST) with  
143 the PPP-BOTDA, respectively. The single-mode fiber had a glass core (diameter: 8.2  $\mu\text{m}$ ), a  
144 glass cladding (outer diameter: 125  $\mu\text{m}$ ), a soft inner coating (outer diameter: 190  $\mu\text{m}$ ), and a  
145 stiff outer coating (outer diameter: 242  $\mu\text{m}$ ). The soft and stiff layers protect the glass from  
146 mechanical impact and from abrasion and environmental exposure, respectively. Both layers are  
147 composed of monomers, oligomers, photoinitiators, and additives (Kouzmina *et al.* 2010). The  
148 distributed sensors measure Brillouin frequency shifts due to temperature and/or strain changes  
149 (Bao *et al.* 2015). Once calibrated, the sensors can be used to evaluate the temperature and strain  
150 changes from the measured Brillouin frequency shifts (Bao and Chen 2015, 2016b).

151 For strain measurement, the coatings must be removed before the optical fiber is attached to a  
152 specimen to ensure effective strain transfer at high temperature. Figs. 2(a) and 2(b) show the  
153 schematic and prototype of a strain sensor package. The bare optical fiber was passed through  
154 two glass tubes (Glass tube 1 in Fig. 2) in series with a small gap between them, and fixed to the  
155 tubes at their far ends using a two-part high-temperature adhesive. The gap between the two  
156 tubes was covered with a third tube (Glass tube 2 in Fig. 2) for additional protection of the fiber.  
157 Each Glass tube 1 was fixed near the gap on the steel beam with a clip and laterally constrained  
158 at the far end with a ring. The leg of each ring or clip was tightly inserted into a small hole ( $\approx$

159 1.4 mm in diameter) pre-drilled on the steel beam. When installed, the two rings and the two  
160 clips were aligned using a steel guide bar as depicted in Fig. 2(b).

161 The strain sensor measures the elongation of steel between the two clips over a base length  
162 denoted by  $d$ . To enable large strain measurements, the steel elongation is averaged over a gage  
163 length of the sensor, denoted by  $L$ . A gage length factor  $\alpha$  of the sensor can thus be defined as the  
164 ratio of the gage length and the base length, or  $\alpha = L/d$  (Huang *et al.* 2010). Since the optical  
165 fiber has limited strain capacity before rupture, increasing  $\alpha$  allows for an increased strain  
166 measurement range, but leads to reduced sensitivity and spatial resolution. In this study,  $\alpha$  was  
167 designed to be 10, providing a maximum strain capacity of approximately 10000  $\mu\epsilon$  (1%). As  
168 shown in Fig. 2(a), the optical fiber for strain sensing had a stand-off distance of  $2.5\pm 0.5$  mm  
169 from the surface of the specimen.

170 For temperature measurement, the coatings of the optical fiber were left in place to provide  
171 protection during installation. The protective coatings burn off at 300°C – 400°C with negligible  
172 influence on the temperature measurement, while the glass core and cladding can survive to  
173 temperatures above 1000°C.

174 Each beam was instrumented with three strain-temperature sensors as shown in Fig. 3:  
175 DFOS-ST1 and DFOS-ST2 on the bottom flange at mid-span and quarter-span of the beam,  
176 respectively, and DFOS-ST3 on the top flange at quarter-span. The optical fiber as a light  
177 transmission cable of DFOS-ST1, DFOS-ST2, and DFOS-ST3 or as a distributed temperature  
178 sensor (DFOS-T in Fig. 3) was passed along the top and bottom flanges of each beam to form a  
179 closed loop with the Neubrescope for PPP-BOTDA measurements. It was intermittently and  
180 loosely attached to the surface of the beam using a two-part high temperature adhesive. The  
181 transmission cable and the temperature sensor were closely spaced, and thus subjected to



182 approximately the same temperatures. Therefore, the strain at the location of DFOS can be  
183 determined from the Brillouin frequency shift with temperature compensation as described by  
184 Bao *et al.* (2015).

### 185 ***Thermocouple, Load and Displacement Sensors***

186 Each beam was instrumented with four glass-sheathed, K-type, bare-bead thermocouples (24  
187 gage wire) peened into small (diameter < 2 mm) holes drilled into the bottom and top flanges as  
188 indicated in Fig. 3: TC1 and TC3 at mid-span, and TC2 and TC4 at quarter-span. Additional  
189 thermocouples were located throughout the test setup to characterize the test environment and  
190 monitor safety-relevant temperatures. The thermocouples have a manufacturer-specified  
191 temperature standard limit of error of 2.2°C or 0.75% (whichever value is greater) over a  
192 measurement range of 0°C – 1250°C.

193 A calibrated (linearity:  $\pm 0.03\%$ , repeatability:  $\pm 0.01\%$ ) load transducer by Omegadyn (LCR-  
194 100), placed on a spanning bar at the bottom of the loading yoke, was used to measure the  
195 applied load as illustrated in Figs. 1(a) and 1(c).

