Doctoral Thesis

The Behaviour of steel columns in fire
Material - Cross-sectional Capacity - Column Buckling

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THE BEHAVIOUR OF STEEL COLUMNS IN FIRE

MATERIAL - CROSS-SECTIONAL CAPACITY - COLUMN BUCKLING

A dissertation submitted to

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Doctor of Sciences

presented by

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Zurich, March 2013

Jacqueline Pauli
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This thesis analyses the load-carrying behaviour of carbon steel columns in fire based on the material behaviour and the cross-sectional capacity. Extensive experimental investigations on the material behaviour of carbon steel at elevated and high temperatures and on structural stub and slender column furnace tests serve as a background of the work. Whenever necessary and reasonable the experiments are complemented by finite element simulations. The results from the experiments and the finite element analysis (FEA) are compared to common European design models. The Thesis is divided into three main chapters analysing the material behaviour, the cross-sectional capacity and the member stability of carbon steel columns at elevated and high temperatures.

After the introduction the second chapter analyses the material behaviour of carbon steel in steady state conditions at temperatures between 20 °C and 1000 °C. Based on different tensile material coupon test series the influence of the temperature, the strain or heating rate and the metallurgical structure is discussed. The decrease of the strength and stiffness of the material with increasing temperature and/or decreasing strain or heating rates is observed. The overall material behaviour is divided into the ranges of moderate, elevated and high temperatures. In the moderate temperature range below 300 °C the stress-strain relationship is linear-elastic, followed by a yield plateau and strain hardening for large strains. In the elevated temperature range between 300 °C and 600 °C the initial linear-elastic branch is directly followed by a distinct nonlinear strain-hardening behaviour. In the high temperature range above 600 °C the plastic behaviour is mainly governed by a flow plateau of constant stress values, leading to an almost bilinear material behaviour. The experimentally obtained stress-strain relationships at elevated and high temperatures are compared to nonlinear material models from the Eurocode and the Ramberg-Osgood approach. It is shown that models from the Ramberg-Osgood family describe the stress-strain behaviour of carbon steel at elevated temperatures well, but have difficulties describing the almost bilinear behaviour at high temperatures.

The third chapter discusses the cross-sectional capacity of carbon steel sections at elevated and high temperatures. Three different types of cross-sections (square hollow, rectangular hollow and H-shaped) are analysed in pure compression, pure bending and an interaction of axial compression and uniaxial bending. Steady state centrically and eccentrically loaded stub column furnace tests on SHS 160·160·5, RHS 120·60·3.6 and HEA 100 are included in the analysis. Finite element simulations on the same types of cross-sections, but with varying cross-sectional slenderness ratios are presented and compared to the test results. Two common European design approaches, called the carbon steel approach and the stainless steel approach are introduced and included in the study. Both approaches are based on a bilinear material model and use a so-called effective yield strength. While the carbon steel approach mainly uses the stress at 2 % total strain $f_{2.0}$ as the 'effective' yield strength, the stainless steel approach works with the 0.2 % proof stress $f_{p,0.2}$. Both approaches and their differences are explained and the cross-sectional capacities according to both models are determined for pure compression, pure bending and the interaction of axial compression and uniaxial bending. The comparison between the test results, the FEA simulations and the design approaches is presented and discussed at 400 °C and 700 °C, representing the elevated and high temperature ranges. The cross-sectional capacities according to FEA and the
design approaches are determined once using the actual material behaviour resulting from the tensile coupon tests and once using the material model of carbon steel at elevated temperatures presented by the Eurocode. The 'effective' yield strength concept implies a bilinear material model into the design formulations, while the real material behaviour is highly nonlinear, which results in very poor predictions of the cross-sectional resistances of class 1 to 3 sections. While the carbon steel approach overestimates the resistance in the majority of the cases, the stainless steel approach is usually considerably underestimating the cross-sectional resistances. Both approaches work well for class 4 sections.

Based on the cross-sectional capacity the forth chapter analyses the load-bearing capacity of carbon steel columns at elevated and high temperatures in the same way. The load-bearing capacity of steady state furnace tests and finite element simulations is compared to buckling curves of the common European carbon and stainless steel approaches for carbon steel columns of the three types of cross-sections with different cross-sectional and overall slenderness ratios. The comparison is presented and discussed at 400 °C and 700 °C, once using the material behaviour of the tensile material coupon tests and once using the material model of carbon steel at elevated temperatures presented by the Eurocode. The design approaches show difficulties to correctly predict the load-bearing capacity of steel columns with nonlinear material behaviour. Some of these difficulties result from the poor prediction of the cross-sectional capacity. But even if the prediction of the cross-sectional capacity is correct the buckling curves do not correctly describe the decrease of the load-caring behaviour of columns with increasing overall slenderness ratios.

The thesis shows the effect of the nonlinear material behaviour of carbon steel in the range of elevated temperatures (between 300 °C and 600 °C) on the cross-sectional capacity and column buckling and discusses the difficulties of two common European design approaches to correctly predict the ultimate loads of carbon steel cross-sections and columns in fire.


Das dritte Kapitel diskutiert den Querschnittswiderstand von Baustahlquerschnitten bei erhöhten und hohen Temperaturen. Drei verschiedene Querschnittstypen (quadratisches und rechteckiges Hohlprofil und H-Profil) werden bei reiner Druckbelastung, reiner Biegebelastung und einer Interaktion von Druck mit Biegung analysiert. Resultate von stationären zentrisch und exzentrisch belasteten Ofenversuchen zur Ermittlung der Tragfähigkeit von SHS 160·160·5, RHS 120·60·3.6 und HEA 100 Profilen sind in die Analyse integriert. Simulationen mit finiten Elementen derselben Querschnittstypen, jedoch mit variabler Querschnittsschlankheit, werden analysiert und mit den Versuchsresultaten verglichen. Zwei bekannte europäische Berechnungsmodelle, hier Baustahlmodell und Edelstahlmodell genannt, werden vorgestellt und in die Studie integriert. Beide Modelle basieren auf einem bilinearen Materialgesetz und benutzen eine so-gennannte Bemessungsspannung. Während das Baustahlmodell hauptsächlich den Spannungswert bei 2 % Gesamtdehnung $f_{2,0.0}$ anwendet, arbeitet das Edelstahlmodell mit dem Spannungswert bei 0.2 % plastischer Dehnung $f_{0.2,0.2}$. Beide Modelle und ihre Unterschiede werden erklärt und die entsprechenden Querschnittswiderstände bei reiner Druck-, reiner Biege- und einer kombinierten Belastung werden ermittelt. Ein Vergleich zwischen den Resultaten der Versuche, der numerischen Simulationen und der Berechnungsmodelle wird bei 400 °C und 700 °C, stellvertretend für die Bereiche der erhöhten und der hohen Temperaturen, durchgeführt. Die Querschnittswiderstände der numerischen


Die Arbeit zeigt den Einfluss des nichtlinearen Materialverhaltens von Baustahl im Bereich erhöhter Temperaturen (300 °C bis 600 °C) auf die Querschnitttragfähigkeit und den Knickwiderstand und diskutiert die Schwierigkeiten zweier weit verbreiteter europäischer Berechnungsverfahren bei der korrekten Vorhersage der Traglasten von Baustahlquerschnitten und -stützen bei erhöhten Temperaturen.
1 INTRODUCTION

1.1 BACKGROUND

The load-bearing capacity can, in the case of a structural engineering application, be determined on four different levels. The first level analyses the behaviour of the considered material. It forms an upper boundary for the load-bearing capacity attainable at any of the other levels. On the second level, the material is considered as a two-dimensional shape and a cross-section is formed. The load-bearing capacity of a cross-section, hereafter called cross-sectional capacity, can be equal to that of the material determined for a standard test section, but is limited by local buckling for most of the common shapes of cross-sections in steel construction. The third level adds the third geometrical dimension and forms members, for example columns or beams. The load-bearing capacity of a member can reach that of its corresponding cross-section, but member buckling occurs for all but very squat members and reduces their load bearing capacity. The fourth level includes, for example, the members of the structure of a building or bridge and analyses the behaviour of the entire system. Each of these levels is based and is dependent on the lower levels, but adds a new dimension to the problem and has, therefore, to be treated on its own merit.

Elevated temperatures, for example in the case of a building fire, directly influence the material behaviour of carbon steel, i.e. the first of the four levels. The material suffers a loss of strength and stiffness with increasing temperatures and the almost linear elastic, perfectly plastic stress-strain relationship of carbon steel at ambient temperature becomes distinctly non-linear. Thermal creep or stress relaxation occurs in the material at elevated temperatures, leading to strain rate-dependent and heating rate-dependent material properties. The material behaviour then influences the cross-sectional capacity, which again influences the behaviour of the members. Therefore, predicting the behaviour of a steel member in the case of fire requires an understanding of the behaviour at each of the two lower levels as well as the dependencies between the different levels.

Most research projects today focus on the behaviour of only one or maybe two levels. Several larger studies on the material behaviour of carbon steel in fire have been performed in recent decades [Outinen 2007, Wohlfeil 2006 and Twilt 1991]. In addition, many smaller studies have been published including steady-state, transient-state or creep tests on material coupons of carbon steel at elevated temperatures [Qiang et. al. 2012 (2x), Wei & Jihong 2012, Schneider & Lange 2011, Ranawaka & Mahendran 2009, Kirby & Preston 1988, Furumura et. al. 1985 and Fujimoto et. al. 1981]. Other studies contain experimental results for stub or slender column tests at elevated temperatures (level 2 or 3), with insufficient information about the material behaviour [Ala-Outinen & Myllymäki 1995 and Profil Arbed 1995]. Only a few studies are available that analyse the load-bearing capacity of carbon steel members at elevated temperatures including material coupon testing [Outinen et. al. 2001, Poh 1998 and Thor 1973].

The European fire design rules are based on ambient temperature design considering temperature-dependent reduction factors for the strength and the stiffness but do not explicitly include the non-linear...
stress-strain relationship of carbon steel at elevated temperatures. Instead, a bilinear material model with a reduced Young’s Modulus in the elastic range and a reduced yield stress for the yield plateau is used for design purposes. Correction factors are added at the higher levels to minimise the error of this simplification, but the first level is still only partially included in the behaviour at the subsequent levels and no all of the influencing factors are considered.

A similar approach was chosen for the European design rules of stainless steel structures. The stress-strain relationship of stainless steel at ambient temperature exhibits non-linear behaviour not unlike that of carbon steel at elevated temperature. But again, the non-linearity is not explicitly taken into account and the simplified design models to determine the load-bearing capacity at levels two and three do not include all aspects of the material behaviour. There are some differences in the approach of stainless steel design at ambient temperature compared to carbon steel design at elevated temperatures. However, no comparative study has been performed so far to analyse the analogy between the two materials taking into account levels 1 to 3.

1.2 Scope of the Research

The aim of this thesis is to provide a better understanding of the relationships between the material behaviour, the cross-sectional capacity and the load-bearing capacity of members at elevated temperatures. It focuses on plain carbon steel, but includes stainless steel models whenever they provide an additional aspect to the topic. Furthermore, it is limited to the material behaviour in pure tension and to the load-bearing capacity of cross-sections subjected to pure compression, pure bending or an interaction of axial compression and uniaxial bending moments. At the third level columns subjected to axial compression are treated.

The foundation of the thesis is provided by an extensive experimental study on material coupons (level 1), stub (level 2) and slender (level 3) columns and beam-columns. Three different types of cross-section, a square hollow section (SHS), a rectangular hollow section (RHS) and an H-section (HEA) with different slenderness ratios were tested under steady-state conditions. The key factor of this experimental study is the direct comparability of the test results obtained at all three levels of one type of cross-section by ensuring that the material coupon, stub and slender column test specimens are cut from the same steel bars and, therefore, possess identical material behaviour, cross-sectional geometry and residual stress pattern.

At levels two and three the test results are complemented with Finite Element (FE) simulations, providing additional information on the influence of slenderness ratios and different material behaviours. The results of the tests and the simulations at each level are compared to existing design models in common use.

1.3 Outline of the Thesis

This thesis is divided into 5 chapters. After the introduction in Chapter 1, the main body of the work consists of three chapters, followed by the conclusions and the outlook.

Chapter 2 analyses the material behaviour (level 1) of carbon steel at elevated temperatures. The influence of the temperature, the strain rate and the microstructure of steel on the material behaviour is explained. The stress-strain relationships are compared to existing material models for carbon steel, stainless steel and aluminium.

Chapter 3 analyses the cross-sectional capacity (level 2) in pure compression, pure bending and an interaction of axial compression and uniaxial bending moments, based on the findings of the material
behaviour. The stub column test results of all three types of cross-section are compared to numerical simulations with different cross-sectional slenderness ratios. The simulations are executed once using the actual material behaviour from the material coupon tests and once using the material model of the European fire design rules for carbon steel. These simulations provide additional information on the local buckling behaviour of the cross-sections as well as the accuracy of the standardised material model. The results from the tests and the simulations are compared to common European design models for carbon steel in fire and stainless steel at ambient temperature.

Chapter 4 analyses the load-bearing capacity of carbon steel columns (level 3) subjected to axial compression, based on the material behaviour and the cross-sectional capacity. The slender column test results of all three types of cross-section are compared to numerical simulations with different cross-sectional and overall slenderness ratios. The simulations are executed once using the actual material behaviour from the material coupon tests and once using the material model of the European fire design rules for carbon steel. These simulations provide additional information on the column buckling behaviour of the members as well as the accuracy of the standardised material model. The results from the tests and the simulations are compared to common European design models for carbon steel in fire and stainless steel at ambient temperature.

Chapter 5 wraps up the work with the main conclusions and an outlook for further research topics.
2 LEVEL 1: MATERIAL BEHAVIOUR

2.1 INTRODUCTION

The material behaviour is one of the key factors in understanding the load-bearing capacity of cross-sections and members. Without consistent material models, including the main parameters influencing the real material behaviour, it is very difficult to correctly predict the load-bearing capacities at the higher levels.

This chapter first analyses the influence of the temperature, the strain or heating rate and the metallurgical structure on the material behaviour of carbon steel at elevated and high temperatures. It is based on extensive material coupon test series executed by different institutes in Europe and Australia over the past 20 years [Pauli et. al. 2012, Schneider & Lange 2011, Wohlfel 2006 and Poh 1998]. The second part of the chapter compares the stress-strain relationships of the test results to material models of the Eurocode family and the Ramberg-Osgood type.

2.2 INFLUENCE OF THE TEMPERATURE

Figure 2.1 contains six graphs exhibiting the stress-strain relationships of steady-state tensile material coupon tests of Pauli et. al. 2012 (left) and Poh 1998 (right). The test specimens of Pauli et. al. were cut from the flat faces of two hot-rolled box sections (SHS 160x160x5 and RHS 120x60x3.6 of steel grade S355) and the web of a hot-rolled H-section (HEA 100 of grade S355). The specimens of Poh were cut from the flanges of two welded I-sections (700WB130 and 1200WB423 of grades 300 and 400, respectively) and a hot-rolled I-section (360UB50.7 of grade 300 Plus). The tests are described in more detail in Appendix A.

The stiffness of carbon steel in the elastic range is governed by the interatomic forces. An elastic deformation of the metal is defined by the temporary increase or decrease of the interatomic distance. The force necessary to provoke this small deformation is strongly dependent on the bond energy of the atoms. A higher bond energy results in a higher applied force and, therefore, a higher Young's Modulus $E_0$. When the material is heated, the equilibrium distance between the atoms becomes larger and the material expands. The bond energy decreases with the increase of the interatomic equilibrium distance, leading to a decrease in the Young's Modulus as the temperature rises. This loss of stiffness with increasing temperature can be well observed in the test results of Figure 2.1.

A plastic deformation takes place if the critical shear stress within one crystal of the material is exceeded and the dislocations start to migrate. From a microscopic point of view, therefore, the beginning of yielding can be very precisely defined as the start of the migration of the first dislocation within the material.
Figure 2.1  Influence of the temperature on the stress-strain relationships of tensile material coupon tests
Carbon steels show an abrupt initial yielding behaviour at ambient temperature. The carbon atoms work as a barrier to plastic deformation. The stress rises above the elastic limit to a certain peak level, called upper yield point, at which the barrier is overcome and the stress drops almost instantly to the level of the lower yield point. The stress level reached at the upper yield point is influenced strongly by the specimen preparation and testing conditions. After the lower yield point is reached, the stress level oscillates around the value of the lower yield point for a considerable amount of straining, forming the so-called yield plateau. The reason behind the constant stress value is a highly heterogeneous yielding process as different portions of the specimen successively undergo yielding. At the end of the plateau the entire specimen has yielded and the homogeneous strain-hardening process begins. If the temperatures rise, the yield plateau becomes shorter and finally disappears entirely at temperatures between 300 °C and 400 °C (Figure 2.1). The strain hardening behaviour becomes dominant even in the range of strains below 2 %.

The strain hardening process is dominated by the increasing number of dislocations migrating through the grains. As more dislocations are formed that are all oriented in different directions, they start blocking each other and become entangled. These effects strengthen the material and increase the stress level necessary to produce further plastic deformation. At the same time, the so-called dynamic restoration process, composed of dynamic recovery and recrystallisation, starts to work against the strain hardening behaviour. In the case of dynamic recrystallisation new grains nucleate and grow, continually replacing the older deformed grains and softening the material. In the dynamic recovery process, dislocations in all the (old and new) grains annihilate each other and become less frequent, again softening the material. The larger the deformations within the material, the quicker the dynamic recovery and the dynamic recrystallisation processes, while the amount of newly formed dislocations stays constant. At the end of the plateau the entire specimen has yielded and the homogeneous strain-hardening process begins. If the temperatures rise, the thermally agitated dislocation movement becomes easier and faster and less strain hardening is observed. Both the dynamic recovery and recrystallisation processes become more effective and the strength of the material decreases (Figure 2.1, Lankford et. al. 1985 and McQueen & Jonas 1975).

If the temperatures are high enough, the restoration can reach the same rate as the strain hardening. The result is that the hardening and softening of the material balance each other leading to a constant steady-state flow stress value. This flow stress plateau is theoretically reached at the end of every strain hardening process. The ductility of steel at ambient temperature, however, is not high enough to reach this level before fracture takes place. The higher the temperature and the slower the strain rate, the faster the restoration processes can take place and smaller strains are necessary to reach the flow stress plateau.

The stress-strain behaviour of carbon steel with regard to its temperature dependence can be divided into three main domains. The domain of the moderate temperature behaviour covers a temperature range up to 200 °C. It is characterised by a linear-elastic branch followed by a plastic yield plateau and strain hardening behaviour at larger strains. The decrease of both the Young's Modulus representing the stiffness in the elastic range as well as the yield strength of the plateau is only moderate for the Grade 300 and Grade 300 Plus steels. In the case of the Grade 400 steel the increase of the yield strength at room temperature resulting from the quenching and tempering treatment is lost by reheating the steel leading to a greater decrease of the yield strength at 100 °C and 200 °C.

The domain of elevated temperature behaviour covers the temperature range between 300 °C and 600 °C. The linear-elastic branch is significantly shorter than at lower temperatures and the corresponding Young's moduli are lower. At 300 °C and sometimes at 400 °C a small yield plateau can still be present, but the plastic range is mainly governed by a distinct strain hardening behaviour up to strains far larger than 2 %.

The domain of high temperature behaviour covers the temperature range above 600 °C. The linear-elastic branches and their associated Young's Moduli are greatly decreased. A short range of strain hardening behaviour is still present, but the plastic behaviour is mainly governed by a steady-state flow plateau characteristic of the equilibrium between the generation and the annihilation of the dislocations present within the crystal structure of the material. In some cases, even a small decrease of the stress can be observed when the restoration process takes place slightly faster than the strain hardening process.
Figure 2.2  Influence of the strain rate on the stress-strain relationships of tensile material coupon tests
2.3 Influence of the strain / heating rate

At ambient temperature the material behaviour of carbon steel is independent of moderate changes of the strain rate. At elevated and high temperatures, however, the strain rate of an applied deformation has a similar influence on the material behaviour to the temperature itself. If the deformation process is fast, there is no time for recovery and the strain hardening process predominates. At low strain rates, however, the deformation is slow enough for the restoration to take place, weakening the material.

Figure 2.2 shows six graphs containing stress-strain relationships of the same test series presented in Figure 2.1. Each graph has the curves obtained at a single temperature, but at different strain rates. At 400 °C the strain hardening process is predominant and the influence of strain rate on the restoration process does not have any significant effect on the overall behaviour. At temperatures above 500 °C, however, a slower application of the mechanical load (i.e. a lower strain rate) favours the restoration process leading to a value of strain hardening that is balanced sooner, and a steady-state flow plateau that is reached for smaller strains and at a lower stress value.

In natural fire conditions as well as in a transient testing environment, the applied mechanical load is constant, while the temperature increases. Therefore, it is the heating rate instead of the strain rate that influences the mechanical behaviour of carbon steel. The main effects, however, are the same. Slower changes in temperature favour the restoration processes within the material.

2.4 Influence of the metallurgical structure

Figure 2.3 to Figure 2.5 show the stress-strain relationships of the tensile coupon tests of Pauli et al. 2012, Poh 1998. In addition, steady-state tensile material coupon tests performed by Schneider & Lange 2011 and Wohlfel 2006 in Darmstadt, Germany, on specimens of steel grade S460 are included. The measured stress value $\sigma$ for each experiment is divided by its measured 0.2 % proof stress $f_{p,0.2,\theta}$ and the measured strain $\varepsilon$ is divided by the measured total strain at the 0.2 % proof stress, $\varepsilon_{p,0.2,\theta}$.

Figure 2.3 shows the stress-strain relationships in the moderate temperature range below 300 °C. In these graphs the stress-strain relationships of all tests and steel grades coincide to a great extent within the elastic range and the yield plateaus. The onset and shape of the strain hardening branch is different in each of the performed tests. The strain hardening behaviour is mainly governed by the amount and orientation of dislocations, the size and orientation of the grains and the individual phases within the microscopic structure of the steel. These aspects are influenced by the exact chemical composition (not just the content of carbon and the other main alloys) of the steel and the entire fabrication process including the hot-rolling and cooling periods and, therefore, are different for each individual steel bar.

Figure 2.4 shows the stress-strain relationships in the elevated temperature range between 300 °C and 600 °C. The yield plateau disappears and the plastic behaviour of the material is entirely governed by the strain hardening and restoration processes and, therefore, by the crystalline microstructure of the steel. The resulting scatter in the stress-strain relationships is considerable. Nevertheless, the overall shapes of the curves at the same temperature are quite similar. The influence of the strain rate is less significant than that of the microstructure of the material.

