Local buckling of slender aluminium sections exposed to fire

Mechanical properties at elevated temperature

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Summary

This report gives the mechanical properties of aluminium alloys 5083-H111 and 6060-T66 when exposed to fire. To determine these properties, steady-state tensile tests, creep tensile tests, transient state tensile tests and steady-state bending tests were carried out.

The strength resulting from a steady-state test at elevated temperature depends on the strain rate applied in the test.

The creep tests were carried out to determine the parameters in an existing creep model for primary and secondary creep. Based on the creep tests, the creep model was extended for the first part of the tertiary creep stage. The model was used to simulate transient state tests with various heating rates and for constant stress as well as varying stress in time. The strain development of these tests was accurately predicted with the model. Based on this, it is concluded that the material model is suited to determine the mechanical properties of fire exposed aluminium alloys.

The values for the modulus of elasticity as a function of temperature, as given in Eurocode 9 part 1-2, agrees with the values resulting from bending tests carried out in this research. The values for the 0.2 % proof stress as a function of temperature, as given in Eurocode 9 part 1-2, are based on steady state tests. This research shows that these values are unsafe for fire exposure (transient state situation, with linear heating and constant stress in time).

Due to creep (and possibly also due to overageing and annealing at elevated temperature), the strength of fire exposed aluminium depends on the heating rate. For practical design situations, however, it suffices to provide strength data that are independent of the heating rate, at least for the studied alloys 5083-H111 and 6060-T66.
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1 Introduction

This report gives the results of tensile and bending tests carried out and the use of models to determine the material properties of aluminium alloys 5083-H111 and 6060-T66 when exposed to fire. The report focuses on the determination of relevant material properties such as the modulus of elasticity, the proportional limit, the 0.2 % proof stress and the tensile strength. Also, the influence of creep on the stress-strain relation is determined.

The report is a background document for the PhD study on local buckling of fire exposed slender aluminium sections.

The PhD study focuses on local buckling of fire exposed aluminium. The stress-strain relations will only be used to simulate local buckling. It is not intended to determine the entire stress-strain relation at every temperature.

By carrying out transient state tests and determine the stress-strain relation based on these tests (such as done for steel), many tests will have to be carried out to determine the stress-strain relation at one temperature. Stress strain relations at other temperatures are then available as well.

Chapter 2 gives an overview of the time-dependent material behaviour of aluminium alloys at elevated temperature. The information in this chapter was obtained from literature and from pilot tests. It was used to determine the appropriate tests to be carried out in order to determine the material properties of fire exposed aluminium alloys.

Chapter 3 gives an overview of the types of tests used to determine the material properties. The results of the tensile tests at constant temperature and an imposed strain rate (steady-state tests) are given in chapter 5. Chapter 6 gives the results of bending tests, which are carried out to determine the modulus of elasticity. Results of tensile tests with constant temperature and force (creep tests) are discussed in chapter 7. Chapter 8 describes the tensile tests with constant load and a linear increasing temperature (transient state). These transient state tensile tests are an approximation of the real load condition when a structure is exposed to fire.

An analytical creep model was selected to model the material properties at elevated temperature. The parameters of this Dorn-Harmathy creep model are determined with the results of the creep tests. The model was used to simulate the transient state tests. Chapter 9 gives the description of the model, the parameters for both alloys and the results of the simulations.

Conclusions are given in chapter 10.
2 Time dependency of the mechanical properties – explanation of physical phenomena

The mechanical properties of aluminium exposed to elevated temperature depend on the temperature history. The phenomena responsible for this time dependency can be divided in stress related phenomena and overageing and annealing, which are not related to the stress level.

2.1 Overageing and annealing

Tests show that the favourable aluminium matrix obtained through cold working or thermal treatment is gradually destroyed at elevated temperature. If an alloy is treated for a too long time at elevated temperature, the particles grow, resulting in a lower strength (overageing). At even higher temperatures, a treated alloy approaches the strength of an alloy in annealed temper (annealing).

When analysing test data in Kaufman [11] and Voorhees and Freeman [32], it appears that the strength of heat-treated alloys indeed depends on the thermal exposure period in case of heat-treated alloys. The cause of this is that the formation and destroy of precipitate particles requires time (see literature report [17]). Although recrystallisation may also require time, test results on aluminium specimens in Kaufman [11] show no influence of the thermal exposure period on cold-worked alloys.

2.2 Visco-elastic and visco-plastic behaviour

From a microscopic point of view, when stress is applied on a metal, movement of dislocations as well as flux (or diffusion) of vacancies and the counterflow of atoms, either in the lattice or along grain boundaries and external surfaces, may advance in time. This process extends the crystal length in tensile direction and reduces the width in direction of the compressive stress. As a result, the stressed specimen deforms in time. Both the dislocation and the diffusion process increase in activity at increasing temperature. Thus, the strain exhibited in a specimen depends on the stress level, the temperature and the time.