196 The mid-span vertical deflection at the bottom surface of the beam was measured using two  
197 linear potentiometers (Novotechnik TR-0050) located below the burner rack and connected to  
198 the beam via high-temperature ceramic fibers. The use of two fibers provided compensation for  
199 the unwanted influence of gas temperature on displacement measurements with an estimated  
200 expanded uncertainty of 0.2 mm (95% confidence).

### 201 **Test Protocol**

202 Each beam was subjected to both fire and mechanical loading. Fig. 4 illustrates the fire test  
203 protocol. The heat release rate (HRR) was held approximately constant at five target levels: 25  
204 kW, 65 kW, 120 kW, 195 kW, and 350 kW, which corresponded to beam temperatures at TC1 of

205 approximately 200°C, 400°C, 600°C, 850°C, and 1050°C, respectively. During the test of Beam  
206 #2, the gas was turned off for about 20 seconds before the HRR was increased to 120 kW and  
207 195 kW, respectively, to allow for visual observation. When the HRR was increased to a higher  
208 level, the target value was overshoot and then quickly regulated down to the expected value. At  
209 each HRR level, in addition to the self-weight, the beam was subjected to three levels of loads at  
210 the mid-span. For Beam #1, the three loads were approximately 68 N, 98 N, and 126 N, and  
211 sustained for 7 minutes, 4 minutes, and 4 minutes, respectively. For Beams #2 and #3, the three  
212 loads were approximately 68 N, 176 N, and 285 N, each sustained for 6 minutes.

### 213 **THERMO-MECHANICAL ANALYSIS**

214 Traditionally, thermo-mechanical analysis of a structure subjected to fire is a multi-step  
215 process that starts with prediction of the fire behavior. Distributions of heat flux to member  
216 surfaces are calculated to provide boundary conditions to the thermo-mechanical analysis.  
217 Temperature distributions in members are then determined by solving heat conduction equations  
218 with the boundary conditions. Finally, the calculated temperature distributions are applied to  
219 determine material properties, and thermally induced strain and the structural response can be  
220 analyzed. This is a complex process and accumulated errors can be significant.

221 In this study, temperature distributions near the beam surface were directly measured with  
222 the distributed fiber optic sensor (DFOS-T), eliminating the need for prediction of the thermal  
223 boundary conditions. Researchers have previously investigated the relationship between the  
224 temperatures on a beam surface and within the beam. A so-called “section factor” – the ratio of  
225 the fire-exposed perimeter to the cross-sectional area – determines the heat transfer rate within  
226 the beam. Larger section factors lead to higher heat transfer rates. When the section factor of an  
227 unprotected steel section is larger than  $300 \text{ m}^{-1}$ , the temperature within steel can be considered to

228 be the same as the surface temperature (Li *et al.* 2006; Li and Wang 2012). Since the small test  
229 beams (S3×5.7 section) had a section factor of  $353 \text{ m}^{-1}$ , the measured surface temperatures at the  
230 top or bottom flange were approximately equal to those within the steel. Furthermore, due to the  
231 small beam height, the vertical distributions of temperature over the beam height can be  
232 approximated as linearly-distributed between the upper and lower surface temperatures (Choi  
233 2008).

### 234 **High Temperature Steel Properties**

235 Temperature-dependent material properties governing structural behaviors include thermal,  
236 mechanical, and deformation properties (Kodur *et al.* 2010). The thermal properties include  
237 thermal conductivity, specific heat, and density. The mechanical properties include yield strength,  
238 elastic modulus, and post-yielding stress-strain relationship. The deformation properties include  
239 thermal expansion and creep.

240 Various models of temperature-dependent mechanical properties were compared by Li and  
241 Wang (2012). In their study, four degradation laws of yield strength and elastic modulus of steel  
242 at elevated temperatures and their corresponding thermal strains as shown in Fig. 5 were taken  
243 from four standards: EN 1993-1-2 (ECS 2005), AS 4100 (SA 1998), CECS 200 (CECS 2006),  
244 and ANSI/AISC 360-10 (AISC 2010). The reduction factors for yield strength and elastic  
245 modulus are denoted by  $\eta_y$  and  $\eta_E$ , respectively.  $\eta_y = f_{yT}/f_{y20}$ , and  $\eta_E = E_T/E_{20}$ , where  $f_{y20}$  and  $f_{yT}$   
246 represent the yielding strengths at 20°C and arbitrary temperature  $T$ , respectively;  $E_{20}$  and  $E_T$   
247 represent the elastic moduli at 20°C and arbitrary temperature  $T$ , respectively.

### 248 **Mechanical Analysis**

249 With the measured temperature distributions and the temperature-dependent properties of the  
250 steel, a finite element model of the beam was created using ABAQUS®. Three-dimensional 8-

251 node brick elements (2.5 mm mesh size) with reduced integration (C3D8R) were used to model  
252 the simply-supported beam, as shown in Fig. 6. Based on the measured temperature distributions  
253 and high temperature steel properties, user subroutines “UMAT” and “UTEMP” (SIMULIA  
254 2014) were applied to define the temperature-dependent nonlinear plasticity of the steel and the  
255 non-uniform temperature distributions, respectively.