Figure 2.5 shows the stress-strain relationships in the high temperature range above 600 °C. The restoration process becomes dominant, leading to steady-state flow-stress plateaus or even a slight decrease in the stress-strain relationship. The influence of the strain rate on the stress level is of about the same magnitude as the influence of the microstructure of the material. One additional possible influence on the stress-strain curves at 700 °C may be the phase transformation from $\alpha$-iron to $\gamma$-iron, theoretically taking place above 723 °C. As no micrographic investigations have been performed on the microstructure of the specimens, no statement can be made regarding the influence of the phase transformation on the stress-strain relationships of the experiments at 700 °C.
Figure 2.3  Schematic illustration of the stress and strain annotations (top left) and stress-strain relationships of individual test results in the moderate temperature range below 300 °C
Figure 2.4  Stress-strain relationships of individual test results in the elevated temperature range between 300 °C and 600 °C
Figure 2.5 Stress-strain relationships of individual test results in the high temperature range above 600 °C
2.5 **Comparison with material models in the relevant Eurocodes**

The Eurocodes contain several material models for steel or aluminium that allow the calculation of the entire non-linear stress-strain curve on the basis of material parameters, such as the Young’s Modulus, the proportional limit or the 0.2 % proof stress (Figure 2.3 top left). These models will first be described and then compared to the test results of Pauli et al. 2012.

### 2.5.1 Carbon and stainless steel at elevated temperature

Eurocode EN1993-1-2 2006, dealing with the structural fire design of steel structures, includes two non-linear material models. The first model describes the stress-strain relationship of carbon steel at elevated temperatures, while the second model can be used to determine the stress-strain relationship of stainless steel at elevated temperatures. The basic structure of the two models is the same, i.e. they both divide the stress-strain relationship into an elastic segment and a plastic segment, using an elliptical curvature to describe the plastic branch (Table 2.1). The model dates back to Rubert & Schaumann 1985.

In the case of carbon steel, the linear elastic branch is defined by the Young’s Modulus $E_{0,\theta}$ up to the proportional limit $\varepsilon_{p,\theta}$. In the case of stainless steel, the model uses an exponential equation to define the slightly curved elastic branch up to the total strain at the 0.2 % proof stress, $\varepsilon_{p,0.2,\theta}$. The initial slope of the curved elastic branch is defined by the Young’s Modulus $E_{0,\theta}$ and the slope at the end of this first segment is defined by the Tangent Modulus $E_{0.2,\theta}$ at the 0.2 % proof stress.

The second segment covers the highly curved plastic range of the stress-strain relationship. In the case of carbon steel, the model defines an elliptic curvature to describe the stress-strain relationship between the proportional limit $\varepsilon_{p,\theta}$ and the end of the curved segment at $\varepsilon_{2.0,\theta} = 2 \%$. The initial slope of the ellipse is defined by the Young’s Modulus $E_{0,\theta}$ and the slope at the end of the second segment is defined by the Tangent Modulus $E_{2.0,\theta} = 0$. A third segment is added to define a constant stress level $\sigma_{f,\theta}$ for strains larger than 2 %. In the case of stainless steel, a similar elliptic branch is used between the total strain at the 0.2 % proof stress, $\varepsilon_{p,0.2,\theta}$ and the total strain at the ultimate stress $\varepsilon_{u,\theta}$ ranging between 15 and 40 %, depending on the steel grade and the temperature. The initial slope of the ellipse is defined by the Tangent Modulus $E_{0.2,\theta}$ and the slope at the end of the second segment is defined by the Tangent Modulus $E_{u,\theta} = 0$.

The parameters used to mathematically describe the elliptic arc are the two end points of the arc (stress and strain value) and the slope of the arc at these points. The starting point of the arc is easily defined for the carbon steel model, using the initial slope $E_{0,\theta}$ and the proportional limit $\varepsilon_{p,\theta}, f_{p,\theta}$. In the case of the stainless steel model, the starting point is defined by the 0.2 % proof stress $\varepsilon_{p,0.2,\theta}$ and the total strain at the ultimate stress $\varepsilon_{u,\theta}$ ranging between 15 and 40 %. The Eurocode gives direct values of $E_{0.2,\theta}$ for different stainless steels and different temperatures. It is not defined how to calculate the $E_{0.2,\theta}$ value from the other material parameters ($E_{0,\theta}, \varepsilon_{p,0.2,\theta}, f_{p,0.2,\theta}$) used in the model.

The end point of the elliptic arc needs the same amount of information as the starting point, i.e. the stress, the strain and the slope. The carbon steel model defines the endpoint at 2 % total strain and fixes the slope to 0. This leads to an enforced high curvature of the elliptic arch up to 2 % strain. At the same time, the model ignores the strain hardening of the material taking place at strains higher than 2 % and, therefore, has difficulties in modelling the exact stress-strain behaviour of an experimentally obtained curve. The stainless steel model defines the end point at the ultimate stress ($\varepsilon_{u,\theta}, f_{u,\theta}$) and again fixes the slope to 0. The strain hardening of the material is considered for the entire stress-strain curve until failure. In cases of elevated temperatures the ultimate stress is measured at strains of 50 % or more. The use of this model to describe an unknown stress-strain behaviour would require experimental data up to these large strain values, which is not generally available. If the material properties are defined not by tension but by compression experiments, the ultimate stress cannot be determined at all.

In Figure 2.6 and Figure 2.7 the experimental stress-strain relationships at 400 °C and 700 °C of the tensile material coupon tests of Pauli et al. 2012 are compared to the different Eurocode models. These
Table 2.1  Selected material models of the Eurocodes EN1993-1-2, EN1993-1-4 and EN1999-1-1

EN1993-1-2: Carbon steel in fire

Segment 1: Linear  \( \sigma = E_0 \cdot \varepsilon \)  for \( \varepsilon \leq \varepsilon_p \)

Segment 2: Elliptic  \( \sigma = \frac{b}{a} \cdot \sqrt{a^2 - (\varepsilon_{2,0} - \varepsilon)^2} + f_p - c \)  for \( \varepsilon_p < \varepsilon \leq \varepsilon_{2,0} \)

Segment 3: Constant  \( \sigma = f_{0,0} \)  for \( \varepsilon > \varepsilon_{2,0} \)

with  
\[ a^2 = \frac{E_0 \cdot (\varepsilon_{2,0} - \varepsilon_p)^2 + c \cdot (\varepsilon_{2,0} - \varepsilon_p)}{E_0} \]
\[ b^2 = E_0 \cdot c \cdot (\varepsilon_{2,0} - \varepsilon) + c^2 \]
\[ c = \frac{(f_{2,0} - f_p)^2}{2 \cdot (f_p - f_{2,0}) + E_0 \cdot (\varepsilon_{2,0} - \varepsilon_p)} \]

EN1993-1-2: Stainless steel in fire

Segment 1: Exponential  \( \sigma = \frac{E_0 \cdot \varepsilon}{1 + a \cdot \varepsilon^b} \)  for \( \varepsilon \leq \varepsilon_{p,0,2} \)

Segment 2: Elliptic  \( \sigma = f_{p,0,2} - e + d \cdot \sqrt{c^2 - (\varepsilon_u - \varepsilon)^2} \)  for \( \varepsilon_{p,0,2} < \varepsilon \leq \varepsilon_u \)

with  
\[ a = \frac{E_0 \cdot \varepsilon_{p,0,2} - f_{p,0,2}}{f_{p,0,2} \cdot \varepsilon_{p,0,2}} \]
\[ b = \frac{(1 - \varepsilon_{p,0,2} \cdot E_{0,2}/f_{p,0,2}) \cdot E_0 \cdot \varepsilon_{p,0,2}}{(E_0 \cdot \varepsilon_{p,0,2}/f_{p,0,2} - 1) \cdot f_{p,0,2}} \]
\[ c^2 = (\varepsilon_u - \varepsilon_{p,0,2})(\varepsilon_u - \varepsilon_{p,0,2} + \frac{e}{E_{0,2}}) \]
\[ d^2 = e \cdot (\varepsilon_u - \varepsilon_{p,0,2}) \cdot E_{0,2} + e^2 \]
\[ e = \frac{(\sigma_u - \sigma_{p,0,2})^2}{(\varepsilon_u - \varepsilon_{p,0,2}) \cdot E_{0,2} - 2(\sigma_u - \sigma_{p,0,2})} \]
\[ E_{0,2} = k_{E,0,2} \cdot E_0 \]
Comparison with material models in the relevant Eurocodes

EN1993-1-4: Stainless steel at ambient temperature

Segment 1: Exponential  
\[ \varepsilon = \frac{\sigma}{E_0} + \varepsilon_{0.2} \left( \frac{\sigma}{f_{p,0.2}} \right)^n \]  
for \( \sigma \leq f_{p,0.2} \)

Segment 2: Exponential  
\[ \varepsilon = \varepsilon_{0.2} + \frac{f_{p,0.2}}{E_0} + \frac{\sigma - f_{p,0.2}}{E_{0.2}} + \varepsilon_u \left( \frac{\sigma - f_{p,0.2}}{f_u - f_{p,0.2}} \right)^m \]  
for \( f_{p,0.2} < \sigma \leq f_u \)

with  
\[ n = \frac{\ln(20)}{\ln(f_{p,0.2}/f_{p,0.01})} \]
\[ m = 1 + 3.5 \cdot \frac{f_{p,0.2}}{f_u} \]
\[ E_{0.2} = \frac{E_0}{1 + \varepsilon_{0.2} \cdot n \cdot E_0/f_{p,0.2}} \]

EN1999-1-1: Aluminium at ambient temperature - Model 1

Segment 1: Linear  
\[ \sigma = E_0 \cdot \varepsilon \]  
for \( \varepsilon \leq 0.5 \varepsilon_{e,0.2} \)

Segment 2: Polynomial  
\[ \sigma = f_{p,0.2} \left( -0.2 + 1.85 \cdot \frac{\varepsilon}{\varepsilon_{e,0.2}} - \left( \frac{\varepsilon}{\varepsilon_{e,0.2}} \right)^2 + 0.2 \cdot \left( \frac{\varepsilon}{\varepsilon_{e,0.2}} \right)^3 \right) \]  
for \( 0.5 \varepsilon_{e,0.2} < \varepsilon \leq 1.5 \varepsilon_{e,0.2} \)

Segment 3: Hyperbolic  
\[ \sigma = f_{p,0.2} \left( \frac{f_u}{f_{p,0.2}} - 1.5 \cdot \left( \frac{f_u}{f_{p,0.2}} - 1 \right) \cdot \frac{\varepsilon_{e,0.2}}{\varepsilon} \right) \]  
for \( 1.5 \varepsilon_{e,0.2} < \varepsilon \leq \varepsilon_u \)

EN1999-1-1: Aluminium at ambient temperature - Model 2

Segment 1: Exponential  
\[ \varepsilon = \frac{\sigma}{E_0} + \varepsilon_{0.2} \left( \frac{\sigma}{f_{p,0.2}} \right)^n \]

with  
\[ n = \frac{\ln(\varepsilon_{0.2}/\varepsilon_u)}{\ln(f_{p,0.2}/f_u)} \]
two temperatures represent the two ranges of elevated and high temperatures showing different material behaviours. The calculated curve, according to EN 1993-1-2 2006, is presented for carbon steel (CS) by a long-dashed line, while for stainless steel (SS) it is represented by a dashed-dotted line. The measured values of Young's Modulus $E_{0,θ}$, proportional limit $f_{p,θ}$, and stress at 2 % total strain $f_{2,0,θ}$ of the experiments were used to calculate the carbon steel model for each material, temperature and strain rate. At 400 °C the curvature of the carbon steel model up to a strain of 2 % is too severe, overestimating the real stress-strain relationship. Beyond 2 % strain, the stress level of the model stays constant, underestimating the true capacity of the material. At 700 °C the strength of the material is underestimated by the carbon steel model for strains below 2 %.

In the stainless steel model, the measured Young's Modulus $E_{0,θ}$ and 0.2 % proof stress $f_{p,0.2,θ}$ of the experiments could be directly used for each material, temperature and strain rate. The slope at the beginning of the elliptic arc, $E_{0.2,θ}$, is given in EN 1993-1-2 2006 as the product of a reduction factor $k_{E,0.2}$ and the Young's Modulus $E_{0,θ}$. This reduction factor is defined for different stainless steel grades and temperatures, but is not directly applicable for carbon steel. Therefore, $E_{0.2,θ}$ was calculated using the model of EN 1994-1-4 2007 for stainless steel at ambient temperature (see below). The endpoint of the elliptic arch of the model is defined at the ultimate load $f_{u,θ}$. These values were not available from the experimental data and the stress at 5 % total strain $f_{5,0,θ}$ was used instead. The calculated curves fit the experimental results better than those obtained with the carbon steel model, but the stress level at 400 °C is still slightly overestimated, because the slope of the predicted curvature decreases to 0 at the end of the elliptic arc.

The main problem of the elliptic approach of EN1993-1-2 is the fact that, in addition to two points on the stress-strain relationship, it is necessary to know the slope of the curve at these points and that these slopes cannot be calculated independently of the model parameters. Therefore, the model sets the slope at the end of the elliptic arc to 0. If this point is set at low strain levels of 2 to 5 % the ultimate stress of the model is attained too soon. If, on the other hand, the endpoint of the ellipse is assumed to coincide with the ultimate load from the experiment, very large strains (and therefore large amounts of test data) are necessary. Either way, the curvature of the model is predefined by the ellipse and cannot be adjusted to the individual test results. The model is mathematically simple but difficult to apply to experimentally obtained stress-strain relationships.

### 2.5.2 Stainless steel at ambient temperature

Eurocode EN1993-1-4 2007 contains the supplementary rules for stainless steel structures and presents a model to describe the stress-strain relationship of stainless steel at ambient temperature. The model is based on the extended Ramberg-Osgood approach as defined by Mirambell & Real 2000 (see below). It divides the stress-strain relationship into two segments using exponential formulations with different exponents to adjust the curvature (Table 2.1). The first segment describes the material behaviour of the stainless steels up to the 0.2 % proof stress $f_{p,0.2}$. The initial slope of the curved line is defined by the Young's Modulus $E_{0,θ}$. The exponent $n$ of the first segment is a function of the 0.2 % proof stress and the 0.01 % proof stress. The initial slope of the second segment between the 0.2 % proof stress and the ultimate strength $f_u$ is defined by the Tangent Modulus $E_{0.2,θ}$ at the 0.2 % proof stress. The exponent $m$ is a function of the 0.2 % proof stress and the ultimate strength $f_u$.

The parameters needed to mathematically describe the two functions are the three points on the stress-strain curve (stress and strain value), one within the first segment, one at the intersection of the two segments and the third at the end of the second segment. The first fixed point is the 0.01 % proof stress $f_{p,0.01,θ}$. The use of the 0.01 % proof stress is not very common and information on this material parameter may not be available from a test series. The second fixed point is the 0.2 % proof stress $f_{p,0.2,θ}$. The use of this parameter is very common and no problems should occur from its application. The third fixed point is the ultimate stress $f_{u,θ}$. As in case of the EN1993-1-2 models, the ultimate stress may not be available due to the experimental setup (no ultimate stresses can be derived from compressive tests) or the very large strains needed in tensile testing to reach the ultimate load.
This model is compared to the test results in Figure 2.6 and Figure 2.7. The calculated curve according to EN 1993-1-4 for stainless steel (SS) is presented by a dashed-triple-dotted line. The measured Young's Modulus $E_{0,θ}$ and 0.2% proof stress $f_{p,0.2,θ}$ in the experiments can be directly used for each material, temperature and strain rate. The slope at the beginning of the elliptic arc, $E_{0.2,θ}$, and the two exponents $n$ and $m$ can be calculated independently of the model parameters. The 0.01% proof stress $f_{p,0.01,θ}$ and the ultimate stress $f_{u,θ}$ were replaced by the proportional limit $f_{p,θ}$ and the stress at 5% total strain $f_{5.0,θ}$, respectively, as these values were available from the test results. The calculated curves fit the experimental data well. Even if the ultimate stress has been replaced by the stress at 5% total strain, the shape of the curve does not change as much as it did in the case of the elliptic model, because it only alters the location of the fixed point of the model, but not the slope of the curve. In addition, the model's two exponents $n$ and $m$ permit for an easy adaptation of the curvature to any experimental stress-strain curve.

2.5.3 Aluminium at ambient temperature

Eurocode EN1999-1-1 2010 contains the general rules of aluminium structures and presents two models to describe the stress-strain relationship of aluminium at ambient temperature. The first model divides the stress-strain relationship into three segments (Table 2.1, Aluminium model 1). The first segment covers the linear-elastic range defined by the Young’s Modulus $E_0$ and $0.5\cdot e_{e,0.2}$. The second segment uses a polynomial formulation to describe the curvature of the stress-strain relationship up to $1.5\cdot e_{e,0.2}$. Beyond this point the third segment describes the curvature up to the total strain at the ultimate strength $e_u$ using a hyperbolic formulation. To divide into three segments, only the elastic strain value at the 0.2% proof stress $e_{e,0.2}$ is necessary. The first segment is a linear-elastic branch that is easily calculated. The second segment uses a 3rd degree polynomial formulation as a function of $e_{e,0.2}$ and $f_{p,0.2,θ}$ that is also easily applicable. The constant factors in front of each term can be used to fit the equation to an experimentally obtained stress-strain relationship. The third segment uses a hyperbolic formulation as a function of $e_{e,0.2}$, $f_{p,0.2,θ}$ and $f_{u}$. Again, the factors in front of the terms can be used to fit the model to an experimental result. Between the second and third segment, the continuity of the calculated stress-strain curve is uncertain, making the model difficult for use in finite-element simulations. Again, the model is compared to the test results in Figure 2.6 and Figure 2.7. The calculated curve (Alu 1) is presented by a short-dashed line. The ultimate stress $f_{u,θ}$ was replaced by the stress at 5% total strain $f_{5.0,θ}$. The replacement of this parameter proved a problem, as the slope is again set to 0 at this point leading a rather severe curvature and overestimating the stress values of the experimental curves considerably for 400 °C.

The second model describes the stress-strain relationship with a single exponential formulation, based on the original equation by Ramberg & Osgood 1943 and its modification by Hill 1944 (see below). A logarithmic relation between the 0.2% proof stress $f_{p,0.2}$ and a second proof stress on the curve $f_{p,x}$ is used to obtain the exponent $n$ (Table 2.1, Aluminium model 2). To calculate the experimental curves presented in Figure 2.6 and Figure 2.7, the 1.0% proof stress $f_{p,1.0,θ}$ was used as a second fixed point on the curve for the exponent $n$. The resulting curve is represented in the graphs by a dotted line. Like the Ramberg-Osgood-based model of EN1993-1-4 describing the stainless steel behaviour at ambient temperature, the fit of the calculated curve with the experimental results is good. This model for aluminium is easy to calculate as it describes the entire curve in one single segment. On the other hand, the model fits an experimentally obtained stress-strain relationship not quite as well as the stainless steel model.

2.5.4 Conclusions

The five material models of Eurocodes EN1991 to EN1999 use different underlying mathematical formulations to describe a non-linear stress-strain relationship. The 'ideal' model should be easily calculable, be based on commonly used and available material parameters, show no discontinuities at the intersections of the different segments and be adaptable to all the different non-linear shapes of the stress-strain relationship of any given material. All of these requirements are answered by the two exponential models of EN1993-1-4 and EN1999-1-1 model 2. Both models are based on the original Ramberg-Osgood approach that will be described in more detail in the following paragraphs.
Figure 2.6  Comparison of the tensile test results to the material models of the Eurocode at 400 °C
Comparison with material models in the relevant Eurocodes

Figure 2.7  Comparison of the tensile test results to the material models of the Eurocode at 700 °C
2.6 The Ramberg-Osgood Approach

A commonly used approach to describe the stress-strain relationship of stainless steels and aluminium for structural applications is a family of equations usually referenced to as different versions of the Ramberg-Osgood model. All of these equations describe the strain as an exponential function of the stress. The simplest form (the original Ramberg-Osgood equation and its modification by Hill) only requires the Young’s Modulus $E_0$ and one additional known stress value on the curve together with the exponent $n$ to describe the overall behaviour of the material. Recent versions of the model include additional stress values and a second exponent, leading to a better fit of the computed curves to experimental data at the cost of a more complicated mathematical solution.

2.6.1 Historical Overview

Holmquist & Nadai 1939 proposed a formulation to describe the stress-strain relationship of metals exhibiting non-linear material behaviour as an exponential function in relation of the proportional limit $f_p$ and the 0.2 % proof stress $f_{p,0.2}$ with the corresponding (total) strain $\varepsilon_{p,0.2}$.

$$\varepsilon = \frac{\sigma}{E_0} + \varepsilon_{p,0.2} \left( \frac{\sigma - f_p}{f_{p,0.2} - f_p} \right)^n$$

for $\sigma > f_p$

The actual shape of the stress-strain relationship is defined by the exponent $n$, which has to be determined individually for each material. The difficulty at that time of solving this mathematical equation for the exponent $n$ made researchers look for a simpler model with less parameters.

Ramberg & Osgood 1943 proposed an equation similar in shape to that of Holmquist-Nadai, but solvable with only 3 parameters. Again, an exponential function was used to describe the curved shape of the stress-strain relationship, but this time only the Young’s Modulus $E_0$ together with two constants $K$ and $n$ were used. Hill 1944 presented a first modification only one year later importing into the formula of Ramberg-Osgood the concept of using the 0.2 % proof stress $f_{p,0.2}$ replacing the Young’s Modulus in the second part of the equation and replacing the constant $K$ by the corresponding plastic strain $\varepsilon_{0.2}$.

$$\varepsilon = \frac{\sigma}{E_0} + k\left( \frac{\sigma}{E_0} \right)^n$$

$$\varepsilon = \frac{\sigma}{E_0} + \varepsilon_{0.2} \left( \frac{\sigma}{f_{p,0.2}} \right)^n$$

This equation of Hill is usually referred to as the basic Ramberg-Osgood model. It was only superseded, when modern computing techniques simplified the solving of equations having additional parameters. However, the basic idea of the exponential approach survived.

Mirambell & Real 2000 adopted the equation of Hill for the initial part of the stress-strain relationship, where $f \leq f_{p,0.2}$. For the second part of the stress-strain relationship covering the range of $f > f_{p,0.2}$ they proposed a new formula similar to that of Holmquist-Nadai. The basic idea behind this second formula was to move the origin of the curve to the point $(\varepsilon_{p,0.2} ; f_{p,0.2})$ and to use the slope of the curve at this point $E_{0.2}$ as Tangent Modulus. The second reference point needed on the curve is defined by the ultimate stress $f_u$ and its corresponding plastic strain $\varepsilon_{p,u}$. A different exponent $m$ is used in this second equation to describe the shape of the stress-strain relationship beyond $f_{p,0.2}$.

$$\varepsilon = \frac{\sigma}{E_0} + \varepsilon_{0.2} \left( \frac{\sigma}{f_{p,0.2}} \right)^m$$

for $\sigma \leq f_{p,0.2}$
The Ramberg-Osgood approach

\[ \varepsilon = \frac{\sigma - f_{p,0.2}}{E_{0.2}} + \varepsilon_{p,0.2}\left(\frac{\sigma - f_{p,0.2}}{f_u - f_{p,0.2}}\right)^n + \varepsilon_{p,0.2} \quad \text{for } \sigma > f_{p,0.2} \]

The introduction of the second formula improved the agreement of the computed stress-strain relationships with test results. The use of the ultimate strength \( f_u \), however, limits the application of the formula to tensile applications only.