## 256 **RESULTS AND DISCUSSION**

### 257 **Temperature Distributions**

258 Temperature distributions in steel Beam #2 are presented in Fig. 7 at the five investigated  
259 HRR values ranging from 25 kW to 350 kW. At each HRR, the temperature distribution along  
260 the beam was not symmetric about the mid-span. The overall temperature distribution pattern  
261 varied as the HRR increased. These results generally agree with the visual observation that the  
262 flames were somewhat asymmetrical during the tests. The asymmetry is attributed to variations  
263 in the ventilation of the flame channel compartment and in the gas distribution among the four  
264 burners. These results illustrate the complex behavior of fire that can cause predicted temperature  
265 distributions to differ significantly from actual conditions (Cadorin and Franssen 2003;  
266 McGrattan *et al.* 2010; Li and Zhang 2012).

267 Fig. 8 shows a representative temperature time history measured from TC1 in Beam #2. The  
268 five plateaus corresponded to the five HRR levels in Fig. 4(a). At each sustained HRR level, the  
269 beam temperature gradually stabilized to a temperature with some variation. The variations were  
270 relatively small at low HRR values and became larger as the HRR was increased. To quantify the  
271 temperature variations, the mean values and standard deviations were calculated over 15 minutes  
272 for Beam #1, and 18 minutes for Beams #2 and #3 when the mechanical loads were applied at  
273 each temperature level. The means and standard deviations are presented in Table 1. The

274 coefficient of variation for all the thermocouple readings is less than 3.6 %.

275 Similarly, to average out the effects of temperature fluctuation, five measurements were  
276 made using the DFOS-T at each sustained temperature level. Each measurement was an implicit  
277 average over 15 seconds to 40 seconds. The mean values and the standard deviations of five  
278 measurements were calculated and compared with the thermocouple results listed in Table 1. The  
279 DFOS-T readings have a maximum coefficient of variation of 4%, which was similar to that of  
280 the thermocouples.

281 Table 1 also shows that the relative difference between the mean temperatures from the  
282 DFOS-T and the thermocouple ranges from -10% to 8%. To understand the statistical  
283 significance of the measurement differences, the average of mean temperature differences (four  
284 for Beam #1, three for Beam #2, four for Beam #3) was calculated at each HRR level and  
285 presented in Fig. 9 as an average temperature difference. In addition, the range of the mean  
286 differences at 95% confidence level is represented by the error bar in Fig. 9. It can be observed  
287 from Fig. 9 that the mean difference at 95% confidence level is less than 4.7%, which is  
288 acceptable in most engineering applications. The discrepancies may be attributed to several  
289 factors. First, the DFOS-T sensor was installed in a slightly different location than the  
290 thermocouples. Second, the thermocouple beads were located slightly below the surface of the  
291 beam and the DFOS-T slightly above the surface, and thus, the influence of gas temperature  
292 variation on the measurements varied. Additionally, the thermocouples were not corrected for  
293 radiation losses.

## 294 **Strains**

295 The simulated strains of Beam #2 under fire and 285 N loading using the mechanical  
296 properties specified in the EN1993-1-2 code are presented in Fig. 10 for the first three HRR

297 levels. They include the effects of thermal elongation due to uniform temperature change,  
298 thermal bending due to temperature gradient over the cross section, and mechanical bending due  
299 to the applied load. At the applied load of 285 N, the mechanical bending caused elastic strain  
300 only. Creep strain (time dependent) was not explicitly modeled in the thermo-mechanical  
301 analysis, although creep was implicitly included in the stress-strain relationship and the  
302 measured temperature distributions that were input into the model. Furthermore, the beam  
303 changes its position with respect to the heat source when deflected significantly, altering the  
304 temperature distribution in beam (Baum 2011). As deflection increases, the influence of  
305 deflection on the temperature distribution was taken into account in thermal analysis through the  
306 DFOS-T measurement.

307 The strains at the bottom flange and mid-span of Beam #2 due to thermal elongation, thermal  
308 bending and mechanical bending are presented in Fig. 11 for the first three HRRs and all loading  
309 conditions. Fig. 11 shows that the thermal elongation accounted for over 95% of thermal induced  
310 strain, and the thermal strain accounted for over 95% of total strain. The observation that thermal  
311 elongation effects dominated the response is supported by the fact that the top flange of the beam  
312 is always subjected to positive strains as illustrated by Fig. 10.

313 The simulated strains are also compared in Fig. 11 with the strains measured by the  
314 DFOS-ST1 sensors (average  $\pm$  one standard deviation of five readings). The variations of the  
315 strain measurements – due mainly to temperature fluctuation – are small compared to their  
316 average amplitudes. The simulated strains at HRR of 195 kW and 350 kW are not included in  
317 Fig. 11 because the DFOS-ST sensors failed due to excessive fiber deformation. In general, the  
318 simulated strains under the investigated heating and loading conditions compared well with their  
319 corresponding measured strains. To quantify the difference, the strains simulated by finite

320 element analysis using the temperature-dependent properties specified in Fig. 5 and the measured  
321 strains at the three DFOS-ST locations are compared under the highest load applied and HRR up  
322 to 120 kW. Similar to Fig. 9, Fig. 12 shows the average of relative strain differences at the  
323 DFOS-ST locations (three for each beam) at each HRR level, and the margin of error  
324 (represented by the error bar) of the data for all beams at 95% confidence level. For the four  
325 temperature-dependent material property models, the overall average strain difference ranges  
326 from 9.7% (EN 1993-1-2) to 13% (CECS 200) at 95% confidence level. The EN 1993-1-2 code  
327 gives the smallest margin of error.