Gardner & Nethercot 2004 modified the second equation of Mirambell-Real to make it applicable to tension and compression applications by including a second offset stress \( f_{p,1.0} \) instead of the ultimate stress \( f_u \).

\[ \varepsilon = \frac{\sigma}{E_0} + \varepsilon_{0.2}\left(\frac{\sigma}{f_{p,0.2}}\right)^n \quad \text{for } \sigma \leq f_{p,0.2} \]

\[ \varepsilon = \frac{\sigma - f_{p,0.2}}{E_{0.2}} + \left(\varepsilon_{p,1.0} - \varepsilon_{p,0.2} - \frac{f_{p,1.0} - f_{p,0.2}}{E_{0.2}}\right) \cdot \left(\frac{\sigma - f_{p,0.2}}{f_{p,1.0} - f_{p,0.2}}\right)^m + \varepsilon_{p,0.2} \quad \text{for } \sigma > f_{p,0.2} \]

### 2.6.2 Comparison with the test results

The applicability of the different formulations of the Ramberg-Osgood approach to describe the stress-strain relationship of the carbon steel elevated temperature tensile coupon tests of Pauli et. al. 2012 has been tested. The measured Young's Modulus \( E_{0,\theta} \), the proportional limit \( f_{p,\theta} \), the 0.2 % proof stress \( f_{p,0.2,\theta} \) and the 1.0 % proof stress \( f_{p,1.0,\theta} \) were integrated into the equations for each material, temperature and strain rate. If the ultimate stress \( f_{u,\theta} \) was necessary, it was replaced by the measured stress at 5 % total strain \( f_{5.0,\theta} \). The method of least squares was used to compute the best-fit exponents \( n \) and \( m \) of each Ramberg-Osgood equation for each test result. These best fit exponents are summarised in Table 2.2.

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<th>Section</th>
<th>Temperature [°C]</th>
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<th>Ramberg-Osgood</th>
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Figure 2.8  Comparison of the tensile test results of Pauli et. al. to the Ramberg-Osgood approach at 400 °C
Figure 2.9  Comparison of the tensile test results of Pauli et al. to the Ramberg-Osgood approach at 700 °C
In Figure 2.8 and Figure 2.9 the computed stress-strain relationships of the best fits of the different formulations of the Ramberg-Osgood approach are plotted against the experimental data of Pauli et. al. at 400 °C and 700 °C, respectively.

The model of Holmquist-Nadai for \( \sigma > f_p \), represented by long-dashed lines, underestimates the experimental stress-values for small plastic strains, but overestimates the stress for total strains larger than about 4%. At a temperature of 700 °C, where the experimental data shows a decline in the stress values with increasing strain, the model overestimates the stress values.

The model of Ramberg-Osgood as modified by Hill, represented by dashed-dotted lines, shows excellent agreement with the experimental data at 400 °C. At 700 °C it proved difficult to determine a best-fit exponent \( n \) for the entire curve. Therefore, the exponent was fitted only to the initial 1% strain of the curve. The resulting calculated stress-strain relationship considerably overestimates the strength of the material for larger strains.

The model of Mirambell-Real, represented by short-dashed lines, shows good agreement with the experimental data for \( \sigma \leq f_{p,0.2} \). For \( \sigma > f_{p,0.2} \) and 400 °C the curvature of the model seems to be too severe, resulting in an overestimation of the stress values for smaller strains and an underestimation of the stresses for large strains. This, of course, is due to the fact that for the computation the ultimate stress \( f_{u,0} \) of the model has been replaced by the stress at 5% total strain \( f_{5.0,0} \). For \( \sigma > f_{p,0.2} \) and 700 °C the severe curvature of the model fits the experimental data better than the two preceding models.

The model of Gardner-Nethercot, represented by dotted lines, shows the best agreement with the experimental data of all models.

Summarising, it may be stated that the model of Holmquist-Nadai and the model of Mirambell-Real present difficulties in exactly representing the experimental curves (as in this case no ultimate stress \( f_{u,0} \) was available). The model of Ramberg-Osgood shows excellent agreement with the experimental results at 400 °C but difficulties arise in describing the severe curvature at 700 °C. The model of Gardner-Nethercot shows excellent agreement with all experimental stress-strain relationships at 400 °C but again it proves difficult to describe the severe curvature at 700 °C.

2.7 Conclusions

The material behaviour of carbon steel in fire is influenced by the temperature, the strain and heating rates and the metallurgical structure. Three different ranges of temperatures can be defined.

In the range of moderate temperatures below 300 °C, the stress-strain relationship consists of a linear elastic branch, followed by a yield plateau and pronounced strain hardening at larger strains. The stiffness and the yield strength decrease slightly and the plateau becomes shorter with increasing temperatures. The strain rate has no significant influence on the strength and the stiffness. The steel microstructure influences the onset and shape of the strain hardening behaviour. The commonly used bilinear elastic, perfectly plastic material model for the ambient temperature design of carbon steel describes the actual behaviour very well.

In the range of elevated temperatures between 300 °C and 600 °C, the stress-strain relationship is governed by a strong strain hardening behaviour after a shorter linear elastic branch at the beginning. Increasing temperatures and decreasing strain rates have similar effects on the material behaviour. The strength and the stiffness decrease and the strain hardening is less pronounced. The influence of the strain rate, however, is observed only at temperatures of 500 °C and higher. The steel microstructure influences the shape of the strain hardening behaviour. If different steels are compared with each other the influence of the different microstructures is higher than that of the strain rate. The material model of the European fire design rules for carbon steel has difficulties in describing the stress-strain relationships from tensile tests, because it overestimates the strain hardening for strains smaller than 2% and underestimates it for larger strains. The shape of the modelled curve cannot be adapted to individual
stress-strain relationships of the experimental results of different steels, temperatures and strain rates. The one-stage Ramberg-Osgood model and its modification by Gardner-Nethercot, on the other hand, allow for a precise modelling of experimentally obtained individual stress-strain relationships of different steels, temperatures and strain rates.

In the range of high temperatures above 600 °C, the stress-strain relationship exhibits an almost bilinear shape again. The short linear-elastic branch is followed by a small curved segment of strain hardening and a predominantly steady-state flow plateau. Increasing temperatures and decreasing strain rates result in decreasing strength and stiffness, but do not significantly influence the shape of the stress-strain relationship. The steel microstructure, however, influences the steady-state flow plateau, which is not always horizontal, but can be slightly ascending or descending for individual test results. The tested material models of the Eurocode or the Ramberg-Osgood family all show difficulties in describing the severe curvature and the almost bilinear shape of carbon steel at these temperatures. A simple bilinear material model similar to that at ambient temperatures would probably work better here.
3 LEVEL 2: CROSS-SECTIONAL CAPACITY

3.1 INTRODUCTION

In Chapter 2 the material behaviour of carbon steel at different temperatures was analysed and divided into the domains of moderate, elevated and high temperatures. This chapter now discusses the influence of this temperature-dependent material behaviour on the load-bearing capacity of common, standardised European carbon steel sections. It is divided into three main parts analysing the cross-sectional capacity for pure compression, pure bending about one of the two major axes of the section and for interaction between axial compression and uniaxial bending.

The analysis is based on an extensive experimental study on stub columns at elevated and high temperatures executed at the ETH Zurich. These tests were performed on three different cross-sections (Figure 3.1), namely a square hollow section (SHS 160 x 160 x 5), a rectangular hollow section (RHS 120 x 60 x 3.6) and an H-section (HEA 100) at 20 °C, 400 °C, 550 °C and 700 °C and at a strain rate of 0.10 %/min. The compressive load was applied to the stub columns both centrically and eccentrically. The tests are described in more detail in Appendix A and in Pauli et. al. 2012.

Different models exist in the literature for determining the load-bearing capacity of steel sections or individual plates with non-linear material behaviour (Somaini 2012, Quiel & Garlock 2010, Niederegger 2009, Heidarpour & Bradford 2008 / 2007, Knobloch 2007, Ashraf 2006, Gardner 2002, Ranby 1999 and Huck 1993). Here the test results are only compared to finite element simulations and two existing basic models to analytically determine the cross-sectional capacity of steel sections in structural engineering. These two concepts are based on the ambient temperature behaviour of carbon steel and assume bilinear material behaviour with constant effective yield strength in the plastic range. The first model is used in fire design of carbon steel structures and will be referred to as the carbon steel approach (CSA). The second model is commonly used in stainless steel design at ambient temperature and can be adopted for carbon steel in fire. It will be called hereafter the stainless steel approach (SSA).

Both models are based on the non-dimensional cross-sectional slenderness ratio at ambient temperature $\bar{\lambda}_{p,20^\circ C}$ and the cross-sectional classification system. The non-dimensional cross-sectional slenderness ratio at ambient temperature is defined as

$$\bar{\lambda}_{p,20^\circ C} = \frac{h}{t_{\text{f}} \text{ or } b}{\text{t}_{\text{f}}} \frac{\varepsilon}{28.4 \cdot \varepsilon \cdot k_{\sigma}}, \quad \text{with}$$

$$k_{\sigma} = \begin{cases} 4 & \text{for internal compression parts} \\ 0.426 & \text{for outstand flanges} \end{cases}$$

$$\varepsilon = \sqrt{\frac{235}{f_y,20^\circ C}}$$
and includes the geometry (h/t or b/t, Figure 3.2), the material (28.4·ε) and the boundary conditions of those plates of the section that are subjected to compressive stresses (kσ). The cross-sectional classification system of EN 1993-1-1 2005 defines four different classes according to the cross-sectional slenderness ratio of a section:

"Class 1 cross-sections are those which can form a plastic hinge with the rotation capacity required from plastic analysis without reduction of the resistance.
Class 2 cross-sections are those which can develop their plastic moment resistance, but have limited rotation capacity because of local buckling.
Class 3 cross-sections are those in which the stress in the extreme compression fibre of the steel member assuming an elastic distribution of stresses can reach the yield strength, but local buckling is liable to prevent development of the plastic moment resistance.
Class 4 cross-sections are those in which local buckling will occur before the attainment of yield stress in one or more parts of the cross-section."

Table 3.1  Resistance to pure compression according to the carbon and stainless steel approaches

<table>
<thead>
<tr>
<th>Class</th>
<th>Ambient temperature carbon steel</th>
<th>CSA</th>
<th>SSA</th>
</tr>
</thead>
<tbody>
<tr>
<td>Class 1 to 3</td>
<td>N_{pl,CS,20°C} = f_{y,20°C} · A_0</td>
<td>N_{pl,CS,0} = f_{20,0} · A_0</td>
<td>N_{pl,SS,0} = f_{p,0.2} · A_0</td>
</tr>
<tr>
<td>Class 4</td>
<td>N_{eff,CS,20°C} = f_{y,20°C} · A_{eff}</td>
<td>N_{eff,CS,0} = f_{p,0.2,0} · A_{eff}</td>
<td>N_{eff,SS,0} = f_{p,0.2,0} · A_{eff}</td>
</tr>
</tbody>
</table>

\[ A_{eff} = A_0 - (1 - \rho) \cdot b_{comp} \cdot t \]

<table>
<thead>
<tr>
<th>internal compression parts</th>
<th>[ \rho = \frac{\lambda_p - 0.055(3 + \psi)}{\lambda_p} \leq 1.0, \quad \psi = 1.0 ]</th>
</tr>
</thead>
<tbody>
<tr>
<td>outstand flanges</td>
<td>[ \rho = \frac{\lambda_p - 0.188}{\lambda_p} \leq 1.0 ]</td>
</tr>
</tbody>
</table>

with \( b_{comp} \): width of class 4 compression parts

\[ \rho = - \frac{0.772}{\lambda_p} - \frac{0.125}{\lambda_p} \leq 1.0 \]

\[ \rho = \frac{1}{\lambda_p} - \frac{0.231}{\lambda_p} \leq 1.0 \]
3.2 Pure compression

Figure 3.3 provides a schematic illustration of the cross-sectional classification system for plates subjected to pure compression according to the carbon steel approach (CSA) and stainless steel approach (SSA). The ambient temperature carbon steel concept is added for comparison. The cross-sectional slenderness ratios of the tested cross-sections are indicated as well.

The HEA 100 section is very compact and belongs to class 1 according to all three models. However, the SHS 160·160·5 and RHS 120·60·3.6 are class 2 for carbon steel at ambient temperature, class 3 (on the boundary to class 4) in the carbon steel approach and even class 4 in the stainless steel approach.

Both the carbon and the stainless steel approach are based on the ambient temperature carbon steel cross-sectional capacity and allow compact and semi-compact cross-sections (classes 1 to 3) to reach a plastic resistance defined as the product of the cross-sectional area and an 'effective yield strength'. The effective yield strength of the carbon steel approach is defined as the strength at 2 % total strain $f_{2.0}$, while the stainless steel approach uses the 0.2 % proof stress $f_{p,0.2}$ (Table 3.1 and Figure 3.3). The second difference between the two approaches is the non-dimensional cross-sectional slenderness ratio defining the boundary between classes 3 and 4 (Table 3.2). The resistance of slender cross-sections (class 4) is

Table 3.2 Boundary values of $\overline{\lambda}_{p,20°C}$ between the cross-sectional classes

<table>
<thead>
<tr>
<th>Model</th>
<th>Internal compression parts</th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Class 1/2</td>
<td>Class 2/3</td>
<td>Class 3/4</td>
<td>Class 1/2</td>
<td>Class 2/3</td>
</tr>
<tr>
<td>Ambient temperature carbon steel</td>
<td>0.58</td>
<td>0.67</td>
<td>0.74</td>
<td>0.49</td>
<td>0.54</td>
</tr>
<tr>
<td>CSA</td>
<td>0.49</td>
<td>0.57</td>
<td>0.63</td>
<td>0.41</td>
<td>0.46</td>
</tr>
<tr>
<td>SSA</td>
<td>0.45</td>
<td>0.47</td>
<td>0.54</td>
<td>0.54</td>
<td>0.56</td>
</tr>
</tbody>
</table>

Figure 3.3 Schematic illustration of the cross-sectional resistance to pure compression for internal compression parts (left) and outstand flanges (right) according to the carbon and stainless steel approaches (CSA and SSA)
Figure 3.4  True stress-strain relationships of material coupon tests and stub column tests on the HEA 100 sections compared to the bilinear material models of the carbon and stainless steel approaches
Introduction

Figure 3.5 True stress-strain relationships of material coupon tests and stub column tests on the SHS 160·160·5 sections compared to the bilinear material models of the carbon and stainless steel approaches.
Figure 3.6 True stress-strain relationships of material coupon tests and stub column tests on the RHS 120·60·3.6 sections compared to the bilinear material models of the carbon and stainless steel approaches.
in both approaches determined with the effective width method reducing the cross-sectional area by the factor \( \rho \) in relation to the cross-sectional slenderness ratio. The carbon steel approach also reduces the strength of the material to the 0.2\% proof stress \( f_{p,0.2,\theta} \). This leads to a discontinuity of the resistance to pure compression in relation to the slenderness ratio (Figure 3.3).

Figure 3.4 shows four graphs containing true stress-strain curves of the tensile material coupon tests (continuous lines) and the centrically loaded stub column tests (long dashed lines) of the HEA 100 section at four different temperatures. The HEA 100 is a very stocky section and the stub column tests at 20 \(^\circ\)C and 550 \(^\circ\)C, where the ultimate strength is reached at strains of 3 to 4\%, prove the high capacity of plastification of such sections. At 400 \(^\circ\)C the stub column test had to be aborted before the ultimate load was reached, but still it can be assumed that strains of 3 to 4\% would have been reached at the ultimate load as well. At 700 \(^\circ\)C the material behaviour of declining stresses after about 0.5\% of strain is also obtained in the stub column test. But even here, the stress level remains almost constant and significant plastification can take place before the strength drops too far. Therefore, the high capacity for plastification of stocky sections such as the HEA 100 lead to cross-sectional capacities under pure compression higher than the different effective yield strengths \( f_{p,0.2,\theta} \) and \( f_{2.0,\theta} \) (short-dashed lines) adopted in the cross-sectional capacity approaches described above.

Figure 3.5 and Figure 3.6 show four graphs each containing true stress-strain curves for the tensile material coupon tests (continuous lines) and the centrically loaded stub column tests (long dashed lines) of the SHS 160-160-5 and the RHS 120-60-3.6 sections at four different temperatures. These two sections have a higher cross-sectional slenderness ratio than the HEA 100 and the stub column tests exhibit ultimate loads at strains between 0.3\% and 1.1\%. At ambient temperature, the ultimate strength of the stub column tests coincides with the yield strength \( f_{y,20^\circ\text{C}} \). At 400 \(^\circ\)C and 500 \(^\circ\)C the ultimate strengths of the stub column tests lie between the effective yield strengths \( f_{p,0.2,\theta} \) and \( f_{2.0,\theta} \). At 700 \(^\circ\)C the values of \( f_{p,0.2,\theta} \) and \( f_{2.0,\theta} \) are very close together and the ultimate loads of the stub column tests are very close to these two values.

The cross-sectional slenderness ratio has a strong influence on the capacity for plastification of the section and, therefore, on the amount of deformation of the section at the ultimate load. With a non-linear stress-strain relationship different deformation capacities lead to different stress values. Rather than having a constant yield strength reached by the majority of sections with a bilinear material behaviour, the non-linear stress-strain relationship implies a different ultimate stress for every single cross-section, depending of its overall geometry and the slenderness ratios of the individual plates. The influences of the geometry and the material behaviour on the cross-sectional capacity of steel sections at elevated and high temperatures will be further analysed in the following paragraphs.

### 3.2.1 Influence of the Slenderness Ratio and the Material Behaviour

The following analysis is based on the test results and finite element simulations to gain information for different slenderness ratios in addition to those of the test specimens. The FE analysis was limited to three types of cross-section including a square hollow section (SHS), a rectangular hollow section (RHS) with an aspect ratio of 1:2 and an H-section (HEA) with an aspect ratio of 1:1. The width and the height of the cross-sections were chosen equal to those of the cross-sections used in the column furnace tests, i.e. 160 mm for the SHS section, 60 mm and 120 mm for the RHS section and 100 mm (width and height) for the HEA section. The wall thickness (resp., the web thickness in the case of the H-section) was chosen to obtain predefined cross-sectional slenderness ratios. Detailed information on the finite element model is given in Appendix B.

The comparison is presented for two different temperature ranges. The test and FE results at 400 \(^\circ\)C represent the elevated temperature range with a strongly non-linear material behaviour (Chapter 2). The test and FE results at 700 \(^\circ\)C represent the high temperature range, where the non-linear branch of the stress-strain relationship is much shorter and the material behaviour is almost bilinear again. The graphs containing the corresponding results for 20 \(^\circ\)C and 550 \(^\circ\)C are given in Appendix C.
Figure 3.7  Resistance to pure compression at elevated temperatures (400 °C)
Figure 3.8  Resistance to pure compression at high temperatures (700 °C)
In Chapter 2 a significant disagreement was found between the material model of Rubert-Schaumann 1985 (adapted in EN 1993-1-2 2006) and the material behaviour resulting from tensile tests at high temperatures. In order to analyse the influence of the material model on the resistance of the cross-section to pure compression the FE simulations were executed once using the stress-strain relationship resulting from the tensile coupon tests (strain rate of 0.10 %/min) and once using the elliptical material model of Rubert-Schaumann. The resistances according to CSA and SSA were also calculated once using the material parameters from the tensile coupon tests and once using those of S355 of EN 1993-1-1/2.

3.2.1.1 Elevated temperatures

Figure 3.7 presents 6 graphs containing the comparison of the cross-sectional resistance to pure compression of the test results, the FE simulations and the carbon and stainless steel approaches at 400 °C. The two graphs at the top are for the HEA section, the two graphs at mid-height for the rectangular hollow section and the two graphs at the bottom for the square hollow section. The graphs on the left include the results of the FE study and the design approaches determined with the actual material behaviour from the tensile coupon tests while the graphs on the right present the results obtained using the material model for S355 of EN 1993-1-2. The stub column test results for 400 °C and a strain rate of 0.1 %/min are included in the graphs on the left side for each cross-section, represented by black symbols. The FE results for different cross-sectional slenderness ratios are represented in all graphs using white symbols. The continuous lines represent the carbon steel approach (CSA) and the dashed lines the stainless steel approach (SSA). The vertical dotted lines indicate the boundaries between classes 3 and 4 for both the carbon and the stainless steel approaches.

The HEA 100 and the RHS 120·60·3.6 test results gave stress values higher than those of the material coupon tests for the same material (Figure 3.4 and Figure 3.6). Therefore, the FE simulations using the stress-strain relationship of these material coupon tests can only result in lower ultimate loads than the test results. In the case of the square hollow section the FE simulates the test result quite well. The material model of EN 1993-1-2 at 400 °C corresponds sufficiently well to the actual stress-strain relationships of the tensile coupon tests. No big difference in the overall behaviour presented in the graphs of the left and the right side is visible except different values for the ultimate loads for the same slenderness ratios.

The resistances obtained using the carbon steel approach including the f_{2.0,0} stress value coincide with the FE results only for very compact cross-sections with a slenderness ratio of about \( \lambda_{p,20°C} \leq 0.3 \). The resistances of the cross-sections with slenderness ratios between \( \lambda_{p,20°C} \leq 0.3 \) and \( \lambda_{p,20°C} \leq 0.75 \) (which is the boundary with class 4) are considerably overestimated by the carbon steel approach. For slenderness ratios higher than \( \lambda_{p,20°C} = 0.75 \) (class 4 sections) the use of the 0.2 % proof stress and the effective area \( A_{eff} \) lead to very good predictions of the resistance to pure compression compared to the FE results.

The stainless steel approach uses the 0.2 % proof stress for all slenderness ratios. As a consequence the resistances of the cross-sections to pure compression are underestimated for slenderness ratios of about \( \lambda_{p,20°C} \leq 0.6 \). The lower the slenderness ratio, the larger is the difference between the design value and the FE result. For slenderness ratios higher than \( \lambda_{p,20°C} = 0.6 \) (class 4 sections) the use of the 0.2 % proof stress and the effective area \( A_{eff} \) lead to very good predictions of the resistance to pure compression compared to the FE results.

3.2.1.2 High temperatures

Figure 3.8 presents 6 graphs containing the comparison of the cross-sectional resistance to pure compression for test results, FE simulations and the carbon and stainless steel approaches at 700 °C. The two graphs at the top consider the HEA section, the two graphs at mid-height the rectangular hollow section and the two graphs at the bottom the square hollow section. The graphs on the left include the results of the FE study and the design approaches determined with the actual material behaviour from the tensile coupon tests while the graphs on the right present the results obtained using the material model for S355 of EN 1993-1-2. The stub column test results for 700 °C and a strain rate of 0.1 %/min are included in the graphs on the left side for each cross-section, represented by black symbols. The FE results for different cross-sectional slenderness ratios are represented in all graphs by white symbols. The continuous lines represent the carbon steel approach (CSA) and the dashed lines the stainless steel approach (SSA).
The differences between the ultimate loads from the FE analysis and the test results are within 10 %, which is considered acceptable. The material model of EN 1993-1-2 at 700 °C did not agree well with the actual measured material behaviour of the tensile material coupon tests (Chapter 2). As a result there is a large difference in the development of the stub column ultimate load in relation to the cross-sectional slenderness ratio between the graphs on the left and those on the right.