328 The differences between the simulated and measured strains can be attributed to two main  
329 physical phenomena. First, the temperature distribution selected in simulations from five  
330 readings could be different from that at the moment of strain measurement. Second, the adopted  
331 temperature-dependent properties in simulations may not accurately represent those of the test  
332 beam. For example, the temperature-dependent properties suggested in the EN 1993-1-2 code  
333 (ECS 2005) are based on the average values from a small number of steel types (Luecke *et al.*  
334 2011). The second point can be further substantiated by the fact that the average strain  
335 differences in Fig. 12 are mostly negative when the material properties specified in AS 4100  
336 code are used since the thermal strain is the smallest as shown in Fig. 5(c).

### 337 **Mid-span Deflection**

338 Figs. 13(a) and (b) compare the simulated and the measured mid-span deflections for Beam  
339 #1 and Beam #2 at all investigated HRRs and applied loads. Beam #3 is not reported because the  
340 displacement sensor failed at the start of the test. The measurements from the displacement  
341 sensors were corrected for the settlement of supports and thermal elongation of the sensor  
342 attachments. The simulated mid-span deflection used the temperature-dependent material

343 properties specified in EN1993-1-2 (ECS 2005). Fig. 13 indicates that the simulated and  
344 measured results are in good agreement up through 120 kW (about 600°C beam temperature).  
345 The discrepancies at higher temperatures result primarily from not modeling creep. Additional  
346 sources of error could come from a mismatch between the adopted temperature-dependent  
347 properties and those of the test specimens as well as uncertainties in the displacement and fiber  
348 optic sensor measurements. At elevated temperatures, the temperature-corrected displacement  
349 measurements are within  $\pm 0.2$  mm of the manufacturer-specified accuracy of the linear  
350 potentiometers.

351 According to the finite element analysis, 80% – 95% of mid-span deflection was due to  
352 thermal bending when the HRR was at 120 kW. Since the thermal gradient of Beam #2 at HRR =  
353 120 kW was smaller than that at HRR = 65 kW, the deflections at HRR = 120 kW were smaller  
354 as indicated in Fig. 13(b). This seemingly surprising result suggests that the temperature  
355 distribution in the beam largely depended on the fire dynamics and air circulation in our specific  
356 test setup, and that prediction of structure response could be quite inaccurate of uniform heating  
357 assumed. The significant difference in deflection trend between Beams #1 and #2 at HRR =  
358 120 kW was likely due to the gas shutoff during the test of Beam #2.

359 At beam temperatures below 600°C (HRR < 120 kW in these tests), even the largest applied  
360 load of 285 N was insufficient to cause significant deformation of the beam. This was a  
361 limitation of the present tests. At beam temperatures above 600°C, the applied loads were  
362 sufficient to cause extensive mechanical deformation through creep and allow for a more  
363 differentiated assessment of thermal and mechanical contributions to beam response.

## 364 CONCLUSIONS

365 In this study, Brillouin scattering based fiber optic sensors were used to measure



366 temperatures and strains in steel beams exposed to fire. The measured temperatures along the top  
367 and bottom flanges of the beams and their linearly interpolated temperatures in the webs of the  
368 “I-shaped” sections were input to a finite element model of the beam with building code  
369 specified temperature-dependent material properties. The following conclusions can be drawn  
370 based on the experimental and computational results:

371 1 Distributed fiber optic temperature sensors can operate up to at least 1050°C in fire with  
372 adequate sensitivity and accuracy for typical structural engineering applications. The  
373 measured temperatures were validated by thermocouple measurements resulting in an  
374 average relative difference of less than 4.7% at 95% confidence level.

375 2 When  $HRR \leq 120$  kW, the maximum beam temperature was approximately 600°C. The  
376 computational model provided an acceptable prediction of strains (average relative difference  
377 < 13%) and mid-span deflections (0.31 mm maximum difference), when compared to direct  
378 strain measurements by distributed fiber optic strain sensors and temperature compensated  
379 potentiometers measurements, respectively. The material properties specified in EN 1993-1-2  
380 resulted in the smallest margin of error among the four considered building codes. In our  
381 tests, the thermal elongation (not thermal bending) accounted for over 95% of thermal strain,  
382 and the thermal strain accounted for over 95% of total strain. At beam temperatures below  
383 600°C, approximately 80% – 95% of mid-span deflection was due to the effects of thermal  
384 bending.

385 3 When  $HRR \geq 195$  kW, the mechanical loads had a greater influence on the mid-span  
386 deflection due to substantial reduction of the mechanical properties of steel and the resulting  
387 creep. Without explicitly considering creep effects in the simulations, the deformation was  
388 significantly underestimated.