The graphs on the left show the results obtained using the measured tensile coupon test material behaviour. The non-linear branch of the measured material coupon test stress-strain curves is very short and takes place mainly for strains smaller than 0.2 % plastic strain. After this strain value the stress remains almost constant. The difference between the 0.2 % proof stress $f_{p,0.2,700°C}$ and the stress at 2 % total strain $f_{2,0,700°C}$ is only of 2.6 % for the SHS 160·160·5 material and -5.0 % for the HEA 100 material. In the case of the RHS 120·60·3.6 material the two stress values were even identical. This leads to almost identical ultimate loads for compact sections for both design approaches. The discontinuity at the boundary with class 4 in the case of the carbon steel approach is very small and the model generally predicts the FE results very well. Only in the case of the HEA class 4 sections does the reduction of the cross-sectional area underestimate the FE results. The stainless steel approach places the boundary between class 3 and 4 at a smaller slenderness ratio. Therefore, the class 4 cross-sectional resistance of this model is slightly lower than that of the carbon steel approach. The difference, however, is very small.

The graphs on the right show the results obtained using the material behaviour for S355 steel according to EN 1993-1-2 of Rubert-Schaumann 1985. This material model implies a non-linear stress-strain relationship up to strain values of 2 %. The difference between the $f_{2,0,700°C}$ and the $f_{p,0.2,700°C}$ of this model is 43.5 % (calculated using the reduction factors $k_{y,700°C}$ and $k_{p,0.2,700°C}$). As a result the ultimate loads determined by FE simulations, but also by the design models are considerably higher for small slenderness ratios and lower for high slenderness ratios compared to the graphs on the left. In addition, the design models (mainly the carbon steel approach) sometimes have the same difficulty in predicting the ultimate load for cross-sections of the classes 1 to 3 as at 400 °C. The carbon steel approach works well for very compact sections, but greatly overestimates the resistance of class 2 and 3 cross-sections. The stainless steel approach on the other hand underestimates the ultimate loads for all sections classified as class 1 to 3 according to that model. Both models work well in the class 4 range.

Figure 3.9  Distribution of stress and strain of a cross-section subjected to pure bending with a bilinear (left) and a non-linear (right) material behaviour
### 3.3 Pure Bending

A cross-section with a linear-elastic perfectly plastic material behaviour and subjected to pure bending first reaches its elastic bending moment resistance at the beginning of the yield plateau. The main characteristic of this resistance is an elastic (i.e. linear) stress distribution over the section (Figure 3.9 left). The resistance is calculated as the product of the elastic section modulus $W_{el}$ defined by the geometry and the linear stress distribution and the maximum stress value $f_y$. If the load is increased, the stress value stays the same until a uniform stress distribution is reached, when the entire cross-section has yielded in either compression or tension. This plastic bending moment resistance is calculated as the product of the plastic section modulus $W_{pl}$ defined by the geometry and the uniform stress distribution and the yield strength $f_y$.

In the case of non-linear material behaviour, however, larger strains are always accompanied by larger stresses and no uniform stress distribution ever develops within the cross-section. No plastic modulus $W_{pl}$ can be formed and the definition of a plastic bending moment resistance of a cross-section with a non-linear stress-strain relationship would have to be modified.

Figure 3.10 provides a schematic illustration of the cross-sectional resistance to pure bending according to the carbon steel approach (CSA) and stainless steel approach (SSA). The ambient temperature carbon steel concept is added for comparison. If a section is subjected to pure bending the plates of the section that are subjected purely to compressive stresses usually define the cross-sectional class for the entire section. Therefore, the boundaries between the different cross-sectional classes in Figure 3.10 are the same as in Figure 3.3 and Table 3.2.

The carbon and stainless steel approaches are both based on a bilinear material behaviour. They allow compact cross-sections (classes 1 and 2) to reach a plastic resistance defined as the product of the plastic section modulus $W_{pl}$ and the 'effective yield strength' of $f_{y,0.0}$ and $f_{p,0.2,0}$ for the carbon and stainless steel approaches, respectively (Table 3.3). Semi-compact cross-sections (class 3) are allowed to reach the elastic resistance defined as the product of the elastic section modulus $W_{el}$ and the effective yield strength. The resistance of slender cross-sections (class 4) is in each of the three models determined using the effective width method, reducing the cross-sectional geometry by the factor $\rho$ in relation to the cross-sectional slenderness ratio. The carbon steel approach again reduces the strength of the material to the 0.2% proof stress $f_{p,0.2,0}$ (Figure 3.10). The effective elastic section modulus $W_{el,eff}$ is determined on the reduced cross-section, where the factor $\rho$ defines the reduction of the compressed areas due to local buckling effects. The formulations to calculate $\rho$ are the same as those in Table 3.1 with $\psi = 1.0$ for those plates of the section subjected to pure compression and $\psi = -1.0$ for those plates of the section subjected to pure bending.

#### 3.3.1 Influence of the Slenderness Ratio and the Material Behaviour

No tests have been performed by Pauli et. al 2012 with loading conditions of pure bending. Therefore, the following analysis is based on FE simulations for different slenderness ratios. The FE analysis was performed on the same cross-sections as for pure compression. Detailed information on the FE model are given in Appendix B.

The comparison is again presented for 400 °C representing the elevated temperature range with a strongly non-linear material behaviour and for 700 °C representing the high temperature range with an almost bilinear material behaviour. The graphs containing the corresponding results for 20 °C and 550 °C are given in Appendix C. In order to analyse the influence of the material model on the resistance of the cross-section to pure bending the FE simulations were executed once using the stress-strain relationship resulting from the tensile coupon tests (strain rate of 0.10 %/min) and once assuming the elliptical material model of Rubert-Schaumann. The resistances according to the carbon and stainless steel approaches were also calculated once using the material parameters from the tensile coupon tests and once using those of S355 of EN 1993-1-1/2.


3.3.1.1 Elevated temperatures

Figure 3.11 and Figure 3.12 present 10 graphs containing a comparison of the cross-sectional resistance to pure bending for FE simulations and the carbon and stainless steel models discussed above. Figure 3.11 includes the major axis bending moment resistances and Figure 3.12 includes the minor axis bending moment resistances. Within each of the two figures the graphs on the left include the results of the FE analysis and the design approaches obtained using the actual material behaviour from the tensile coupon tests while the graphs on the right present the results obtained using the material model for S355 of EN 1993-1-2. The FE results for different cross-sectional slenderness ratios are represented in all graphs using white symbols. The continuous lines represent the carbon steel approach (CSA) and the dashed lines the stainless steel approach (SSA). The vertical dotted lines indicate the boundaries between classes 2, 3 and 4 for the two design approaches.

The material model of EN 1993-1-2 at 400 °C corresponds quite well to the actual stress-strain relationships of the tensile coupon tests. No big difference in the overall behaviour presented in the graphs of the left and the right side within a figure is visible except different values for the ultimate bending moments for the same slenderness ratios. The mechanical behaviour underlying the resistance to pure bending about either one of the two principal axes of a hollow section and the resistance to major axis bending of an HEA section are similar and will be treated together here. The resistance to a minor axis bending moment of an HEA section is treated as a special case below.

The carbon steel approach for hollow sections and major axis bending of an HEA section has difficulty predicting the ultimate bending moments for sections of classes 1 to 3. As in the case of pure compres-

Table 3.3 Resistance to pure bending according to the carbon and stainless steel approaches

<table>
<thead>
<tr>
<th>Class</th>
<th>Ambient temperature carbon steel</th>
<th>CSA</th>
<th>SSA</th>
</tr>
</thead>
<tbody>
<tr>
<td>Class 1 and 2</td>
<td>$M_{pl,CS,20^\circ C} = f_y,20^\circ C \cdot W_{pl}$</td>
<td>$M_{pl,CS,0} = f_2,0,0 \cdot W_{pl}$</td>
<td>$M_{pl,SS,0} = f_{p,0.2,0} \cdot W_{pl}$</td>
</tr>
<tr>
<td>Class 3</td>
<td>$M_{el,CS,20^\circ C} = f_y,20^\circ C \cdot W_{el}$</td>
<td>$M_{el,CS,0} = f_2,0,0 \cdot W_{el}$</td>
<td>$M_{el,SS,0} = f_{p,0.2,0} \cdot W_{el}$</td>
</tr>
<tr>
<td>Class 4</td>
<td>$M_{eff,CS,20^\circ C} = f_y,20^\circ C \cdot W_{el,eff}$</td>
<td>$M_{eff,CS,0} = f_{p,0.2,0} \cdot W_{el,eff}$</td>
<td>$M_{eff,SS,0} = f_{p,0.2,0} \cdot W_{el,eff}$</td>
</tr>
</tbody>
</table>
Figure 3.11 Resistance to pure major axis bending at elevated temperatures (400 °C)
Figure 3.12 Resistance to pure minor axis bending at elevated temperatures (400 °C)
Figure 3.13 Resistance to pure major axis bending at high temperatures (700 °C)
Figure 3.14 Resistance to pure minor axis bending at high temperatures (700 °C)
The fully plastic resistance $M_{pl,0}$ is only reached for very compact cross-sections with a slenderness ratio of about $\lambda_{p,20°C} \leq 0.3$. The resistances of the cross-sections with slenderness ratios between $\lambda_{p,20°C} \leq 0.3$ and $\lambda_{p,20°C} \leq 0.75$ (i.e. the boundary with class 4) are considerably overestimated by the design model. The change from $W_{pl}$ to $W_{el}$ for class 3 follows the overall shape of the development of the ultimate bending moment with the slenderness ratio, but as the resistance is still calculated using $f_{p,0.2,θ}$ it still overestimates the FE result. The stainless steel approach on the other hand using the 0.2% proof stress $f_{p,0.2,θ}$ considerably underestimates the resistance for class 1 and 2 sections. In addition to the smaller stress value, the boundary with the higher cross-sectional classes is at smaller slenderness ratios, resulting in an underestimation of the resistance for class 3. The carbon steel approach works well in the case of the class 4 sections, while the stainless steel approach slightly underestimates the resistance of the FE results.

The carbon steel approach for minor axis bending of an HEA section predicts the ultimate bending moments for sections of classes 1 and 2 very well, while the stainless steel approach underestimates it considerably. The large difference between the plastic and the elastic section modulus $W_{pl}$ and $W_{el}$ leads to a large drop in the resistance to minor axis bending at the boundary with class 3. At the end of class 3 the resistance according to the carbon steel approach drops again to the level of the 0.2% proof stress and coincides with the line of the stainless steel approach. Both approaches highly underestimate the resistance resulting from the FE simulations for classes 3 and 4. However, it is important to mention that this is not due to any elevated temperature material behaviour, but can already be observed at ambient temperature (Appendix C, Bambach et al. 2007 and Rusch & Lindner 2001).

### 3.3.1.2 High temperatures

Figure 3.13 and Figure 3.14 present 10 graphs containing the comparison of the cross-sectional resistance to pure bending of FE simulations and the carbon and stainless steel approaches at 700 °C.

The carbon steel approach using the tensile coupon test material behaviour for hollow sections and major axis bending of an HEA section works very well predicting the ultimate bending moments for sections of classes 1 and 2. The resistance drops at the beginning of class 3 due to the application of $W_{el}$ instead of $W_{pl}$. In the case of the hollow sections this leads to a slight underestimation of the resistance in class 3, which continues into class 4. As the $f_{p,0.2,700°C}$ is almost equal to $f_{2.0,700°C}$ only a small second drop in the resistance at the beginning of class 4 is observed. In the HEA section the outstand flanges are responsible for the classification, leading to an earlier boundary between the classes 2 and 3 and a larger range of class 3 sections. Therefore, the underestimation of the resistance within classes 3 and 4 is a little higher. The stainless steel approach using the tensile coupon test material behaviour for hollow sections and major axis bending of an HEA section leads to very similar results. The only difference is due to an earlier boundary between classes 2 to 3, and 3 to 4 in the case of the box sections leading to a slightly larger underestimation of the resistance in classes 3 and 4.

The carbon steel approach using the material behaviour of EN 1993-1-2, 2006 for hollow sections and major axis bending of an HEA section greatly overestimates the resistance for cross-sections of classes 1 to 3, but works very well for class 4. This is mainly due to the large difference between $f_{p,0.2,700°C}$ and $f_{2.0,700°C}$ assumed in this model. As in the case of pure compression the stainless steel approach underestimates the resistance for classes 1 to 3 and even the beginning of class 4, but works well for the main part of class 4.

The carbon and stainless steel approaches using the tensile coupon test material behaviour for minor axis bending of an HEA section work well for class 1 and 2 sections. The large difference between $W_{pl}$ and $W_{el}$ result, as in the case of pure compression, in a large drop of the resistance at the beginning of class 3 and a considerable underestimation of the FE-determined value of ultimate bending moment in classes 3 and 4. When the material model of EN 1993-1-2, 2006 is used the stainless steel approach underestimates the resistance over the entire range of slenderness ratios while the carbon steel approach works well for compact sections of classes 1 and 2 and underestimates classes 3 and 4. As in the case of elevated temperatures this is not a result of any material model, but can already be observed at ambient temperature (Appendix C, Bambach et al. 2007 and Rusch & Lindner 2001).
3.4 AxiAl Compression - UniAxial Ben ding Moment Interaction

The interaction of an axial compression and a uniaxial bending moment in a cross-section depends strongly on the resistances of this section to pure bending and pure compression. Therefore, the common carbon and stainless steel approaches (CSA and SSA) base their interaction formulas on the axial compression and uniaxial bending moment capacities presented in Table 3.1 and Table 3.3 for all cross-sections belonging to one of the four classes. Compact cross-sections (classes 1 and 2) are again assumed to have a fully plastic stress distribution (Table 3.4), while semi-compact cross-sections belonging to class 3 are only allowed to reach the elastic stress distribution. The same relationship is used for class 4 cross-sections replacing the elastic resistances by the reduced elastic ones, whose definition was given above.

The CSA and SSA use the same formulas for the plastic, elastic and reduced elastic interaction of compression and bending. The differences between the two approaches are the definition of the effective yield strength ($f_{y,0,0}$ and $f_{p,0,2,0}$) used to determine the resistances to pure compression and pure bending and the different boundary values of the cross-sectional slenderness ratios used for the classification.

3.4.1 Influence of the slenderness ratio

Both the carbon steel approach (CSA) and the stainless steel approach (SSA) are again compared to test results and finite element simulations. The FE analysis used the same cross-sections as in the case of the pure compression and pure bending simulations. For each slenderness ratio of each of the three types of cross-section different compression-bending moment interactions were simulated. Detailed information on the FE model is given in Appendix B. The comparison is presented for the two different temperatures of 400 °C representing the elevated temperature range with a strongly non-linear material behaviour and of 700 °C representing the high temperature range, where the material behaviour is almost bilinear again. The graphs containing the corresponding results for 20 °C and 550 °C are given in Appendix C. The actual material behaviour resulting from the tensile material coupon tests was used for the FE simulations and the determination of the resistances according to the CSA and SSA.

<table>
<thead>
<tr>
<th>Class 1 and 2</th>
<th>Class 3</th>
<th>Class 4</th>
</tr>
</thead>
<tbody>
<tr>
<td>$M_{y,pl,N}$</td>
<td>$N$ $N_{pl} + M_y M_{y,el} + M_z M_{z,el} \leq 1.0$</td>
<td>$N_{eff} N_{eff} + M_y M_{y,eff} + M_z M_{z,eff} \leq 1.0$</td>
</tr>
<tr>
<td>Box sections</td>
<td>$M_{z,pl,N} = M_{z,pl} \cdot \xi \cdot (1 - n)$</td>
<td></td>
</tr>
<tr>
<td>H – sections</td>
<td>$M_{z,pl,N} = M_{z,pl} \cdot \left[ 1 - \left( \frac{n - a}{1 - a} \right)^2 \right]$</td>
<td></td>
</tr>
</tbody>
</table>

| $N_{pl}$ = $N_{pl,CS,0}$ | $N_{pl,SS,0}$ | CSA | SSA |
| $N_{eff}$ = $N_{eff,CS,0}$ | $N_{eff,SS,0}$ | CSA | SSA |
| $M_{pl}$ = $M_{pl,CS,0}$ | $M_{pl,SS,0}$ | CSA | SSA |
| $M_{el}$ = $M_{el,CS,0}$ | $M_{el,SS,0}$ | CSA | SSA |
| $M_{eff}$ = $M_{eff,CS,0}$ | $M_{eff,SS,0}$ | CSA | SSA |

$n = \frac{N}{N_{pl}}$

$\xi = \frac{1}{1 - 0.5 \cdot a}$

$a = \frac{A_0 - 2 \cdot B \cdot t_f}{A_0}$, H – sections

$a = \frac{A_0 - 2 \cdot (B or H) \cdot t}{A_0}$, Box sections
Figure 3.15 Compression - bending moment interaction at elevated temperatures of SHS sections
Axial compression - uniaxial bending moment interaction

Figure 3.16 Compression - minor axis bending moment interaction at elevated temperatures of RHS sections
Figure 3.17 Compression - major axis bending moment interaction at elevated temperatures of HEA sections
Figure 3.18 Compression - minor axis bending moment interaction at elevated temperatures of HEA sections
3.4.1.1 Elevated temperatures

Figure 3.15 to Figure 3.18 show the graphs containing the comparison of the cross-sectional resistance to an axial compression-uniaxial bending moment interaction of the test results, the FE simulations and the carbon and stainless steel approaches. Figure 3.15 and Figure 3.16 show the interaction for different cross-sectional slenderness ratios of the box sections, while Figure 3.17 and Figure 3.18 contain the major and the minor axis bending moment interaction for different cross-sectional slenderness ratios of the HEA section. The presented cross-sectional slenderness ratios cover the range of all four cross-sectional classes (classification of the plates for pure compression) and include the slenderness ratios of the tested stub columns of all three cross-sections. Graphs containing the compression-bending moment interaction for additional slenderness ratios are given in Appendix C.

The test results in all the graphs are represented by black symbols, while the FE results are represented using white symbols. The plastic (pl), the elastic (el) and, for class 4 sections, the reduced elastic (eff) interactions of a cross-section according to the carbon and the stainless steel approaches are represented and labelled in the graphs. The interaction formulas corresponding to the actual classification of the cross-section of each graph according to the carbon and the stainless steel approaches are represented by a continuous and a dashed line, respectively.

Figure 3.15 shows the axial compression-uniaxial bending moment interaction of SHS sections at 400 °C. The cross-sectional slenderness ratio increases from the top left to the bottom right graph. The (centrically loaded) test result is included in the graph mid-height right corresponding to the cross-sectional slenderness ratio of the test specimens. The FE results for the two axes correspond to those of the chapters on pure compression and pure bending. The overall shape of the interaction resulting from the simulations is slightly curved for all slenderness ratios. It corresponds well to the plastic interaction of the carbon steel approach for very stocky sections ($\lambda_{p,20°C} = 0.27$). The plastic interaction of the stainless steel approach has the same shape, but uses the 0.2 % proof stress instead of the stress at 2 % total strain, underestimating the capacity of very stocky sections. However, this was stated already in the chapters about pure compression and pure bending. The disagreement between the curves of the FE results and the design codes for the first two graphs ($\lambda_{p,20°C} = 0.27$ and 0.40) is entirely due to the difficulties of the design codes in appropriately describing the resistance to pure compression and pure bending at elevated temperatures. The shape of the plastic interaction formula corresponds well to the FE results of compact sections, so no additional error is introduced at this stage. Graphs No. 3 and 4 ($\lambda_{p,20°C} = 0.54$ and 0.60) present semi-compact sections. A cross-sectional slenderness ratio of $\lambda_{p,20°C} = 0.54$ corresponds to class 2 for carbon and class 3 for stainless steel, while $\lambda_{p,20°C} = 0.60$ defines a cross-section of class 3 for carbon and class 4 for stainless steel. In this slenderness range the difference between the two design approaches is largest. The FE results lie between the two design proposals. In addition to the inherent difficulties of predicting the resistance to pure compression and pure bending, the plastic interaction formulas of the design approaches only inadequately describe the shape of the interaction curve. In graphs No. 5 and 6 ($\lambda_{p,20°C} = 0.67$ and 0.81) class 4 sections are represented. For $\lambda_{p,20°C} = 0.67$ the problems are similar to those described above. For $\lambda_{p,20°C} = 0.81$ (and higher slenderness ratios presented in Appendix C), on the other hand, the resistance to pure compression and pure bending is predicted sufficiently well by the design approaches. Here the difference is due to the slightly curved shape of the interaction of the FE results compared the linear interaction proposed by the two design code approaches. The curvature of the FE interaction becomes less pronounced with increasing slenderness ratios, resulting in ever better agreement with the design codes.

Figure 3.16 shows the axial compression - minor axis bending moment interaction of RHS sections at 400 °C. The cross-sectional slenderness ratio increases from the top left to the bottom right graph. All observations stated above for the SHS section are still valid here. Graph No. 4 (mid-height right) includes two additional eccentrically loaded stub column test results.

Figure 3.17 shows the axial compression - major axis bending moment interaction of HEA sections at 400 °C. The cross-sectional slenderness ratio increases from the top left to the bottom right graph. The stub column test results are included in the first graph corresponding to the cross-sectional slenderness ratio of the test specimens of $\lambda_{p,20°C} = 0.33$. The observations for this compact section are very similar compared to compact box sections. Still, the shape of the interactions of the design approaches and the FE results correspond well and only the inherent discrepancies from the determination of the end points
of the curve lead to different results. Graph No. 2 ($\lambda_{p, 20\degree C} = 0.48$) exhibits a semi-compact section of class 3 according to the carbon steel approach and class 2 for the stainless steel approach. As in the case of the box sections the elastic interaction curve has difficulties describing the shape of the interaction from the FE simulations. Graphs No. 3 to 6 ($\lambda_{p, 20\degree C} = 0.64$ to 1.11) show class 4 sections. Both design approaches greatly underestimate the resistance to pure bending (the resistance to pure compression is also, but not so largely, underestimated). The shape of the interaction of the FE simulations is almost linear, so there is no further disagreement here.

Figure 3.18 shows the axial compression-minor axis bending moment interaction of HEA sections at 400 °C. The stub column test results are included in the first graph corresponding to the cross-sectional slenderness ratio of the test specimens of $\lambda_{p, 20\degree C} = 0.33$. The curvature of the interaction of the FE simulation results is very strong for slenderness ratios of all four cross-sectional classes. In the first graph presenting the compact cross-section with $\lambda_{p, 20\degree C} = 0.33$ the shape of the interactions of the design code approaches and the FE results correspond well and only the inherent discrepancies from the determination of the end points of the curve lead to different results. Graph No. 2 ($\lambda_{p, 20\degree C} = 0.48$) has a semi-compact section of class 3 according to the carbon steel approach and class 2 for the stainless steel approach. As in the case of the box sections the elastic interaction curve has difficulties describing the shape of the interaction from the FE simulations. Both design code approaches are incapable of properly predicting the resistances to pure compression and pure bending. Graphs No. 3 to 6 ($\lambda_{p, 20\degree C} = 0.64$ to 1.11) show class 4 sections. There is a large disagreement between the two design approaches and the FE results. First there is the inherent difference between the resistance to pure bending as predicted by the design approaches and as calculated using the FE method. Then again there is the linear interaction of the design formulas, while the FE results exhibit a highly curved interaction relationship for all presented slenderness ratios.

3.4.1.2 High temperatures

Figure 3.19 to Figure 3.22 show the graphs containing the comparison of the cross-sectional resistance to an axial compression-uniaxial bending moment interaction of the test results, the FE simulations and the carbon and stainless steel approaches (CSA and SSA) at 700 °C. Figure 3.19 and Figure 3.20 show the interaction for different cross-sectional slenderness ratios of box sections and Figure 3.21 and Figure 3.22 contain the major and the minor axis bending moment interaction for different cross-sectional slenderness ratios of the HEA section. The presented cross-sectional slenderness ratios cover the range of all four cross-sectional classes (classification of the plates for pure compression) and include the slenderness ratios of the tested stub columns of all three cross-sections. Graphs containing the compression-bending moment interaction for additional slenderness ratios are given in Appendix C.

The test results in all the graphs are represented by black symbols, while the FE results are represented by white symbols. The plastic (pl), the elastic (el) and, for class 4 sections, the reduced elastic (eff) interactions of a cross-section according to the carbon and the stainless steel approaches are represented and labelled in the graphs. The interaction formulas corresponding to the actual classification of the cross-section of each graph according to the carbon and the stainless steel approaches are represented by a continuous and a dashed line, respectively.

Figure 3.19 shows the axial compression-uniaxial bending moment interaction of SHS sections at 700 °C. The cross-sectional slenderness ratio increases from the top left to the bottom right graph. The (centrically loaded) test result is included in the graph mid-height right corresponding to the cross-sectional slenderness ratio of the test specimens of $\lambda_{p, 20\degree C} = 0.60$. The finite element results on the two axes correspond to those of the chapters on pure compression and pure bending. At 700 °C the actual material behaviour from the tensile material coupon tests exhibited an almost bilinear stress-strain relationship resulting in almost identical values for the 0.2 % proof stress and the stress at 2 % total strain. Therefore, the models of the two design approaches to calculate the resistance to pure compression and pure bending worked better than for 400 °C. In addition, the two design approaches result in almost identical resistances for pure compression, pure bending and any interaction of the two. Graphs No. 1 and 2 ($\lambda_{p, 20\degree C} = 0.27$ and 0.40) contain compact cross-sections. It was stated that for 400 °C the plastic interaction of the design approaches worked well compared to the FE results as long as the resistances to pure compression and pure bending taken as the end points of the interaction curve are predicted correctly. The graphs at 700 °C confirm this observation, because, as was mentioned before, the resistances...
Figure 3.19 Compression - bending moment interaction at high temperatures of SHS sections
Figure 3.20 Compression - minor axis bending moment interaction at high temperatures of RHS sections
Figure 3.21 Compression - major axis bending moment interaction at high temperatures of HEA sections
Figure 3.22 Compression - minor axis bending moment interaction at high temperatures of HEA sections
to pure compression and pure bending can be predicted accurately by the design approaches. Graphs No. 3 and 4 ($\lambda_{p,20^\circ C} = 0.54$ and 0.60) contain semi-compact sections. The FE results exhibit a slightly curved interaction that does not reach the stress at 2% total strain anymore. The elastic interactions of the design approaches have difficulties in matching the resistances of the FE simulations for the entire interaction. Graphs No. 5 and 6 ($\lambda_{p,20^\circ C} = 0.67$ and 0.81) contain class 4 sections. The reduced elastic interaction formulas of the design approaches mainly underestimate the resistances resulting from the FE simulations if the bending moment becomes dominant.

Figure 3.20 shows the axial compression-minor axis bending moment interaction of RHS sections at 700 °C. The (centrically loaded) test result is included in the graph mid-height right corresponding to the cross-sectional slenderness ratio of the test specimens of $\lambda_{p,20^\circ C} = 0.62$. The measured material behaviour exhibited a perfectly horizontal flow stress plateau with identical numerical values for $f_{p,0.2,700^\circ C}$ and $f_{2,0.700^\circ C}$. Therefore, the resistances determined with the two design approaches lead to identical results for plastic and elastic interactions. However, different classification systems result in different interaction curves to be applied to a certain cross-sectional slenderness ratio. All observations stated above for the SHS section are still valid here.

Figure 3.21 shows the axial compression - major axis bending moment interaction of HEA sections at 700 °C. The stub column test result is included in the first graph corresponding to the cross-sectional slenderness ratio of the test specimens of $\lambda_{p,20^\circ C} = 0.33$. The measured material behaviour exhibited a slightly decreasing stress-strain relationship after the 0.2% proof stress. The FE simulations as well as the determination of the resistances with the design approaches, however, were performed using a horizontal flow stress plateau with $f_{p,0.2,700^\circ C}$ equal to $f_{2,0.700^\circ C}$. Therefore, the resistances determined with the two design approaches lead to identical results for plastic and elastic interactions. However, different classification systems result in different interaction curves to be applied to a certain cross-sectional slenderness ratio. Only one graph containing a compact section of $\lambda_{p,20^\circ C} = 0.33$ is present. The shape of the interactions of the design approaches and the FE results correspond well because the resistances to pure compression and pure bending can be predicted accurately and the shape of the plastic interaction fits the FE data very well. Graph No. 2 ($\lambda_{p,20^\circ C} = 0.48$) contains a semi-compact section, still of class 1 according to the stainless steel approach, but already of class 3 according to the carbon steel approach. The plastic interaction again fits very well, while the elastic interaction has difficulty predicting the resistance to pure bending and doesn't fit the shape of the curved interaction of the FE simulation results. Graphs No. 3 to 6 ($\lambda_{p,20^\circ C} = 0.64$ to 1.11) contain the slender cross-sections belonging to class 4 according to both design approaches. For $\lambda_{p,20^\circ C} = 0.64$ the resistance to pure compression is predicted well by both CSA and SSA, while the resistance to pure bending is slightly underestimated. The FE results still lead to a small curvature in the interaction not captured by the design approaches. This curvature disappears for higher slenderness ratios and the FE interaction becomes almost linear. The interaction of the design approaches now fits the shape of the FE interaction, but the resistances to pure compression and pure bending are too low compared to the FE results.

Figure 3.22 shows the axial compression - minor axis bending moment interaction of HEA sections at 700 °C. The stub column test result is included in the first graph corresponding to the cross-sectional slenderness ratio of the test specimens of $\lambda_{p,20^\circ C} = 0.33$. Again the FE simulations as well as the determination of the resistances with the design approaches were performed using a horizontal flow stress plateau with $f_{p,0.2,700^\circ C}$ equal to $f_{2,0.700^\circ C}$ leading to identical results for plastic and elastic interactions for the two design approaches. However, different classification systems result in different interaction curves to be applied to a given cross-sectional slenderness ratio. For graphs No. 1 and 2 ($\lambda_{p,20^\circ C} = 0.33$ and 0.48) containing compact and semi-compact cross-sections similar observation can be made as for the major axis bending interaction of the same section. Graphs No. 3 to 6 ($\lambda_{p,20^\circ C} = 0.64$ to 1.11) contain the slender cross-section belonging to class 4 according to both design approaches. It is interesting to see that the results of the FE simulations follow the plastic interaction curve for all the presented slenderness ratios. The design approaches highly underestimate the resistance to pure compression (for $\lambda_{p,20^\circ C} \geq 0.96$) and to pure bending (for all class 3 and 4 sections). The linear shape of the interaction formula adds to the large difference between the cross-sectional capacity predicted by the design approaches and that determined with FE simulations.
3.5 Conclusions

The load-bearing capacity of a cross-section mainly depends on its geometry, the applied external mechanical load (compression, tension, bending, shear, torsion or any interactions) and the mechanical behaviour of the material. The geometry of the cross-section defines the boundary conditions of the individual plates of the section (internal parts or outstand flanges) and influences the slenderness ratio of each of these plates. The external mechanical load defines the areas within the section subjected to compressive stresses that are at risk regarding local buckling instabilities. The mechanical behaviour of the material serves as an upper boundary to the stress level applicable to the section and influences the cross-sectional slenderness ratio of the individual plates.

The slenderness ratio of the individual plates of a section that are subjected to compressive stresses defines the strain level, at which local buckling instabilities occur. Higher slenderness ratios of a plate subjected to compression lead to local buckling failures at smaller strain levels. Therefore, each individual cross-section exhibits local buckling at a different strain level. In the case of a bilinear stress-strain relationship a large range of strain levels correspond to a constant yield stress. In the case of a non-linear stress-strain relationship, on the other hand, a different strain always implies a different stress level.

The two most common design approaches to determine the cross-sectional resistance of steel members with a non-linear material behaviour define an 'effective' yield strength and assume a bilinear stress-strain relationship. In the carbon steel approach (CSA) the effective yield strength is defined as the stress at 2% total strain $f_{2.0}$ while the stainless steel approach (SSA) uses the 0.2% proof stress $f_{p,0.2}$.

The cross-sectional resistance to pure compression decreases with increasing slenderness ratios due to the non-linear stress-strain relationship. At elevated temperatures (between 300 °C and 600 °C) the carbon steel approach underestimates the resistance of class 1 cross-sections, considerably overestimates the resistance of class 2 and 3 cross-sections and works well for class 4 cross-sections. The stainless steel approach underestimates the resistance of class 1 to 3 sections and works well for class 4 sections. In the case of high temperatures (above 600 °C) the almost bilinear material behaviour of steel leads to a good agreement of the resistance to pure compression between the carbon and the stainless steel approaches and FE simulations, as long as the actual material behaviour is used. If the design model of Rubert-Schaumann is adopted, the design approaches have the same problems met in the case of elevated temperatures.

The cross-sectional resistance to pure bending decreases with increasing slenderness ratios due to the non-linear stress-strain relationship. At elevated temperatures (between 300 °C and 600 °C) the carbon steel approach works well to predict the resistance of class 1 cross-sections, but considerably overestimates the resistance of class 2 and 3 cross-sections and works again well for class 4 cross-sections. Minor axis bending of H-sections is an exception. Here the carbon steel approach considerably underestimates the cross-sectional resistance of class 2 to 4 sections. The stainless steel approach underestimates the resistance of class 1 to 3 sections and works well for class 4 sections, except in the case of minor axis bending of H-sections, where the resistance of all cross-sections is considerably underestimated. The step-wise decrease of the resistance at the boundaries between the cross-sectional classes is artificial and seems to be inadequate to predict the continuous decrease of the resistance as indicated by the FE simulations. At high temperatures (above 600 °C) the agreement between the carbon and the stainless steel approaches and FE simulations is better, as long as the actual material behaviour is used. However, the step-wise decrease of the resistance at the class boundaries is artificial. If the design model of Rubert-Schaumann is adopted, the design approaches show the same problems as in the case of elevated temperatures.

The cross-sectional resistance to an interaction of axial compression and uniaxial bending moment decreases with increasing slenderness ratios. The shape of an interaction curve follows a plastic interaction for class 1 to 3 sections, and only slowly approaches a linear (elastic) interaction within class 4. The resistances to pure compression and pure bending form the end points of the interaction curve. The problems of the two design code approaches (CSA and SSA) to correctly predict these resistances strongly influence their capability to predict the resistance to a compression-bending interaction.
The idea of an 'effective' yield strength for a bilinear material model in the design formulations, even if the real material behaviour is highly non-linear, results in very poor predictions of the cross-sectional resistances of class 1 to 3 sections. While the carbon steel approach overestimates the resistance in the majority of the cases, the stainless steel approach is on the safe side, considerably underestimating the cross-sectional resistances.
4 LEVEL 3: MEMBER STABILITY

4.1 INTRODUCTION

In Chapter 2 the material behaviour of carbon steel for different temperatures was analysed and divided into the domains of moderate, elevated and high temperatures. Chapter 3 analysed the influence of the non-linear stress-strain relationship on the cross-sectional capacity under pure compression, pure bending and an interaction of axial compression and uniaxial bending moment. The analysis was discussed for elevated and high temperatures and based on experimental results and FE simulations. This chapter now discusses the influence of this temperature-dependent material behaviour and the cross-sectional capacity on the load-bearing capacity of carbon steel columns.

The foundation of the analysis is again an extensive experimental study on slender columns at elevated and high temperatures conducted at the ETH Zurich. These tests were performed on the same three cross-sections with the same material properties as the stub column tests (SHS 160×160×5, RHS 120×60×3.6 and HEA 100) at 20 °C, 400 °C, 550 °C and 700 °C and at a strain rate of 0.10 %/min. The compressive load was applied centrically to the slender columns. The tests are described in more detail in Appendix A and in Pauli et al. 2012.

Different models exist in the literature to determine the load-bearing capacity of steel columns with non-linear material behaviour (Somaini 2012, Ashraf 2006, Toh et al. 2003, Gardner 2002, Rasmussen & Rondal 1998 and Talamona et al. 1997). Here, the test results are only compared to FE simulations and two existing basic models to analytically determine the member buckling resistance of steel columns in structural engineering. These two concepts are based on the cross-sectional capacity and assume a bilinear material behaviour with constant effective yield strength in the plastic range. The first model is used in the fire design of carbon steel structures and will be referred to as the carbon steel approach (CSA). The second model is commonly used in the stainless steel design at ambient temperature and can be adopted for carbon steel in fire. Hereafter it will be called the stainless steel approach (SSA).

Both models are based on the non-dimensional overall slenderness ratio $\bar{\lambda}_k$ of steel members in compression and provide buckling curves for different cross-sections and temperatures. The basic concept of the buckling curves is to reduce the cross-sectional capacity of a compression member depending on the type of cross-section and the overall slenderness ratio $\bar{\lambda}_k$ of the compression member (Table 4.1).
4.2 Influence of the Slenderness Ratio, the Cross-section and the Material Behaviour

The following analysis is based on the test results and FE simulations to gain information for different slenderness ratios in addition to those of the test specimens. This FE investigation was limited on the three types of cross-section already used for the simulations of the cross-sectional capacity (i.e. SHS, RHS with an aspect ratio of 1:2 and HEA with an aspect ratio of 1:1). Detailed informations on the FE model is given in Appendix B.

The comparison is presented for two different temperature ranges. The test and FE results at 400 °C represent the elevated temperature range with a strongly non-linear material behaviour (Chapter 2). The test and FE results at 700 °C represent the high temperature range, where the non-linear branch of the stress-strain relationship is much shorter and the material behaviour is almost bilinear again. The graphs containing the corresponding results for 20 °C and 550 °C are given in Appendix D.

In Chapter 2 a significant disagreement was found between the material model of Rubert-Schaumann 1985 (adopted in EN 1993-1-2 2006) and the material behaviour resulting from tensile tests at high temperatures. In order to analyse the influence of the material model on the resistance of the steel columns to pure compression the FE simulations were executed once using the stress-strain relationship resulting from the tensile coupon tests (strain rate of 0.10 %/min) and once using the elliptical material model of Rubert-Schaumann. The resistances according to both the carbon and the stainless steel approaches (CSA and SSA) were also calculated, once using the material parameters from the tensile coupon tests and once using those of S355 of EN 1993-1-1/2.

Table 4.1 Buckling curves of the carbon and stainless steel approaches

<table>
<thead>
<tr>
<th>Ambient temperature</th>
<th>Carbon steel</th>
<th>CSA</th>
<th>SSA</th>
</tr>
</thead>
<tbody>
<tr>
<td>...</td>
<td>...</td>
<td>...</td>
<td>...</td>
</tr>
<tr>
<td>Class 1 to 3</td>
<td>( \chi = \frac{1}{\phi + \sqrt{\phi^2 - \lambda_k^2}} \leq 1.0 )</td>
<td>( \chi = \frac{1}{\phi + \sqrt{\phi^2 - \lambda_k^2}} \leq 1.0 )</td>
<td>( \chi = \frac{1}{\phi + \sqrt{\phi^2 - \lambda_k^2}} \leq 1.0 )</td>
</tr>
<tr>
<td>( \phi = 0.5 \cdot (1 + \alpha(\lambda_k - 0.2) + \lambda_k^2) )</td>
<td>( \phi = 0.5 \cdot (1 + \alpha(\lambda_k - 0.2) + \lambda_k^2) )</td>
<td>( \phi = 0.5 \cdot (1 + \alpha(\lambda_k - 0.2) + \lambda_k^2) )</td>
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<td>( \alpha = 0.65 \cdot \sqrt{\frac{235}{f_y, 20^\circ C}} )</td>
<td>( \phi_{\text{Box}} = 0.5 \cdot (1 + \alpha(\lambda_k - 0.4) + \lambda_k^2) )</td>
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<td>HEA, minor axis:</td>
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\( \bar{k}_{20^\circ C} = \frac{L_k \sqrt{A_0/I}}{\pi \sqrt{E_{20^\circ C}/f_y, 20^\circ C}} \) for Class 1 to 3

| Class 4 | \( \alpha = 0.49 \) | HEA, minor axis: | \( \alpha = 0.76 \) |

\( \bar{k}_{\text{eff}, 20^\circ C} = \frac{L_k \sqrt{A_{\text{eff}}/I}}{\pi \sqrt{E_{20^\circ C}/f_y, 20^\circ C}} \) for Class 4

64
4.2.1 Elevated temperatures

Figure 4.1 to Figure 4.5 present the graphs containing the comparison of the resistance of steel columns to pure compression of test results, FE simulations and the carbon and stainless steel approaches for elevated temperatures. Figure 4.1 considers the SHS section, Figure 4.2 and Figure 4.3 the RHS about the major and the minor axes, respectively, and Figure 4.4 and Figure 4.5 the HEA section again about the major and the minor axes. The graphs on the left within each figure include the results of the FE study and the design approaches determined with the actual material behaviour from the tensile coupon tests while the graphs on the right present the results obtained using the material model for S355 of EN 1993-1-2. Three different cross-sectional slenderness ratios are presented for each type of cross-section. The first cross-sectional slenderness ratio of \( \lambda_{p,20^\circ C} = 0.3 \), represented by graphs on the top of each page, corresponds to class 1 sections. The second cross-sectional slenderness ratio of \( \lambda_{p,20^\circ C} = 0.6 \), represented by graphs in the middle of each page, includes box sections classified as class 2 at ambient and class 3 at elevated temperatures for carbon steel, and class 4 for stainless steel, and HEA sections of class 3 for both design approaches. The third cross-sectional slenderness ratio of about \( \lambda_{p,20^\circ C} = 0.8 \), represented by graphs at the bottom of each page considers class 4 sections.

The stub and slender column test results for 400 °C and a strain rate of 0.1 %/min are included in the graphs on the left side corresponding to the cross-sectional slenderness ratio of the test specimens of each cross-section, represented by a black symbol. The FE results for different cross-sectional slenderness ratios are represented in all graphs by white symbols. The continuous lines represent the carbon steel approach and the dashed lines the stainless steel approach.

Figure 4.1 shows the resistance to pure compression of SHS columns of different overall slenderness ratios \( \lambda_{k,20^\circ C} \) at 400 °C. The two graphs on the top of the page include the behaviour of class 1 sections with actual measured material behaviour (left) and nominal material behaviour (right). The overall shape of the buckling curves according to the two design approaches is similar. The cross-sectional resistance to pure compression, represented on the left vertical axis of the graph, however, is different, since to determine the resistance the carbon steel approach uses \( f_{p,0.2,0} \) and the stainless steel approach uses \( f_{p,0.2,0} \). The horizontal plateau of the buckling curves, indicating that no global stability failure will occur for these overall slenderness ratios, is longer in the stainless, than in the carbon steel approach, resulting in almost parallel buckling curves for higher overall slenderness ratios. However, the FE results indicate that global stability failure occurs even for very small overall slenderness ratios (i.e. very short columns) because of the non-linear shape of the stress-strain relationship at elevated temperatures. The resistances of these short columns \( \lambda_{k,20^\circ C} < 1.5 \) to pure compression according to the FE simulations is considerably lower than proposed by the carbon steel approach. The stainless steel approach on the other hand underestimates the resistance for most overall slenderness ratios and only slightly overestimates it for \( \lambda_{k,20^\circ C} = 0.3 \) to 0.9. As the overall shape of the material model of EN 1993-1-2, 2006 is quite similar to the material behaviour measured in the tensile material coupon tests, no significant differences between the graph on the left and the right are observed.

The two graphs at mid-height of the page include the behaviour of sections with a cross-sectional slenderness ratio of \( \lambda_{p,20^\circ C} = 0.60 \) with actual measured material behaviour (left) and nominal material behaviour (right). The stub and slender specimen test results are included in the graph on the left. The overall shape of the buckling curves according to the two design approaches is again similar. The cross-sectional resistance to pure compression, represented on the left vertical axis of the graph, is again different, as the carbon steel approach uses \( f_{p,0.2,0} \) (class 3) and the stainless steel approach uses \( f_{p,0.2,0} \) and a reduced cross-sectional area (class 4) to determine the resistance. The horizontal plateau of the buckling curves, indicating that no global stability failure will occur for these overall slenderness ratios, is again longer for the stainless, than for the carbon steel approach, resulting in almost parallel buckling curves for higher overall slenderness ratios. The FE simulation results for this cross-sectional slenderness ratio indicate a short plateau for small overall slenderness ratios (i.e. very short columns). The column with an overall slenderness ratio of \( \lambda_{k,20^\circ C} = 0.25 \) that failed in a global buckling mode when its cross-sectional slenderness ratio was \( \lambda_{p,20^\circ C} = 0.27 \) now fails in a local buckling mode due to the higher slenderness ratio of the cross-section \( \lambda_{p,20^\circ C} = 0.60 \). The resistances to pure compression according to the carbon steel approach highly overestimate the resistance resulting from FE simulations for \( \lambda_{k,20^\circ C} < 1.5 \), but fit well for higher overall slenderness ratios. The discrepancy for short columns is mainly due to the overestimation...
of the cross-sectional resistance to pure compression. Nevertheless, the overall shape of the buckling curve compared to the FE results seems too steep. The buckling curve according to the stainless steel approach fits the FE results better (mainly because the cross-sectional resistance is closer to the FE result), but again the overall shape of the curve does not correspond to the development of the resistance with the slenderness ratio according to the FE simulations. As the overall shape of the material model of EN 1993-1-2, 2006 is quite similar to the material behaviour measured in the tensile material coupon tests, no significant differences between the graph on the left and the right are observed.