389 4 With a gage length factor of approximately 10, the distributed fiber optic strain sensors  
390 captured large strains and maintained the fiber integrity until the beam temperatures reached  
391 about 600°C. For large strain measurements at higher temperatures, a gage length factor of at  
392 least 20 is suggested.

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397 instruments, or materials are identified in this paper to specify the experimental procedure. Such  
398 identification is not intended to imply recommendation or endorsement by NIST, nor to imply  
399 the materials or equipment are necessarily the best available for the purpose.

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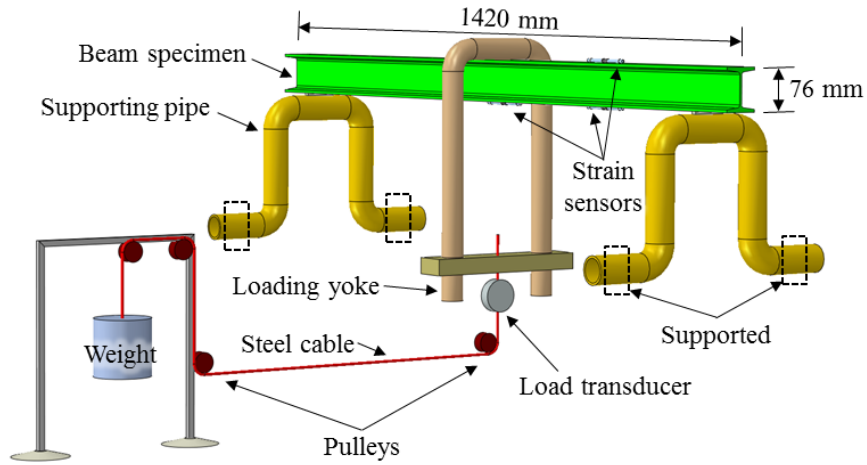
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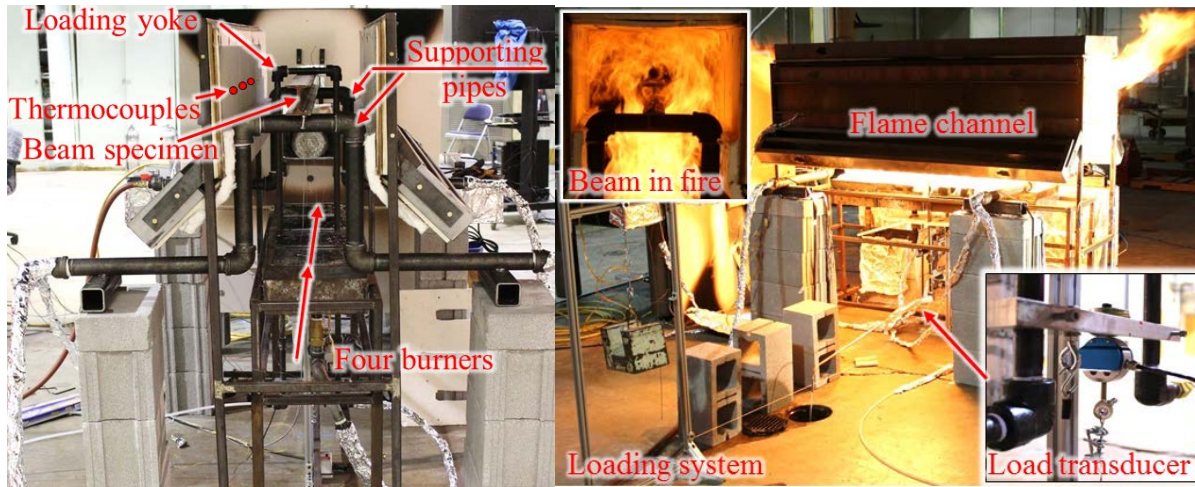
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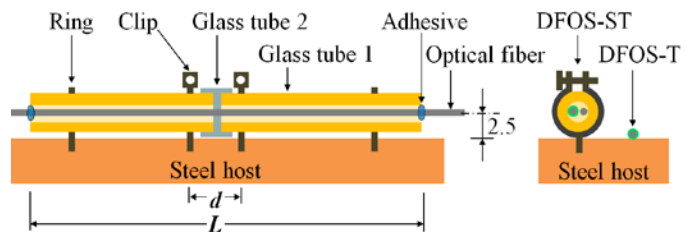


(a) Illustration of specimen loading system

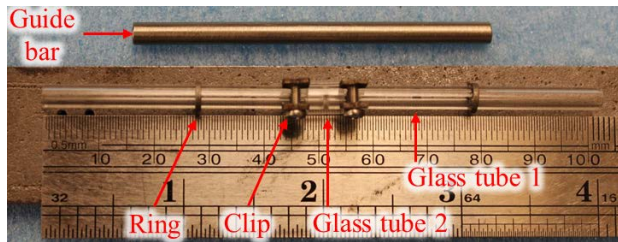


(b) Schematic of burners and specimen supports (c) Schematic of specimen loading system