The two graphs at the bottom of the page include the behaviour of sections with a cross-sectional slenderness ratio of $\lambda_{p,20^\circ C} = 0.81$ with actual measured material behaviour (left) and nominal material behaviour (right). The overall shape of the buckling curves according to the two design approaches is identical for overall slenderness ratios higher than $\lambda_{k,20^\circ C} = 0.5$. The cross-sectional resistance to pure compression, represented on the left vertical axis of the graph, however, is different, because the reduction of the cross-sectional area for class 4 sections starts at smaller cross-sectional slenderness ratios according to the stainless steel compared to the carbon steel approach resulting in smaller cross-sectional resistances for the same cross-sectional slenderness ratios. The longer horizontal plateau of the buckling curve of the stainless steel approach compensates for the lower cross-sectional resistance resulting in identical buckling curves for higher overall slenderness ratios. The FE results for this cross-sectional slenderness ratio exhibit again a short plateau for small overall slenderness ratios (i.e. very short columns). The column with an overall slenderness ratio of $\lambda_{k,20^\circ C} = 0.25$ that failed in a global buckling mode when its cross-sectional slenderness ratio was $\lambda_{p,20^\circ C} = 0.27$ now fails in a local buckling mode due to the higher slenderness ratio of the cross-section $\lambda_{p,20^\circ C} = 0.81$. The resistances to pure compression according to the carbon steel approach overestimate the resistances resulting from FE simulations for very short columns, $\lambda_{k,20^\circ C} < 0.5$, and underestimates it for higher overall slenderness ratios. The buckling curve according to the stainless steel approach underestimates the resistance from the FE simulations for all overall slenderness ratios. The discrepancies between the design approaches and the FE results are due to the problem of predicting the cross-sectional resistances for overall slenderness ratios of $\lambda_{k,20^\circ C} < 0.5$ and due to the rather steep shape of the buckling curve of the design proposals for higher overall slenderness ratios. As the overall shape of the material model of EN 1993-1-2, 2006 is quite similar to the material behaviour measured in the tensile material coupon tests, no significant differences between the graphs on the left and on the right are observed.

Figure 4.2 and Figure 4.3 show the resistance to pure compression of RHS columns pin-ended about the major and the minor axis, respectively, of different overall slenderness ratios $\lambda_{k,20^\circ C}$ at 400 °C. All observations made above for the SHS columns are still valid here.

Figure 4.4 and Figure 4.5 show the resistance to pure compression of HEA columns pin-ended about the major and the minor axis, respectively, of different overall slenderness ratios $\lambda_{k,20^\circ C}$ at 400 °C. The actual ambient temperature yield strength of the HEA test specimens is $f_{y20^\circ C} = 425$ N/mm² while the nominal ambient temperature yield strength of EN 1993-1-1, 2005 is $f_{y20^\circ C} = 355$ N/mm². This results in different cross-sectional slenderness ratios $\lambda_{k,20^\circ C}$ for cross-sections of the same geometry. Therefore, the cross-sectional slenderness ratios of the sections in the graphs on the left showing the behaviour of columns with actual material behaviour and those on the right showing the behaviour of columns with nominal material behaviour are not identical. The graphs at the top include the behaviour of class 1 sections with actual measured material behaviour (left) and nominal material behaviour (right). The overall shapes of the buckling curves according to the two design approaches CSA and SSA are similar. The cross-sectional resistance to pure compression, represented on the left vertical axis of the graph, however, is different, since to determine the resistance the carbon steel approach uses $f_{2,0,0}$ and the stainless-steel approach uses $f_{p,0.2,0}$. The FE results indicate that global stability failure occurs even for very small overall slenderness ratios (i.e. very short columns) due to the non-linear shape of the stress-strain relationship at elevated temperatures. The resistances of these short columns ($\lambda_{k,20^\circ C} < 1.25$) to pure compression according to the FE simulations are considerably lower than indicated by the carbon steel approach. The stainless steel approach, on the other hand, underestimates the resistance for all overall slenderness ratios presented here. As the overall shape of the material model used in EN 1993-1-2, 2006 is quite similar to the material behaviour measured in the tensile material coupon tests, no significant differences between the graphs on the left and on the right are observed.
Figure 4.1  Flexural buckling resistance of SHS sections at elevated temperatures (400 °C)
Figure 4.2  Flexural buckling resistance of RHS sections pin-ended about the major axis at 400 °C
Figure 4.3 Flexural buckling resistance of RHS sections pin-ended about the minor axis at 400 °C
Figure 4.4  Flexural buckling resistance of HEA sections pin-ended about the major axis at 400 °C
Influence of the slenderness ratio, the cross-section and the material behaviour

Figure 4.5 Flexural buckling resistance of HEA sections pin-ended about the minor axis at 400 °C
The graphs at mid-height include the behaviour of sections with a cross-sectional slenderness ratio of $\lambda_{p,20°C} = 0.64$ with actual measured material behaviour (left) and $\lambda_{p,20°C} = 0.58$ with nominal material behaviour (right). The cross-sections with a slenderness ratio of $\lambda_{p,20°C} = 0.58$ are classified as class 3 for both design approaches. The overall shapes of the buckling curves according to the two design approaches are again similar. The cross-sectional resistance to pure compression, represented on the left vertical axis of the graphs, however, is again different, since to determine the resistance the carbon steel approach uses $f_{2,0,6}$ and the stainless steel approach uses $f_{p,0,2,0}$. The cross-sectional capacity obtained from the FE simulation is between the values obtained from the two design proposals. Both design approaches exhibit a short horizontal plateau at the beginning of the buckling curves, indicating that no global stability failure will occur for these overall slenderness ratios. The FE results show that the column with an overall slenderness ratio of $\lambda_{k,20°C} = 0.25$ fails in a global buckling mode when its cross-sectional slenderness ratio is $\lambda_{p,20°C} = 0.30$ and fails in a local buckling mode due to the higher slenderness ratio of the cross-section of $\lambda_{p,20°C} = 0.58$, resulting in a short plateau for small overall slenderness ratios (i.e. very short columns) similar to that of the design approaches. The resistances to pure compression according to the carbon steel approach greatly overestimate the resistance resulting from FE simulations for $\lambda_{k,20°C} < 1.25$, but fit well for higher overall slenderness ratios. This discrepancy for short columns is mainly due to the overestimation of the cross-sectional resistance to pure compression (Chapter 3.2). Nevertheless, the overall shape of the buckling curve compared to the FE results seems to be too steep. The buckling curve according to the stainless steel approach fits the FE results slightly better, but again the overall shape of the curve does not correspond to the development of the resistance with the slenderness ratio obtained from the FE simulations.

The cross-section with a slenderness ratio of $\lambda_{p,20°C} \geq 0.64$ shown in the graphs on the left at mid-height and at the bottom are classified as class 4 for both design approaches. The overall shapes of the buckling curves for the two design approaches within one graph are practically identical for overall slenderness ratios higher than $\lambda_{k,20°C} = 0.5$. The cross-sectional resistances to pure compression, represented on the left vertical axes of the graphs, are different according to both design proposals, because of slightly different formulations of the reduction factors of the cross-sectional area for class 4 sections. Both design approaches exhibit a short horizontal plateau at the beginning of the buckling curves, indicating that no global stability failure will occur for these overall slenderness ratios. The FE simulation results for this cross-sectional slenderness ratio indicate a similar plateau for small overall slenderness ratios (i.e. very short columns). The column with an overall slenderness ratio of $\lambda_{k,20°C} = 0.25$ that failed in a global buckling mode when its cross-sectional slenderness ratio was of $\lambda_{p,20°C} = 0.3$ now fails in a local buckling mode due to the greater slenderness ratio of the cross-section. Both design approaches underestimate the resistance to pure compression obtained from the FE results.

### 4.2.2 High Temperatures

Figure 4.6 to Figure 4.10 present the graphs with the comparison of the resistance of steel columns to pure compression of test results, FE simulations and the carbon and stainless steel approaches for high temperatures. Figure 4.6 treats the SHS section, Figure 4.7 and Figure 4.8 the RHS about the major and the minor axis, respectively, and Figure 4.9 and Figure 4.10 the HEA section again about the major and the minor axis. The graphs on the left within each Figure include the results of the FE study and the design approaches determined with the actual material behaviour from the tensile coupon tests while the graphs on the right present the results obtained using the material model for S355 of EN 1993-1-2. Three different cross-sectional slenderness ratios are presented for each type of cross-section. The first cross-sectional slenderness ratio of $\lambda_{p,20°C} = 0.3$, represented by the graphs at the top, corresponds to class 1 sections. The second cross-sectional slenderness ratio of $\lambda_{p,20°C} = 0.6$, represented by the graphs in the middle, includes box sections classified as class 2 at ambient and class 3 at elevated temperatures for carbon steel, and class 4 for stainless steel, and HEA sections of class 3 for both design approaches. The third cross-sectional slenderness ratio of about $\lambda_{p,20°C} = 0.8$, represented by graphs at the bottom considers class 4 sections.

The stub and slender column test results for 700 °C and a strain rate of 0.1 %/min are included in the graphs on the left side corresponding to the cross-sectional slenderness ratio of the test specimens of each cross-section, represented by black symbols. The FE results for different cross-sectional slender-
Figure 4.6  Flexural buckling resistance of SHS sections at high temperatures (700 °C)
Figure 4.7  Flexural buckling resistance of RHS sections pin-ended about the major axis at 700 °C.
Influence of the slenderness ratio, the cross-section and the material behaviour

Figure 4.8  Flexural buckling resistance of RHS sections pin-ended about the minor axis at 700 °C
Figure 4.9 Flexural buckling resistance of HEA sections pin-ended about the major axis at 700 °C
Influence of the slenderness ratio, the cross-section and the material behaviour

Figure 4.10 Flexural buckling resistance of HEA sections pin-ended about the minor axis at 700 °C
ness ratios are represented in all graphs by white symbols. The continuous lines represent the carbon steel approach and the dashed lines the stainless steel approach.

The graphs on the left of each page show the results obtained using the measured tensile coupon test material behaviour. The non-linear branch of these stress-strain curves is very short and at strains smaller than 0.2% plastic strain. After this strain value the stress remains almost constant. The difference between the 0.2% proof stress $f_{p,0.2,700°C}$ and the stress at 2% total strain $f_{2.0,700°C}$ is only of 2.6% for the SHS 160·160·5 material and -5.0% for the HEA 100 material. In the case of the RHS 120·60·3.6 material the two stress values were even identical. This leads to almost identical ultimate loads for compact and semi-compact sections for both design approaches. Only for slender sections (graphs on the left side at the bottom of the pages) the cross-sectional resistance of the carbon steel approach is slightly higher than that of the stainless steel approach. For all cross-sectional slenderness ratios and all three types of cross-section, however, both design approaches fit the FE results quite well, when the actual material behaviour is used.

The graphs on the right show the results obtained using the material behaviour of Rubert-Schaumann for S355 steel according to EN 1993-1-2. This material model assumes a non-linear stress-strain relationship up to strain values of 2%. The difference between the $f_{2,0,700°C}$ and the $f_{p,0.2,700°C}$ of this model is 43.5% (calculated using the reduction factors $k_y,700°C$ and $k_p,0.2,700°C$). As a result the ultimate loads determined with the FE simulations, but also with the design approaches are considerably higher for small cross-sectional slenderness ratios and lower for high cross-sectional slenderness ratios compared to the graphs on the left. In addition, the design approaches now again have the same difficulties predicting the resistance of columns to pure compression as at 400°C.

4.3 Conclusions

The load-bearing capacity of steel columns depends on the mechanical properties of the material, the geometry of the cross-section and the effective length of the column. The mechanical properties of the material define the maximum possible capacity of the steel. Together with the geometry of the cross-section the material behaviour influences the cross-sectional slenderness ratio and the local buckling behaviour. The cross-sectional capacity decreases with increasing cross-sectional slenderness ratios and serves as an upper boundary of the load-bearing capacity of a column. The effective length, defined by the actual length and the boundary conditions at both ends of the column, together with the geometry of the cross-section and the material behaviour define the overall slenderness ratio of the column. Increasing overall slenderness ratios lead to decreasing load-bearing capacities of the column.

Therefore, it is crucial to have an appropriate material model and to correctly apply this material model in the determination of any cross-sectional resistance to be able to determine the load-bearing capacity of steel columns sufficiently well. The common carbon steel approach (CSA) and stainless steel approach (SSA), which are based on bilinear material behaviour, have difficulty in correctly determining the cross-sectional capacity. The common buckling curves use reduction factors to reduce the cross-sectional capacity in order to predict the load-bearing capacity of columns. If the cross-sectional capacity is not predicted correctly, the buckling curve begins at the wrong starting point. At elevated temperatures this is the case for all class 2 and 3 cross-sections and even some cross-sections from class 1, if the CSA is used and all cross-sections of classes 1 to 3 if the SSA is used. Nevertheless, the load-bearing capacity is correctly determined even in these cases for very slender (very long and thin) columns, where the global buckling occurs already within the elastic range of the material.

In the case of very stocky class 1 cross-sections, with the CSA and most of the class 4 cross-sections with both design approaches at elevated temperatures the cross-sectional capacity can be determined correctly. In these cases it is evident that the shape of the buckling curves does not correctly describe the decrease of the load-carrying behaviour of steel columns with increasing overall slenderness ratios in the case of a non-linear stress-strain relationship.
At high temperatures the actual material behaviour is almost bilinear again. This leads to a correct prediction of the cross-sectional capacity and, therefore, a correct starting point of the buckling curve. Moreover, the shape of the buckling curve in the case of bilinear material behaviour fits the reduction of the load-bearing capacity of the FE results with increasing overall slenderness ratios very well.

It may be concluded that some of the difficulties of correctly predicting the load-bearing capacity of steel columns with non-linear material behaviour stem from the determination of the cross-sectional capacity. But even if the prediction of the cross-sectional capacity is correct, the buckling curves do not correctly describe the decrease of the load-bearing capacity of columns with increasing overall slenderness ratios, if the material behaviour is non-linear.
5 CONCLUSIONS AND OUTLOOK

5.1 Conclusions

In order to analyse the load-carrying behaviour of carbon steel columns in fire it is crucial to correctly implement the material behaviour and the cross-sectional capacity. The strength of the material forms an upper boundary for the cross-sectional resistance, which is limited by local buckling instabilities. The actual cross-sectional resistance represents again an upper boundary for the load-carrying behaviour of a column, which is limited by member buckling instabilities.

The material behaviour of carbon steel in the range of elevated temperatures between 300 °C and 600 °C differs strongly from the ambient temperature behaviour. The non-linearity of the stress-strain relationship within this temperature range can be described sufficiently well by the material model of Rubert-Schaumann. However, the material model of Ramberg-Osgood provides a better fit to the test results.

The material behaviour of carbon steel in the range of high temperatures (above 600 °C) is similar to the ambient temperature behaviour and a bilinear model with reduced strength and stiffness could be used again. The non-linear material models of Rubert-Schaumann and of Ramberg-Osgood have difficulties in precisely describing the stress-strain relationship.

Cross-sections fail in local buckling if a certain deformation in compression is reached. The amount of deformation a cross-section is able to endure without the occurrence of local buckling instabilities is defined by the cross-sectional slenderness ratio. Stocky sections can endure large deformations before failing, while very slender sections may already fail within the elastic range of the material. Each cross-sectional slenderness ratio results in a different strain value, at which local buckling of the cross-section takes place. In the case of a non-linear stress-strain relationship each of these strain values is accompanied by an individual stress value defining the cross-sectional capacity of the section.

Two common European design approaches, entitled here the carbon steel approach (CSA) and the stainless steel approach (SSA) determine the cross-sectional resistance of steel sections with non-linear material behaviours by defining a constant stress level and, therefore, assuming a bilinear stress-strain relationship. Both models lead to incorrect predictions of the ultimate loads of the cross-sections for a large range of slenderness ratios. The carbon steel approach overestimates the resistance of class 2 and 3 sections, while the stainless steel approach underestimates the resistance of class 1 to 3 sections.

The resistance to column buckling is commonly determined with the help of buckling curves. The cross-sectional resistance is reduced depending on the overall slenderness ratio of the column. The buckling curves of the carbon and stainless steel approaches have difficulties in predicting the ultimate load of steel columns at elevated temperatures, because the cross-sectional capacity has not been determined correctly. In cases where the correct cross-sectional resistance is available, the buckling curves ignore the non-linearity of the material behaviour and fail to correctly predict the ultimate loads of the columns.
5.2 Outlook

This work analyses the behaviour of carbon steel columns in fire and compares it to two common existing design models. Of course there are a large number of additional models available in the literature to describe the non-linear material behaviour as well as the resulting cross-sectional resistance or the load-bearing capacity of steel columns. Starting with the material behaviour and then continuing to the cross-sectional capacity and finally to the member stability these models will have to be analysed one by one and evaluated until in the end a set of formulations is found that allow a satisfactory determination of the stress-strain relationship of the material for different temperatures and strain or heating rates as well as the resulting load-bearing capacities of cross-sections and columns.

The cross-sectional resistance to shear, torsion or any interactions between compression, bending, shear and torsion (except those already considered here) of steel sections in fire has not been analysed so far. This analysis would be necessary to gain a complete knowledge of the influence of the non-linear stress-strain relationship of the material on the cross-sectional resistance. The same applies in the case of the load-bearing capacity of steel members. This work is limited to centrically loaded columns, whereas the behaviour of beams or beam-columns has not been analysed here.

The development of residual stresses within a steel column in the case of fire is not analysed here. The heating rate, the strain rate as well as constant load or temperature levels over a certain duration influence the residual stress distribution and the maximum or minimum residual stress values within the section. This development and its influence on the load-bearing capacity of carbon steel cross-sections and columns during a fire have not yet been investigated.

It was found that the strain rate of a steady-state test has a marked influence on the material behaviour of carbon steel at elevated and high temperatures. Material tests are often executed using the steady-state test, while structural furnace tests are often performed using the transient-state method, that corresponds better to real fire situations. In this case, the steady-state material test is influenced by the strain rate and the transient-state structural test is influenced by the heating rate. An investigation on the relationship between the strain and the heating rate would help to answer the question of the correct strain rate for the material test and the corresponding heating rate for the transient-state structural furnace test.
Tensile material coupon tests

APPENDIX A: TEST SERIES

A.1 TENSILE MATERIAL COUPON TESTS

Material coupon tests have been executed at the laboratory of the Institute of Structural Engineering (IBK) at the ETH Zurich to determine the behaviour of the materials used for the structural furnace tests. Different test setups and specimen geometries have been investigated in order to find the right combination. All of these tests are described in detail in Pauli et al. 2012. Here only the tensile coupon tests executed with the final test setup are described and some selected results are presented. They are referred to as the material coupon tests of Pauli et al.

In addition, tensile material coupon tests executed by K. W. Poh at the BHP Research Laboratories, Melbourne, Australia, are presented to gain additional data.

A.1.1 PAULI ET. AL.

The tests briefly described here correspond to the test series 'M7' to 'M9' of Pauli et al. 2012, where more information on the entire testing process is given. These test series contain closed-loop strain rate-controlled steady-state tensile coupon tests on specimens cut from the SHS 160·160·5, the RHS 120·60·3.6 and the HEA 100 sections used for the structural furnace tests. All of these sections were of steel grade S355 (minimum ambient temperature yield strength \( f_y,20°C = 355 \text{ N/mm}^2 \), tensile strength \( f_u,20°C = 510 \text{ N/mm}^2 \) and corresponding elongation \( \varepsilon_u,20°C = 15 \% \)). The tests were executed at the same temperatures as the structural furnace tests, i.e. 20°C, 400°C, 550°C and 700°C. The tests on the SHS 160·160·5 specimens were executed with constant strain rates of 0.50 %/min, 0.10 %/min and 0.02 %/min controlled via the extensometer. The two slower strain rates were chosen to match those of the stub and slender column tests executed on this section. The fastest strain rate was chosen to get additional information on the influence of the strain rate on the material behaviour. The tests on RHS 120·60·3.6 and on HEA 100 specimens were only executed at the strain rate of 0.10 %/min. Most of the experiments were repeated at least three times to get a redundancy of the results. Table A.1 summarises the experiments.

The test specimens for the tensile material coupon tests of Pauli et al. were dogbone-shaped pieces cut from the flat faces of the box sections SHS 160·160·5 and RHS 120·60·3.6 and the web of the H-section HEA 100 used for the column tests (Figure A.1). The nominal width of the slender part of the coupon \( b_{0,nom} \) was 10 mm and the nominal thickness \( t_{0,nom} \) of the test specimens was equal to the wall thickness of the section, i.e 5 mm for the SHS 160·160·5 and the HEA 100 specimens and 3.6 mm for the RHS 120·60·3.6 specimens. The actual values of \( b_0 \) and \( t_0 \) of each test specimen were measured at 5 points indicated in Figure A.1. From the mean values of the measurement points 2, 3 and 4 of the breadth \( b_{0,234} \), the thickness \( t_{0,234} \), the cross-sectional area \( A_{0,234} = b_{0,234} \cdot t_{0,234} \) was calculated and used to determine the stress from the measured load of the experiments.
The tensile material coupon tests were executed in a universal testing machine with a capacity of ±200 kN (Figure A.2 top right). An electrical furnace with three vertically distributed heating zones was used to heat the specimens (Figure A.2 bottom). The steel temperature was controlled and measured by three Type-K thermocouples glued onto the surface of the slender part of the specimen. The vertical displacement of the test specimen was controlled and measured by a high temperature resistant extensometer (Figure A.2 bottom). After three cyclic loadings to check the alignment of the specimen with the test setup, the specimen was gradually heated to the target temperature. During the entire heating process a small constant tensile load was applied to the specimen and the thermal elongation was not restrained. The tensile load was applied to the specimen with a constant strain rate of 0.50 %/min, 0.10 %/min or 0.02 %/min (measured and controlled with the extensometer).

Table A.1

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K. W. Poh executed a large number of steady-state tensile coupon tests on specimens cut from the flanges of 7 different H or I sections and one steel plate (Table A.1). Three different steel grades were used: the Grade 300 (minimum ambient temperature yield strength fy,20°C = 320 N/mm², tensile strength fu,20°C = 430 N/mm² and corresponding elongation εu,20°C = 21 %), the Grade 300 Plus (fy,20°C = 320 N/mm², fu,20°C = 440 N/mm² and εu,20°C = 22 %) and the Grade 400 (fy,20°C = 400 N/mm², fu,20°C = 480 N/mm² and εu,20°C = 18 %). The tests were executed at temperatures between 20 °C and 1000 °C and at strain rates of 0.2 %/min and 4.8 %/min. The test specimens were cylindrical pieces cut from the flanges of the used steel sections. The nominal diameter of the cylinder was 7.3 mm over a distance of 65 mm in the middle of the specimen and of 7.7 mm at both ends of the specimen. Further information is given in Poh 1998.

The tensile tests were executed in a universal testing machine with a capacity of 300 kN. The temperature was applied using a set of water-cooled copper induction coils around the specimen and measured with three Type-K thermocouples. The vertical deformation of the specimen was measured with a pair of capacitive extensometers, placed on two sides of the specimen. After ensuring the alignment of the specimen with the test setup, the specimen was gradually heated to the target temperature. No mechanical load was applied on the specimen during the heating phase and the thermal expansion was not restrained. Then the mechanical load was applied to the specimen at constant strain rates.
A.2 Stub and slender column furnace tests

Centrally and eccentrically loaded steady-state stub and slender column furnace tests have been executed at the laboratory of the Institute of Structural Engineering (IBK) at the ETH Zurich. All of these tests are described in detail in Pauli et. al 2012.

A.2.1 Test programme

Different steel sections with different cross-sectional slenderness ratios were used for the stub and slender column furnace tests. The stub column tests lead to the cross-sectional capacity while the slender column tests describe the column buckling behaviour. In addition, the influence of the strain rate on the ultimate load was investigated. Two different hot-finished square or rectangular hollow sections (SHS 160·160·5 and RHS 120·60·3.6) and an HEA 100 were chosen for the tests (Figure A.1). For details on all of these and some additional preliminary tests, please refer to Pauli et. al 2012.

The experiments were performed using the steady-state testing method (Table A.2 and Table A.3). Tests at ambient temperature were carried out to obtain reference values. In addition, tests were performed at 400 °C, 550 °C and 700 °C. In the experiments the load was applied on most of the test specimens at a strain rate of 0.1 %/min. Additional tests were performed at slower strain rates of 0.02 %/min and/or 0.01 %/min for the SHS 160·160·5 and the HEA 100 sections. For some columns of the RHS 120·60·3.6 and the HEA 100 sections the load was applied eccentrically. The eccentricities of 10 mm, 30 mm or 50 mm were applied to cause bending about the minor axis of the box sections and the minor or the major axis of the H-section. Bending about both the minor and the major axes of a section combined with a compressive load was not investigated.

A.2.2 Test setup

The overall test setup of the slender columns tests can be seen, e. g., in Figure A.3. The reaction frame (a) used for the main tests was built using shear walls with a steel grade of S355. The electrical furnace (b) has a maximum temperature of 1000 °C, a nominal voltage of 230 V and a nominal current of 30 A. Its heating capacity is 75 kW. The size of the inner chamber of the furnace is 800 x 800 x 2000 mm. The

Table A.1 Steady-state tensile coupon test series executed by Pauli et. al. and Poh et. al.

<table>
<thead>
<tr>
<th>Steel grade</th>
<th>Section</th>
<th>Temperatures °C</th>
<th>Strain rates %/min</th>
<th>No. of tests</th>
<th>Year</th>
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<td>20, 400, 550, 700</td>
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<td>2010 - 2011</td>
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<td>HEA 100</td>
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<td>0.20 and 4.80</td>
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<td>1995</td>
</tr>
<tr>
<td></td>
<td>Welded I-section: 700WB130</td>
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<td>0.20 and 4.80</td>
<td>24</td>
<td>1995</td>
</tr>
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<td>0.20</td>
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<tr>
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<td>Hot-rolled steel plate</td>
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<td>0.20 and 4.80</td>
<td>24</td>
<td>1996</td>
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Figure A.2  Experimental setup of the tensile test series M7 to M9: Overall test setup of the Zwick testing machine, the furnace and the extensometer (top right), detail of the extensometer attached to a test specimen (top left), detailed view of the open (bottom left) and the closed (bottom right) furnace with the extensometer.
heating spirals cover all four walls from the bottom to the top of the chamber and are divided into four vertically distributed heating zones that can be heated individually. The load jack (c) is a double-action hydraulic cylinder with a capacity of 4.45 MN in compression and 1.28 MN in tension (corresponding to a hydraulic pressure of 280 bar). Two cooling plates (d) were included in the test setup to prevent the elements below and especially above the furnace from heating up. Two pistons (e) were used in the test setup, one above and one below the test specimen. They were made of steel profiles SHS 400·400·16 of steel grade S355J2H. End plates were welded to the top and the bottom of the pistons.

The air temperature in the furnace chamber was measured and controlled with Type-K thermal sensors located at the back wall of the furnace in the middle of each heating zone. The steel temperature of the test specimens was measured using Type-K thermocouples glued to the test specimens. Four load cells (f) with a nominal capacity of 450 kN (+ 50 %) each were placed above the upper piston. The vertical load was calculated as the sum of the measured vertical loads of the four cells. The relative vertical displacement (g) of the test specimens, i.e. the end shortening of the columns, was determined using two LVDT’s located underneath the furnace. They recorded the relative vertical displacement between the mid-heights of the parallel end plates above and below the test specimen using two stainless steel bars. The horizontal displacement (h) of the slender columns was measured at mid-height of the column using one LVDT on each side of the furnace.

The centrically loaded stub columns were loaded with restrained end conditions at the top and the bottom of the test specimen. The eccentrically loaded stub and all the slender columns were loaded with restrained end conditions about one axis and pin-ended conditions about the other axis of the cross section.

### A.2.3 Test specimens

Stub column tests were performed to get information about the cross-sectional capacity and the local buckling behaviour of the sections. The length of each test specimen was three times the nominal breadth of the section. Slender column tests were performed to get information about the overall buckling behaviour of each section. The length of the slender columns was limited to approximately 2 m because of the height of the furnace. In addition, some test specimens of mean length were tested of the RHS 120·60·3.6 and the HEA 100 sections to provide results for a different overall geometrical slenderness ratio. End plates of steel grade S355 were welded to both ends of each test specimen.

The actual geometry, namely the wall thickness, the width and length of the specimens and the location of the end plates of each test specimen was measured and published in Pauli et. al. 2012. From the measured actual cross-sectional geometry the cross-sectional area and the moments of inertia about the main axes were calculated (Table A.2 and Table A.3). The effective length of the slender columns corresponding to the pin-ended axis of the section was defined as the distance between centres of the rotation of the rocket or roller bearing. The effective length corresponding to the restrained axis, on the other hand, was calculated as half of the specimen length without the end plates.

The initial local geometrical imperfections of the faces of the stub columns and the initial global geometrical imperfections of the slender columns were measured. The average of the maximum deflection of all faces of each stub column test specimen, $e_{0y}$, was deduced and is given in Table A.2. The maximum value of the initial global deflections of the centre line of each slender column in the two main directions $y$ and $z$ of the section, $e_{0y,z}$, were calculated and are presented in Table A.3.
Table A.2  Steady-state stub column tests

<table>
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<tr>
<th>Specimen</th>
<th>Steel temperature [°C]</th>
<th>Strain rate [%/min]</th>
<th>End condition on axis</th>
<th>Load eccentricity [mm]</th>
<th>Cross-sectional area [mm²]</th>
<th>Moment of Inertia [·10⁶ mm⁴]</th>
<th>Specimen Length (no end plates) [mm]</th>
<th>Initial local imperfection [mm]</th>
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<td></td>
<td></td>
<td></td>
<td></td>
<td>y z e₁₁ y e₁₂ z A Iy Iz e₀</td>
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### Test Programme (nominal values)

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<td>End condition on axis</td>
<td>Load eccentricity [mm]</td>
<td>Cross-sectional area [mm²]</td>
<td>Moment of Inertia [·10⁶ mm⁴]</td>
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Figure A.3  Elevation of the experimental setup of the main slender column tests on box and H-sections
### Table A.4  Results of the steady-state material coupon tests

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<th>Yield strength [N/mm²]</th>
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Table A.5  Results of the steady-state stub column tests

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<td>tie</td>
<td>0.253</td>
<td>0.323</td>
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<td>tie</td>
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<td>1</td>
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### Table A.6  Results of the steady-state slender column tests

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Steel temperature, [°C]</th>
<th>Strain rate [%/min]</th>
<th>End condition</th>
<th>Load eccentricity [mm]</th>
<th>Cross-sectional slenderness ( \bar{\lambda}_{Y,20°C} )</th>
<th>Class of cross-section slenderness</th>
<th>Overall slenderness ratio ( \bar{\lambda}_{k,Y,20°C} )</th>
<th>( \bar{\lambda}_{k,z,20°C} )</th>
<th>Ultimate Load at ( F_u,θ ) [kN]</th>
<th>Deformation at ( F_u,θ ) [mm]</th>
<th>( 1^{st} ) and ( 2^{nd} ) order bending moment at ( F_u,θ ) [kNm]</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Series 5: SHS 160·160·5 Slender Columns</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
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</tr>
<tr>
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<td>pin</td>
<td>tie</td>
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<td>0</td>
<td>0.602</td>
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<td>0.42</td>
<td>0.19</td>
<td>98</td>
</tr>
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<td>400</td>
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<td>pin</td>
<td>tie</td>
<td>0</td>
<td>0</td>
<td>0.599</td>
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<td>0.42</td>
<td>0.19</td>
<td>760</td>
</tr>
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<td>pin</td>
<td>tie</td>
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<td>0</td>
<td>0.599</td>
<td>2 4</td>
<td>0.42</td>
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<td>1089</td>
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<td>tie</td>
<td>pin</td>
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<td><strong>Series 7: RHS 120·60·3.6 Slender Columns</strong></td>
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<td>2 4</td>
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<td>211</td>
</tr>
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<td>tie</td>
<td>pin</td>
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<td>1.06</td>
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### Selected test results

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<th>Specimen</th>
<th>Test programme (nominal values)</th>
<th>Specimen slenderness (actual values)</th>
<th>Test results (actual values)</th>
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<tbody>
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<td>Specimen</td>
<td>(Steel temperature, °C)</td>
<td>Strain rate [%/min]</td>
<td>End condition on axis</td>
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<td>Series 9: HEA 100 Slender Columns</td>
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<td>tie</td>
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<td>700</td>
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<td>pin</td>
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<tr>
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<td>tie</td>
</tr>
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<td>pin</td>
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<td>0.10</td>
<td>tie</td>
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<td>550</td>
<td>0.02</td>
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<tr>
<td>L36</td>
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Figure A.4  Load-deformation curves of the tensile material coupon tests and the stub and slender column tests of RHS 120·60·3.6 test specimens, loaded in compression.
Figure A.5  Load-deformation curves of the tensile material coupon tests and the stub and slender column tests of SHS 160·160·5 test specimens, loaded in compression.
Figure A.6 Load-deformation curves of the tensile material coupon tests and the stub and slender column tests of HEA 100 test specimens, loaded in compression
Figure A.7  M-N Interaction of the stub and slender column tests
APPENDIX B: THE FINITE ELEMENT MODEL

B.1 CROSS-SECTIONAL CAPACITY

The finite element (FE) software ABAQUS, Rel. 6.10-1, was used to numerically determine the ultimate strength of different steel members including geometric and material non-linearity and initial imperfections. Stub columns were simulated to numerically analyse the cross-sectional capacity depending on the material behaviour and the cross-sectional slenderness ratio. The stub columns were modelled using reduced integrated 4-node shell elements (designated as S4R general purpose linear shell elements in the ABAQUS element library). The study was limited to three types of cross-section, i.e. a square hollow section (SHS), a rectangular hollow section (RHS) with an aspect ratio of 1:2 and an H-section (HEA) with an aspect ratio of 1:1.

B.1.1 MODELLING THE GEOMETRY

The width B and the height H of the cross-sections (Figure B.1) were chosen equal to those of the cross-sections used in the column furnace tests. All stub columns were modelled with a length of the specimens equal to three times the height of the cross section, which corresponded to the length of the stub column furnace test specimens without the end plates.

SHS:  \( H = 160 \text{ mm} \), \( B = 160 \text{ mm} \), \( L_0 = 480 \text{ mm} \)

RHS:  \( H = 120 \text{ mm} \), \( B = 60 \text{ mm} \), \( L_0 = 360 \text{ mm} \)

HEA:  \( H = 100 \text{ mm} \), \( B = 100 \text{ mm} \), \( L_0 = 300 \text{ mm} \)

Stub columns of all three types of cross-section were modelled with varying cross-sectional slenderness ratios. The wall thickness (resp., the web thickness in the case of the H-section) was chosen to obtain predefined cross-sectional slenderness ratios \( \varepsilon \cdot h/t = 10 \) to \( \varepsilon \cdot h/t = 60 \). The factor \( \varepsilon \) describes the relation between the actual ambient temperature yield strength \( f_y,20^\circ\text{C} \) and the nominal ambient temperature yield strength of 235 N/mm².

\[ \varepsilon = \frac{235}{f_y,20^\circ\text{C}} \]

The actual ambient temperature yield strength \( f_y,20^\circ\text{C} \) was taken from the ambient temperature tensile coupon tests performed on the material of the column furnace test specimens and from the nominal steel grade S355 according to EN 1993-1-1 2005:
The internal and external corner radii of an SHS or an RHS of \( r_i = 0.75 \cdot t \) and \( r_a = 1.75 \cdot t \) were modelled as concentric quarters of a circle (Figure B.2) leading to an average corner radii of \( r_m = 1.25 \cdot t \). The web thickness \( t_w \) of the H-section was chosen to obtain the predefined cross-sectional slenderness ratios \( \varepsilon \cdot h/t_w \). The flange thickness \( t_f \) and the radius of the fillet \( r \) were defined in relation to the web thickness \( t_w \) as:

\[
    t_f = 1.6 \cdot t_w \\
    r = 2.0 \cdot t_w
\]

This corresponds to the ratios of flange to web thickness and radius to web thickness of the smaller standardised European HEA cross-sections usually used as compression members.

<table>
<thead>
<tr>
<th>Table B.1</th>
<th>Cross-sectional slenderness ratios and resulting wall thicknesses</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \varepsilon \cdot b/t )</td>
<td>10</td>
</tr>
<tr>
<td><strong>SHS</strong></td>
<td>( f_y,20^\circ C,SHS ) = 360 N/mm²</td>
</tr>
<tr>
<td></td>
<td>( \varepsilon_{SHS} = 0.81 )</td>
</tr>
<tr>
<td><strong>RHS</strong></td>
<td>( f_y,20^\circ C,RHS ) = 370 N/mm²</td>
</tr>
<tr>
<td></td>
<td>( \varepsilon_{RHS} = 0.80 )</td>
</tr>
<tr>
<td><strong>HEA</strong></td>
<td>( f_y,20^\circ C,HEA ) = 425 N/mm²</td>
</tr>
<tr>
<td></td>
<td>( \varepsilon_{HEA} = 0.74 )</td>
</tr>
<tr>
<td></td>
<td>( \varepsilon_{nom} = 0.81 )</td>
</tr>
<tr>
<td></td>
<td>( \varepsilon_{nom} = 0.81 )</td>
</tr>
</tbody>
</table>
Table B.1 contains the resulting wall thicknesses and the non-dimensional ambient temperature cross-sectional slenderness ratio

$$\lambda = \frac{h}{t} \sqrt{\frac{28.4 \cdot \varepsilon}{\kappa}}$$

for each section and slenderness ratio used in the FE study. The factor $k = 4$ for plates with simple supports on both edges and $k = 0.426$ for plates with a simple support on one edge under pure compression is defined by EN 1993-1-5 2007. The last column of Table B.1 contains the wall-thicknesses and slenderness ratios corresponding to the test specimens of the column furnace tests. An equivalent constant wall thickness was used for the two box sections from the average of the measured cross-sectional areas of the test specimens. In the case of the HEA 100 section the averages of the measured web and flange thicknesses were used for the simulations.

The simulations were executed using the reduced integrated 4-node shell elements S4R for the entire geometry. A mesh refinement with six shell elements as a quarter of a circle was used to model the corners of the box sections (Figure B.2). The fillet of the H-section was considered by increasing the thickness of the adjacent flange elements resulting in a coextensive cross section. Rigid beam connections were used for connecting the web and the flanges (Figure B.2).

**B.1.2 Imperfections and Residual Stresses**

The shape of the initial local geometrical imperfections of the simulated stub columns was determined from the first (symmetrical) local buckling eigenmode due to pure compression from a linear elastic analysis (*Buckling) provided by the ABAQUS software (Figure B.3). The magnitude of the eigenmode $e_{0,\text{local,meas}}$ was the average of the measured maximum deflections of the faces of the test specimens:

- **SHS 160·160·5**: $e_{0,\text{local,meas}} = 0.48$ mm
- **RHS 120·60·3.6**: $e_{0,\text{local,meas}} = 0.29$ mm
- **HEA 100**: $e_{0,\text{local,meas}} = 0.15$ mm

No residual stresses were included in the FE analysis.
The material behaviour was divided into an elastic and an inelastic (plastic) segment. The elastic segment (ABAQUS command *ELASTIC) was defined by the Young's Modulus $E_{0,0}$ and the Poisson's ratio $\nu_{0} = 0.3$ for each of the investigated temperatures $\theta$. In simulations with nominal material behaviour the elastic material parameters for carbon steel S355 were taken from EN 1993-1-2 2006 using the reduction factor $k_{E,\theta}$ for elevated temperatures. In simulations with actual material behaviour the Young's modulus was taken from the tensile coupon test results (strain rates of 0.10 %/min), whereas the Poisson's ratio was still nominal. Table B.2 summarises the elastic material parameters used for the simulations.

The inelastic segment (ABAQUS command *PLASTIC) was defined by a polygonal true stress-logarithmic strain relationship for each temperature. In simulations with nominal material behaviour the stress-strain relationship for carbon steel S355 was taken from EN 1993-1-2 2006 using the reduction factor $k_{y,\theta}$ for elevated temperatures (Figure B.4). In simulations with actual material behaviour the stress-strain relationship was taken from the tensile coupon test results (strain rates of 0.10 %/min). In both cases the inelastic ambient temperature material behaviour was modelled without any strain-hardening effects (Figure B.4).

<table>
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<tr>
<th>Temperature [°C]</th>
<th>$E_{0,0,\text{nom}}$ [N/mm²]</th>
<th>$E_{0,0,\text{meas}}$ [N/mm²]</th>
<th>$E_{0,0,\text{meas}}$ [N/mm²]</th>
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<tr>
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<td>161'000</td>
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<td>27'300</td>
<td>38'400</td>
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</table>
Ideal simply supported boundary conditions were realized for both ends of the specimens. A set of additional nodes, located in exactly the same positions as the nodes at each end of the column, but without any element attached to them, modelled the rigid end plate (Figure B.5). A reference node at the centre of the plate was connected with rigid multi-point constraints to the other nodes of the rigid end plate. Kinematic coupling constraints tied each of the six degrees of freedom of each node within the original node set at the end of the column to its twin in the rigid end plate. This way, the end of the column was able to translate and rotate in any direction, while the coupling to the rigid end plate ensured that the end of the column remained plane. Warping moments were able to develop in the column, ensuring the reaching of the full plastic cross-sectional resistance.

The modelled columns had to be fixed within the virtual space of the simulation. The translations of the nodes at the centre of each face were fixed in the longitudinal direction and in the lateral direction parallel to the face (Figure B.6). The lateral translation perpendicular to the face as well as all rotations were free. In the case of the H-section additional nodes in the middle of the free edges of the flanges were also fixed in the longitudinal direction. These point-wise boundary conditions had no influence on the behaviour of the simulated columns due to the symmetry of the model.

The temperature was applied to the model as an initial condition defining the material behaviour and kept constant during the entire analysis. No thermal expansion or temperature gradients were modelled.

### B.1.4.1 Pure compression

The ambient temperature plastic resistance $N_{pl,20^\circ C} = A \cdot f_{y,20^\circ C}$ was applied to the reference points of the rigid end plates positioned at both ends of the column (Figure B.6). The lateral displacements in the direction of the y and z axes of the two reference nodes were blocked and only longitudinal shortening in the direction of the x-axis was allowed. During the static analysis the load was increased incrementally until failure of the specimen. The simulation was not limited by any maximum strain considerations or deformation criteria.
B.1.4.2 Pure bending

The ambient temperature plastic resistance $M_y/z,_{pl,20°C} = W_y/z,_{pl} f_y/20°C$ about the major or minor axis is applied to the reference points of the rigid end plates positioned at both ends of the column (Figure B.6). All six degrees of freedom of the two reference points were free. During the static analysis the load was increased incrementally until failure of the specimen. The simulation was not limited by any maximum strain considerations or deformation criteria.

B.1.4.3 Axial compression - uniaxial bending moment interaction

The ambient temperature plastic resistance $N_{pl,20°C} = A f_y/20°C$ was applied to the reference points of the rigid end plates positioned at both ends of the column (Figure B.6). In the same step a bending moment about either the major or the minor axis was applied to the column as $M_y/z = N_{pl,20°C} \cdot e_1$. The eccentricity $e_1$ varied between 0 and 999 mm to simulate different interactions of compression and bending moments. All six degrees of freedom of the two reference points were free. During the static analysis the load was increased incrementally until failure of the specimen. The simulation was not limited by any maximum strain considerations or deformation criteria.

B.2 Member Stability

The numerical simulation of the slender columns are based on the stub column model. The columns were again modelled using reduced integrated 4-node shell elements (designated as S4R general purpose linear shell elements in the ABAQUS element library). The same types of cross-section with the same cross-sectional slenderness ratios were used.

B.2.1 Modelling the Geometry

Three different cross-sectional slenderness ratios were analysed for each type of cross-section (SHS, RHS and HEA).

- SHS 160·160·5: $\overline{\lambda}_{p,20°C} = 0.27 \quad 0.60 \quad 0.81$
- RHS 120·60·3.6: $\overline{\lambda}_{p,20°C} = 0.28 \quad 0.62 \quad 0.83$
- HEA 100: $\overline{\lambda}_{p,20°C} = 0.33 \quad 0.64 \quad 0.80$

In the case of the HEA section the difference between the actual and the nominal ambient temperature yield strength led to different cross-sectional slenderness ratios for the same geometry. In the simulations with nominal material behaviour the cross-sectional slenderness ratios of the HEA were $\overline{\lambda}_{p,20°C} = 0.30$, 0.58 and 0.73.

The non-dimensional overall slenderness ratio at ambient temperature $\overline{\lambda}_{k,20°C}$ of the columns was varied between 0.25 and 2.50. The slenderness ratio is defined as:

$$\overline{\lambda}_{k,20°C} = \frac{L_k \sqrt{A/I}}{\pi \sqrt{E_{0,20°C}/f_{y,20°C}}}$$

The cross-sectional area $A$ and the moment of inertia $I$ (of either the major or the minor axis) were known from the stub column simulations. The material properties $E_{0,20°C}$ and $f_{y,20°C}$ were taken from either the tensile material coupon test results or the nominal S355 according to EN 1993-1-2, 2006, as was done for the stub column simulations. The resulting effective lengths $L_k$ for both axes of the sections are summarised in Table B.3 and Table B.4.
Table B.3  Non-dimensional overall slenderness ratios and resulting effective lengths [mm] for the actual material behaviour from the tensile material coupon tests

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Table B.4  Non-dimensional overall slenderness ratios and resulting effective lengths [mm] for the nominal material behaviour of S355 according to EN 1993-1-2

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B.2.2 Imperfections and Residual Stresses

The shape of the initial geometrical imperfections of the simulated slender columns was determined from the first global buckling eigenmode due to pure compression from a linear elastic analysis (*Buckling) provided by the ABAQUS software (Figure B.7) and the corresponding first local buckling eigenmode of a column with the effective length of 4 m (Figure B.8). The magnitude of the global imperfection was $L_0/1000$, while the magnitude of the local imperfection was identical to the stub column simulations:

- SHS 160·160·5: $e_{0, \text{local, meas}} = 0.48 \text{ mm}$
- RHS 120·60·3.6: $e_{0, \text{local, meas}} = 0.29 \text{ mm}$
- HEA 100: $e_{0, \text{local, meas}} = 0.15 \text{ mm}$

Some of the columns with an overall slenderness ratio of $\lambda_{k, 20^\circ C} = 0.25$ exhibited local failure modes. In these cases the simulations were repeated without any global imperfections and with local imperfections corresponding to the actual effective length of the simulated column.