**Fig. 1.** Test setup.



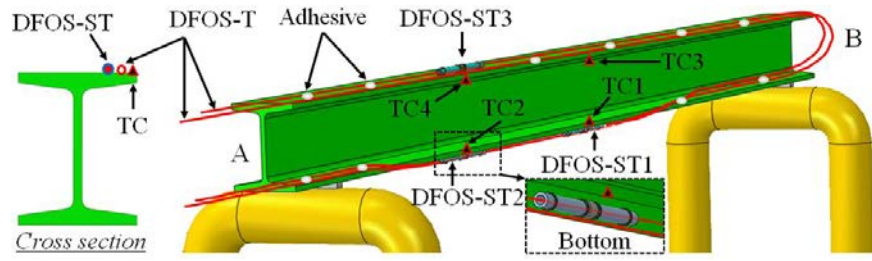
(a) Illustration (units in mm)



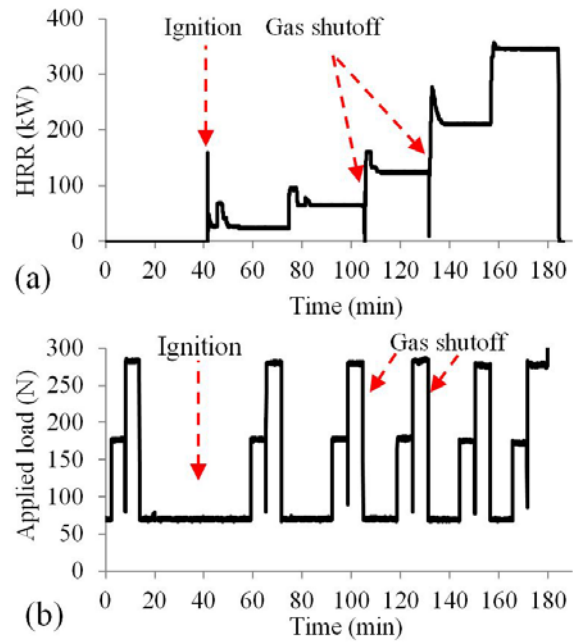
(b) Prototype attached to a steel substrate

**Fig. 2.** Fiber optic strain sensor.

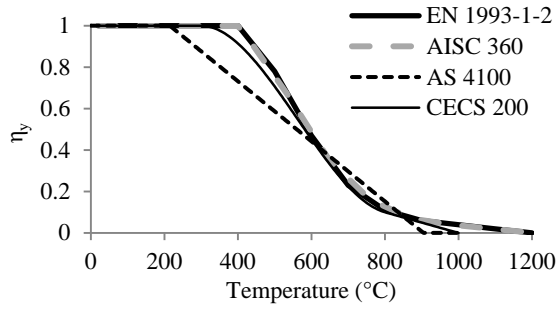




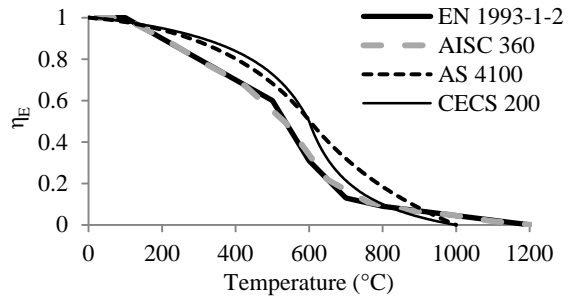
**Fig. 3.** Deployment of fiber optic sensors (DFOS-T, DFOS-ST) and thermocouples (TC).



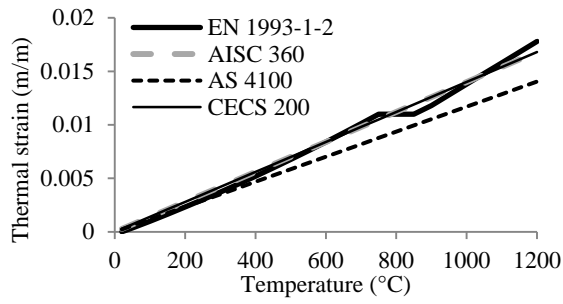
**Fig. 4.** Test protocols: (a) Heat Release Rate (HRR) versus time; (b) Applied load versus time.