No residual stresses were included in the FE analysis.

B.2.3 Material

The simulation of the material properties was exactly the same as in the case of the FE analysis of the cross-sectional capacity.

B.2.4 Boundary Conditions and Load Applications

The ideal simply supported boundary conditions at both ends of the specimens modelled in the simulations of the member stability behaviour were very similar to those of the cross-sectional capacity simulations. Again a set of additional nodes, located in exactly the same positions as the nodes at each end of the column, but without any element attached to them, modelled the rigid end plates (Figure B.5). The reference node connected to the nodes of the end plate with rigid multi-point constraints was now located 73 mm away from the end plate, leading to an effective length of the column slightly longer than the length of the specimen itself. The distance of 73 mm on each side corresponds to the distance between the centre of rotation of the pin-ended roller bearing of the column tests and the end of the test specimens.

Kinematic coupling constraints tied each of the six degrees of freedom of each node within the original node set at the end of the column to its twin in the rigid end plate. In this way, the end of the column was able to translate and rotate in any direction, while the coupling to the rigid end plate ensured that the end of the column remained plane. Warping moments were able to build in the column, ensuring the reaching of the full plastic cross-sectional resistance.

The modelled columns had to be fixed within the virtual space of the simulation. The translations of the nodes at the centre of each face were fixed in the longitudinal direction and in the lateral direction parallel to the face (Figure B.6). The lateral translation perpendicular to the face as well as all rotations were free. In the case of the H-section additional nodes in the middle of the free edges of the flanges were also fixed in the longitudinal direction. These point-wise boundary conditions had no influence on the behaviour of the simulated column due to the symmetry of the model.

The temperature was applied to the model as an initial condition defining the material behaviour and kept constant during the entire analysis. No thermal expansion or temperature gradients were modelled.
Figure B.7 The first global buckling eigenmode due to pure compression of the simulated columns determined with the ABAQUS software

Figure B.8 The local buckling eigenmode due to pure compression of the simulated columns determined with the ABAQUS software
The ambient temperature plastic resistance $N_{pl,20^\circ C} = A \cdot f_y,20^\circ C$ was applied to the reference points of the rigid end plates positioned at both ends of the column (Figure B.6). In these simulations one lateral displacement in the direction of the y or z axes of the two reference nodes was blocked to allow for a global buckling shape to form in the direction of the desired axis. During the static analysis the load was increased incrementally until failure of the specimen. The simulation was not limited by any maximum strain considerations or deformation criteria.

### B.3 Accuracy of the Finite Element Model

The finite element model was built to simulate the stub and slender columns as realistically as possible, while still being simple enough to handle the large amount of data and number of simulations.

The same cross-section was used to simulate all test specimens of one type of cross-section. A mean value was used for the wall thickness and nominal values were used for the width and height of the section and the length of the specimen. There are some differences between the geometry of the simulated cross-sections and those of the test specimens. Especially the corner radii of the box sections and the fillet of the H-section proved difficult. The difference for the cross-sectional area and the moments of inertia between the test specimens and the corresponding simulated sections are summarised in Table B.5.

For the shape of the initial imperfections the model used the first eigenmode and not the measured distribution of the test specimens. The magnitude of the initial local imperfections was taken from the measurements of the test specimens for all simulated cross-sectional slenderness ratios. Therefore, there is a difference to the two design approaches used in the comparison. The magnitude of the initial global imperfections was taken equal to that of the design approaches of $l/1000$, where $l$ is the column length.

No residual stresses were taken into account in the FE analysis. The development of residual stresses within a steel column in the case of fire is difficult to determine. The heating rate, the strain rate as well as constant load or temperature levels over a certain amount of time influence the residual stress distribution and the maximum or minimum residual stress values within the section. This development and its influence on the load-bearing capacity of carbon steel cross-sections and columns during a fire are not well known and it was decided to perform the simulations without residual stresses.

| Table B.5 Differences between the test specimens (average), the simulated columns and the design approaches |
|-----------------|-----------------|-----------------|
| SHS             | RHS             | HEA             |
| Geometry        |                 |                 |
| $A_0$ [mm$^2$]  | $I_y$ [mm$^4$]  | $I_z$ [mm$^4$]  |
| Test            | 3307            | 13238465        | 13223778        |
| FE / CSA / SSA  | 3363            | 13407155        | 13407155        |
| Geometry        |                 |                 |
| $A_0$ [mm$^2$]  | $I_y$ [mm$^4$]  | $I_z$ [mm$^4$]  |
| Imperfections   | $e_{0,\text{local}}$ [mm] | $e_{0,y,\text{global}}$ [mm] | $e_{0,z,\text{global}}$ [mm] |
| Test            | 0.48            | 0.73            | 0.74            |
| FE              | 0.48 L / 1000   | L / 1000        | L / 1000        |
| CSA*            | 0.29 L / 1000   | L / 1000        | L / 1000        |
| SSA*            | 0.15 L / 1000   | L / 1000        | L / 1000        |
| $h$ / 200       | L / 1000        | L / 1000        | L / 1000        |
| $h$ / 200       | L / 1000        | L / 1000        | L / 1000        |
| $h$ / 200       | L / 1000        | L / 1000        | L / 1000        |
| $h$ / 200       | L / 1000        | L / 1000        | L / 1000        |

* including residual stresses
APPENDIX C: CROSS-SECTIONAL CAPACITY

C.1 PURE COMPRESSION - ADDITIONAL TEMPERATURES

C.1.1 20°C
C.1.2 550°C
C.2 Pure Bending - Additional Temperatures

C.2.1 20°C
S355 of EN 1993-1-1/2

Material

Tensile test result

Data

FEA

CSA

SSA

RHS 120·60·x, 20 °C

M_{z,u,θ} [kNm]

CSA Class

SSA Class

0.00 0.25 0.50 0.75 1.00 1.25 1.50 1.75 2.00

0

M_{z,u,θ} [kNm]

CSA Class

SSA Class

0.00 0.25 0.50 0.75 1.00 1.25 1.50 1.75 2.00

0

HEA 100·100·x, 20 °C

RHS 120·60·x, 20 °C

Pure Bending - Additional Temperatures
C.2.2 550°C
C.3 Axial Compression - uniaxial Bending Moment Interaction - Additional Temperatures and Slenderness Ratios

C.3.1 20°C
Axial Compression - uniaxial Bending Moment Interaction - Additional Temperatures and Slenderness Ratios
Axial Compression - uniaxial Bending Moment Interaction - Additional Temperatures and Slenderness Ratios
CROSS-SECTIONAL CAPACITY
C.3.2 400°C
Axial Compression - uniaxial Bending Moment Interaction - Additional Temperatures and Slenderness Ratios
Axial Compression - uniaxial Bending Moment Interaction - Additional Temperatures and Slenderness Ratios
C.3.3 550°C
Axial Compression – uniaxial Bending Moment Interaction – Additional Temperatures and Slenderness Ratios

Material Tensile test result

$\lambda_{p,20°C}$

CSA

SSA

Data

○ FEA

- - - CSA

- - - SSA

$\tau_{\mu,\theta} = \lambda_{p,20°C}$
Axial Compression - uniaxial Bending Moment Interaction - Additional Temperatures and Slenderness Ratios

Data

Material Tensile test result

λ = 1.11

λ = 1.27

λ = 1.43

λ = 1.59

λ = 1.75

λ = 1.91
C.3.4 700°C
CROSS-SECTIONAL CAPACITY

\[ N_{n,0} \text{ [kN]} \]

Data
- FEA
- CSA
- SSA
Material
Tensile test result

\[ M (N_{n,0}) \text{ [kNm]} \]

\[ \tau_{n,0} = 0.94 \]

\[ \tau_{n,0} = 1.08 \]

\[ \tau_{n,0} = 1.21 \]

\[ \tau_{n,0} = 1.35 \]

\[ \tau_{n,0} = 1.48 \]

\[ \tau_{n,0} = 1.62 \]
Axial Compression - uniaxial Bending Moment Interaction - Additional Temperatures and Slenderness Ratios

\[ N_{u,0} \text{ [kN]} \]

\[ M_z(N_{u,0}) \text{ [kNm]} \]

Data

- FEA
- CSA
- SSA

Material

- Tensile test result

\[ \lambda = 0.97 \]

\[ \lambda = 1.11 \]

\[ \lambda = 1.25 \]

\[ \lambda = 1.39 \]

\[ \lambda = 1.52 \]

\[ \lambda = 1.66 \]
Axial Compression - uniaxial Bending Moment Interaction - Additional Temperatures and Slenderness Ratios

Material

Tensile test result

$N_{u,0}$ [kN] HEA 100·100·x, 700 °C

$M_z (N_{u,0}) [kNm]$ 

$\lambda_{p,20°C} = 0.32$

Data

○ FEA

- FEA

Material

Tensile test result

$N_{u,0}$ [kN] HEA 100·100·x, 700 °C

$M_z (N_{u,0}) [kNm]$ 

$\lambda_{p,20°C} = 1.27$

Data

○ FEA

- FEA

Material

Tensile test result

$N_{u,0}$ [kN] HEA 100·100·x, 700 °C

$M_z (N_{u,0}) [kNm]$ 

$\lambda_{p,20°C} = 1.43$

Data

○ FEA

- FEA

Material

Tensile test result

$N_{u,0}$ [kN] HEA 100·100·x, 700 °C

$M_z (N_{u,0}) [kNm]$ 

$\lambda_{p,20°C} = 1.59$

Data

○ FEA

- FEA

Material

Tensile test result

$N_{u,0}$ [kN] HEA 100·100·x, 700 °C

$M_z (N_{u,0}) [kNm]$ 

$\lambda_{p,20°C} = 1.75$

Data

○ FEA

- FEA

Material

Tensile test result

$N_{u,0}$ [kN] HEA 100·100·x, 700 °C

$M_z (N_{u,0}) [kNm]$ 

$\lambda_{p,20°C} = 1.91$

Data

○ FEA

- FEA

Material

Tensile test result
APPENDIX D: MEMBER STABILITY

D.1 PURE COMPRESSION - ADDITIONAL TEMPERATURES

D.1.1 20°C
Data FEA Material Tensile test result

Material S355 of EN 1993-1-1/2

$\lambda_{p,20\degree C} = 0.30$

Material S355 of EN 1993-1-1/2

$\lambda_{p,20\degree C} = 0.33$

Material S355 of EN 1993-1-1/2

$\lambda_{p,20\degree C} = 0.44$

Material S355 of EN 1993-1-1/2

$\lambda_{p,20\degree C} = 0.00$

Material S355 of EN 1993-1-1/2

$\lambda_{p,20\degree C} = 0.73$
D.1.2 550°C
Pure Compression - Additional Temperatures
Pure Compression - Additional Temperatures

Data
- Test
- FEA
- CSA
- SSA

Material
- HEA 100·100·x, 550 °C
- S355 of EN 1993-1-1/2

Tensile test result

λ = 0.33

$\tau_{p,20°C} = 0.30$

$\lambda = 0.64$

$\lambda = 0.58$

$\lambda = 0.73$
**NOTATION**

**CAPITAL LETTERS**

A  
**Area**  
\( A_0 \) Initial cross-sectional area  
\( A_{0,234} \) Average of the measured cross-sectional area of the tensile test specimen  
\( A_{\text{eff}} \) Cross-sectional area reduced by the effective width method

B  
**Width of the cross-section**

CSA  
**Carbon steel approach**

E  
**Slope of a stress-strain relationship**  
\( E_0 \) Young's Modulus, slope of the initial linear-elastic branch, \( E_0 = f_p / \varepsilon_p \)  
\( E_{0,\text{meas}} \) Actual Young's Modulus  
\( E_{0,\text{nom}} \) Nominal Young's Modulus according to EN 1993-1-1/2/4  
\( E_{0.2} \) Tangent Modulus at the point \( \varepsilon_{p,0.2}, f_{p,0.2} \)  
\( E_{2.0} \) Tangent Modulus at the point \( \varepsilon_{2.0}, f_{2.0} \)  
\( E_u \) Tangent Modulus at the point \( \varepsilon_u, f_u \)

F  
**External normal force**  
\( F_{u,0} \) Ultimate axial load at the temperature \( \theta \)

FE  
**Finite Element**  
FEA Finite Element analysis

H  
**Height of the cross-section**

HEA  
**H-shaped section**

I_{y/z}  
**Moment of inertia in the direction of the y/z axis**

K  
**Constant in the original Ramberg-Osgood formulation**
NOTATION

L  Test specimen length
   L_0 ........... Gauge length of a material coupon test specimen
   L_0 ........... Initial length of a column test specimen without end plates
   L_k ........... Effective length
   ΔL .......... Measured relative deformation of a test specimen during a test

LVDT  Linearly-varying displacement transducer

M  Bending moment
   M_{eff} .......... Reduced elastic resistance to bending of a class 4 cross-section
   M_{eff,CS,20°C} .... At ambient temperature (carbon steel)
   M_{eff,CS,θ} .......... At the temperature θ according to the CSA
   M_{eff,SS,θ} .......... At the temperature θ according to the SSA
   M_{el} .......... Resistance to bending with an elastic stress distribution
   M_{el,CS,20°C} .... At ambient temperature (carbon steel)
   M_{el,CS,θ} .......... At the temperature θ according to the CSA
   M_{el,SS,θ} .......... At the temperature θ according to the SSA
   M_I .......... First order bending moment
   M_{I,y,u,θ} .......... First order major axis bending moment at ultimate load F_{u,θ}
   M_{I,z,u,θ} .......... First order minor axis bending moment at ultimate load F_{u,θ}
   M_{II} .......... Second order bending moment
   M_{II,y,u,θ} .......... Second order major axis bending moment at ultimate load F_{u,θ}
   M_{II,z,u,θ} .......... Second order minor axis bending moment at ultimate load F_{u,θ}
   M_{pl} .......... Resistance to bending with a plastic stress distribution
   M_{pl,CS,20°C} .... At ambient temperature (carbon steel)
   M_{pl,CS,θ} .......... At the temperature θ according to the CSA
   M_{pl,SS,θ} .......... At the temperature θ according to the SSA
   M_{u,θ} .......... Ultimate bending moment at the temperature θ
   M_y .......... Major axis bending moment
   M_{y,eff} .......... Major axis reduced elastic resistance to pure bending
   M_{y,el} .......... Major axis elastic resistance to pure bending
   M_{y,pl} .......... Major axis plastic resistance to pure bending
   M_{y,pl,N} .......... Major axis plastic resistance allowing for normal forces
   M_{y,u,θ} .......... Ultimate major axis bending moment at the temperature θ
   M_z .......... Minor axis bending moment
   M_{z,eff} .......... Minor axis reduced elastic resistance to pure bending
   M_{z,el} .......... Minor axis elastic resistance to pure bending
   M_{z,pl} .......... Minor axis plastic resistance to pure bending
   M_{z,pl,N} .......... Minor axis plastic resistance allowing for normal forces
   M_{z,u,θ} .......... Ultimate minor axis bending moment at the temperature θ

N  Resistance to normal force
   N_{eff} .......... Reduced elastic resistance to pure compression of a class 4 cross-section
   N_{eff,CS,20°C} .... At ambient temperature (carbon steel)
   N_{eff,CS,θ} .......... At the temperature θ according to the CSA
   N_{eff,SS,θ} .......... At the temperature θ according to the SSA
   N_{pl} .......... Plastic resistance to pure compression
   N_{pl,CS,20°C} .... At ambient temperature
At the temperature \( \theta \) according to the CSA

At the temperature \( \theta \) according to the SSA

RHS Rectangular hollow section

S4R General purpose linear shell elements of the ABAQUS standard finite element library

SHS Square hollow section

SSA Stainless steel approach

W Section modulus

\( W_{el} \) Elastic section modulus

\( W_{el,eff} \) Effective elastic section modulus

\( W_{pl} \) Plastic section modulus

**Lower Case Characters**

a Ratio of the web area to the cross-sectional area

b Width of the cross-section without corners or fillets

\( b_{comp} \) Of class 4 compression parts of a cross-section without corners or fillets

\( b_0 \) Width of the tensile test specimen

\( b_{0,nom} \) Nominal width of the tensile test specimen

\( b_{0,234} \) Average of the measured width of the tensile test specimen

e\(_0\) Geometrical imperfection

\( e_{0} \) Magnitude of the initial local geometrical imperfection

\( e_{0,y} \) Initial global deflection of the centre line of a column in the direction of \( y \)

\( e_{0,z} \) Initial global deflection of the centre line of a column in the direction of \( z \)

e\(_1\) Nominal eccentricity of the normal force

\( e_{1,y} \) In the direction of \( y \)

\( e_{1,z} \) In the direction of \( z \)

f Stress value

\( f_p \) Proportional limit, end of the initial linear-elastic branch, \( f_p = E_0 \cdot \varepsilon_p \)

\( f_{p,x} \) x % proof stress, i.e. stress at x % plastic strain

\( f_{p,0.01} \) 0.01 % proof stress, i.e. stress at 0.01 % plastic strain

\( f_{p,0.2} \) 0.2 % proof stress, i.e. stress at 0.2 % plastic strain

\( f_{p,1.0} \) 1.0 % proof stress, i.e. stress at 1.0 % plastic strain

\( f_u \) Ultimate stress

\( f_{x} \) Stress at x % total strain

\( f_{2.0} \) Stress at 2.0 % total strain

\( f_{5.0} \) Stress at 5.0 % total strain

\( f_{y,20^\circ C} \) Ambient temperature yield stress of carbon steel
NOTATION

\( f_{y,20}\text{C,SHS} \) ..... Actual of the SHS 160·160·5 test specimens
\( f_{y,20}\text{C,RHS} \) ..... Actual of the RHS 120·60·3.6 test specimens
\( f_{y,20}\text{C,HEA} \) ..... Actual of the HEA 100 test specimens
\( f_{y,20}\text{C,nom} \) ..... Nominal according the EN 1993-1-1

\( h \) Height of the cross-section without corners or fillets

\( k \) Temperature dependant reduction factor

\( k_{E,0.2} \) Defined in EN 1993-1-2 for different stainless steels
\( k_{p,0.2,\theta} \) Of the 0.2 % proof stress defined in EN 1993-1-2 for carbon steel
\( k_{y,\theta} \) Of the stress at 2 % total strain defined in EN 1993-1-2 for carbon steel

\( k_n \) Local buckling factor

\( n \) Exponent defining the curvature of the first segment in a Ramberg-Osgood formulation

\( n \) Ratio of the normal force to the plastic resistance to normal forces

\( m \) Exponent defining the curvature of the second segment in a two-stage Ramberg-Osgood formulation

\( r \) Radius of a fillet of an H-section or the corner of a box section

\( r_{a} \) Outer radius of the corner of a box section
\( r_{i} \) Inner radius of the corner of a box section
\( r_{m} \) Medium radius of the corner of a box section

\( t \) Wall thickness of a test specimen

\( t_{0} \) Thickness of the tensile test specimen
\( t_{0,\text{nom}} \) Nominal thickness of the tensile test specimen
\( t_{0,234} \) Average of the measured thickness of the tensile test specimen
\( t_{f} \) Flange thickness of an H-section
\( t_{w} \) Web thickness of an H-section

\( u_{u,0} \) Vertical deformation at the ultimate load \( F_{u,0} \)

\( v_{u,0} \) Horizontal deformation at the ultimate load \( F_{u,0} \) in the direction of the y axis

\( w_{u,0} \) Horizontal deformation at the ultimate load \( F_{u,0} \) in the direction of the z axis

\( x, y, z \) Cartesian system of coordinates with x: the direction of the normal force

**Greek Characters**

\( \alpha \) \( \alpha \)-iron, ferrite, body-centred cubic crystalline structure

\( \alpha \) Imperfection factor for flexural buckling

\( \gamma \) \( \gamma \)-iron, austenite, face-centred cubic crystalline structure
Reduction factor considering ambient temperature yield strength for local buckling

\( \varepsilon_{\text{S}\text{H}\text{S}} \) Regarding the measured material of the SHS 160·160·5 test specimens
\( \varepsilon_{\text{R}\text{H}\text{S}} \) Regarding the measured material of the RHS 120·60·3.6 test specimens
\( \varepsilon_{\text{H}\text{E}\text{A}} \) Regarding the measured material of the HEA 100 test specimens
\( \varepsilon_{\text{nom}} \) Nominal according to EN 1993-1-1

Strain value

\( \varepsilon_{e,0.2} \) Elastic strain at \( f_{p,0.2} \), \( \varepsilon_{e,0.2} = \frac{f_{p,0.2}}{E_0} \)
\( \varepsilon_{p} \) Total strain at the proportional limit \( f_p \), \( \varepsilon_{p} = \frac{f_p}{E_0} \)
\( \varepsilon_{p,0.2} \) Total strain at \( f_{p,0.2} \), \( \varepsilon_{p,0.2} = \varepsilon_{e,0.2} + \varepsilon_{0.2} \)
\( \varepsilon_{\text{pl,u}} \) Plastic strain at \( f_u \)
\( \varepsilon_{u} \) Total strain at \( f_u \)
\( \varepsilon_x \) Total strain of \( x \) %

\( \varepsilon_{0.2} \) Total strain of 0.2 %, \( \varepsilon_{0.2} = 0.002 \)
\( \varepsilon_{2.0} \) Total strain of 2.0 %, \( \varepsilon_{2.0} = 0.02 \)
\( \varepsilon_y \) Strain at yield stress \( f_y \)

Temperature

\( \bar{\lambda}_k \) Non-dimensional overall slenderness ratio

\( \bar{\lambda}_{k,20^\circ\text{C}} \) At ambient temperature for class 1 to 3 sections
\( \bar{\lambda}_{k,y,20^\circ\text{C}} \) Regarding major axis bending
\( \bar{\lambda}_{k,z,20^\circ\text{C}} \) Regarding minor axis bending
\( \bar{\lambda}_{k,\text{eff},20^\circ\text{C}} \) At ambient temperature for class 4 sections
\( \bar{\lambda}_{k,\text{CSA}} \) According to the CSA
\( \bar{\lambda}_{k,\text{eff},\text{CSA}} \) For class 4 sections
\( \bar{\lambda}_{k,\text{SSA}} \) According to the SSA
\( \bar{\lambda}_{k,\text{eff,SSA}} \) For class 4 sections

\( \bar{\lambda}_{p,20^\circ\text{C}} \) Non-dimensional cross-sectional slenderness ratio at ambient temperature

Geometrical cross-sectional constant

Reduction factor of the effective width method

Engineering stress

Auxiliary factor for the flexural buckling curve

Poisson's ratio

Reduction factor for the flexural buckling curve

Ratio of the end-stresses in a compression element
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REFERENCES


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