(a) Yield strength

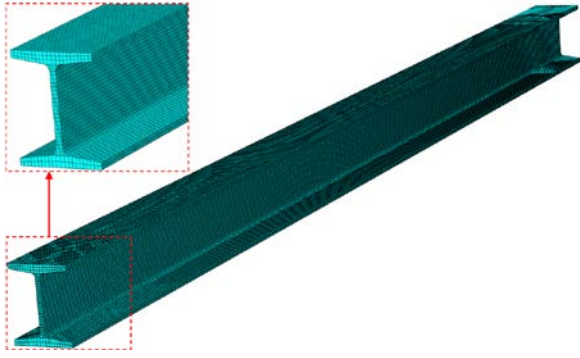


(b) Elastic modulus

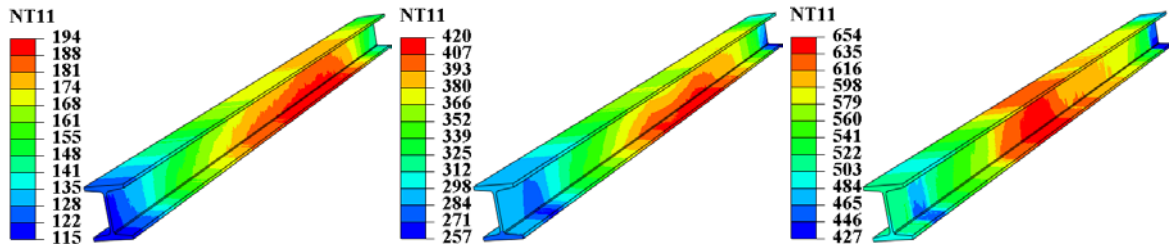


(c) Thermal strain

**Fig. 5.** Comparison of material models.



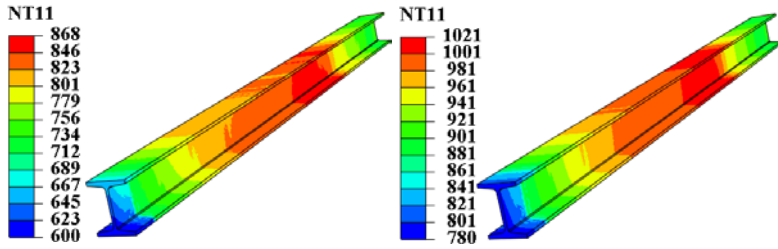
**Fig. 6.** Finite element model of steel beam specimen.



(a) HRR=25 kW

(b) HRR=65 kW

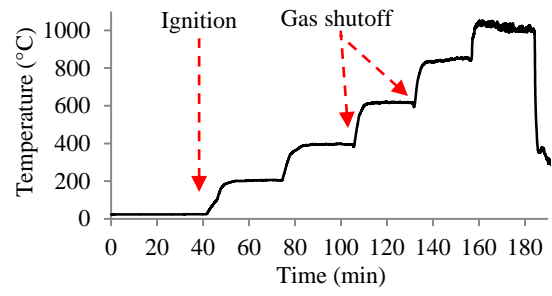
(c) HRR=120 kW



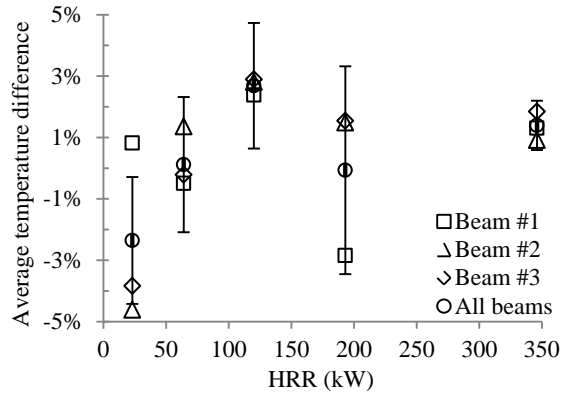
(d) HRR=195 kW

(e) HRR=350 kW

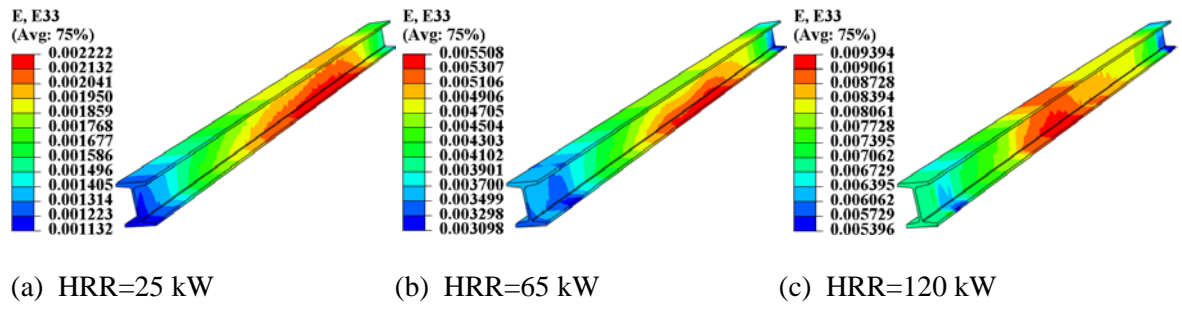
**Fig. 7.** Temperature distributions of Beam #2 at various heat release rates (HRR) measured using the DFOS-T sensor (temperatures in °C).



**Fig. 8.** Temperature time history measured by thermocouple TC1 in Beam #2.

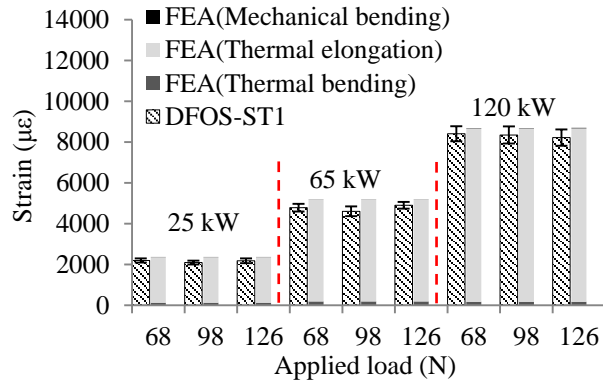


**Fig. 9.** Average relative difference between the fiber optic sensor (DFOS-T) and thermocouple (TC) temperature readings.

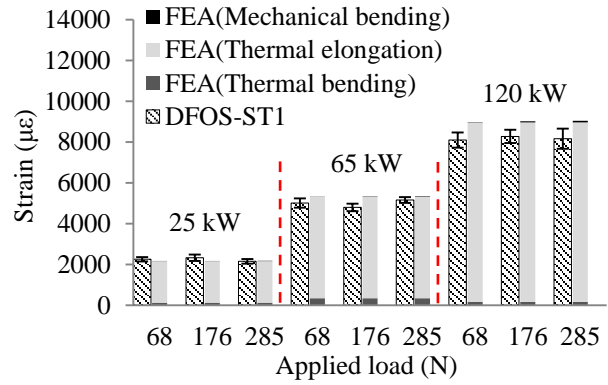


**Fig. 10.** Total strain distributions of Beam #2 under 285 N loading and fire.

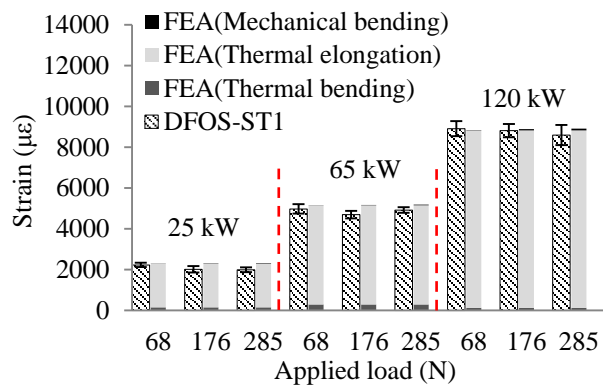




(a) Beam #1

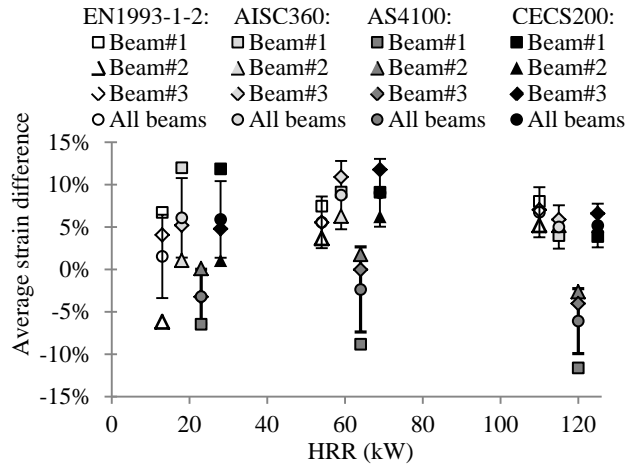


(b) Beam #2

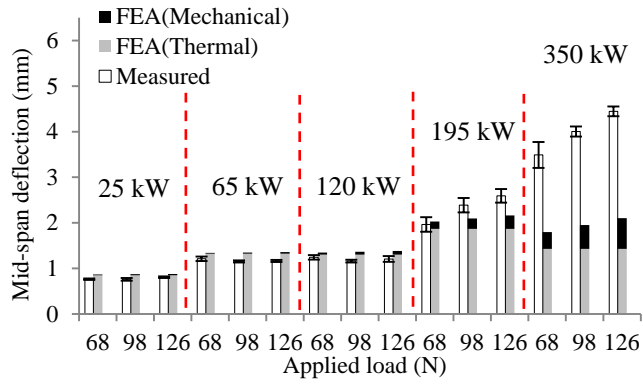


(c) Beam #3

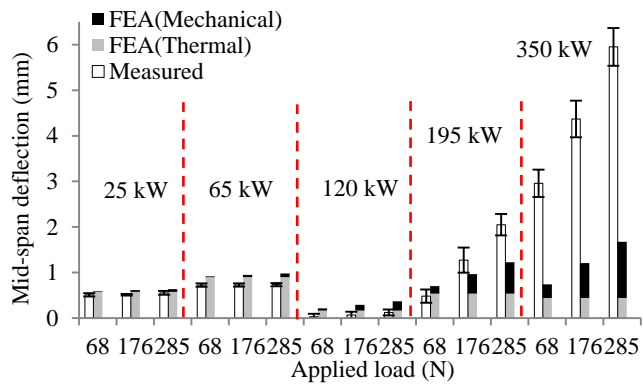
**Fig. 11.** Longitudinal strain on the bottom flange at mid-span of S3×5.7 steel beams under three point bending with a 1250 mm clear span.



**Fig. 12.** Average relative difference between the simulated and measured strains under the highest load.



(a) Beam #1



(b) Beam #2

**Fig. 13.** Mid-span deflections of S3×5.7 steel beams under three point bending with a 1250 mm clear span.