If a test is carried out in which a stress is applied during a certain period, and afterwards the stress is removed, the elastic strain recovers instantaneously, a part of the strain recovers in time, and a part remains (Figure 2-1). The part of the strain recovering in time is called visco-elastic strain (or sometimes anelastic strain), while the strain that remains is called visco-plastic strain (or sometimes plastic creep strain). The recovering strain in time is usually only a small portion of the total time dependent strain in case of metals (Findley et al [5]).
Visco-elastic and visco-plastic behaviour exhibits in different ways, depending on the type of test carried out.

- Creep, which results in on-going elongation of material when a constant temperature and load level are applied. The creep process is normally divided in three stages, i.e. a primary stage, with a decreasing strain rate in time, a secondary stage, with constant strain rate (also called steady-state strain rate) and a tertiary stage, with increasing strain rate (Figure 2-2). At the end of the tertiary creep stage, creep rupture occurs. Creep thus involves time dependent deformation and fracture of materials. The creep strain is the summation of visco-plastic and visco-elastic strain.

- Dependency on the strain rate, which exhibits in tensile tests. In these tests the strain rate is normally kept constant (between certain load levels). As an example, Figure 2-3 gives the results of tensile tests carried out at two different rates by Van den Boogaard [1]. It is shown that the strain rate applied here has a significant influence on the ultimate tensile strength and on the strain at rupture at elevated
temperature (a lower strain rate results in a lower strength and a higher strain at rupture). On the contrary, at room temperature, a lower strain rate results in a higher strength. However at room temperature the curves are closer together.

![Graph showing strain rate dependency at room temperature and at 250 °C](image-url)

Figure 2-3 – Strain rate dependency at room temperature and at 250 °C

Note that visco-elastic and visco-plastic behaviour results in time-dependent deformations.

Visco-elastic and visco-plastic behaviour depends on the temperature, the time at elevated temperature and on the stress level. Overageing and annealing depend on the temperature and the time at elevated temperature, but not on the stress level.

2.3 **Description of Dorn-Harmathy creep model**

An analytical model is available with which the primary and secondary creep strain can be determined. In this model, the strain rate development depends on the temperature, time and stress level. With the Dorn-Harmathy creep model it is possible to account for a temperature variation in time. Tertiary creep is not incorporated in the model.

Evaluation of the application of the Dorn-Harmathy creep model for various steel grades is given in Thor [31]. The activation energy and other material dependent parameters are given for various metals, including aluminium alloys, in many articles, e.g. in Dorn [3], Li and Langdon [13] and , Park, Lavernia and Mohamed [22], Schoft and Schoft et al. [28], [29] and Oertel et al. [21].

2.3.1 **Dorn’s description of the secondary creep strain rate**

Based on experiments, Dorn [3] determined that the relation between the temperature and the secondary creep strain rate can be described by the well-known Arrhenius equation:

\[
\dot{\varepsilon}_i \sim e^{\frac{-Q}{RT}}
\] (2.1)
In which $R$ is the universal gas constant ($=8.31447 \ [\text{J/mol K}]$), $T$ is the absolute temperature [K] and $Q$ is the activation energy for creep [J/mol], which depends on the metal considered. The equation applies for diffusional creep processes in metals. As diffusional creep is dominant at temperatures above 0.5 times the absolute melting temperature (Dorn [3]), the Arrhenius equation applies above this temperature For aluminium alloys, this temperature is approximately equal to 150 °C, i.e. the equation applies in the temperature domain that is relevant for fire exposed aluminium.

The creep strain rate is further a function of the stress level, which completes equation (2.1) as follows:

$$
\dot{\varepsilon} = f (\sigma) \cdot e^{\frac{-Q}{RT}}
$$

(2.2)

In the secondary creep strain range, the function for the stress dependency, $f(\sigma)$, is equal to the Zener-Holloman parameter [35], denoted here with $Z$ ($Z = f(\sigma)$). Thus the secondary creep strain $\varepsilon_{t,\text{II}}$ is equal to:

$$
\dot{\varepsilon}_{t,\text{II}} = Z \cdot e^{\frac{-Q}{RT}}
$$

(2.3)

$$
\varepsilon_{t,\text{II}} = \int Z \cdot e^{\frac{-Q}{RT}} \, dt
$$

(2.4)

The equations can also be applied in case of an increasing temperature.

Various suggestions are made for the function for the Zener-Holloman parameter. The Norton-Bailey creep law describes the stress-dependency of the secondary strain rate:

$$
Z = A(\sigma)^n
$$

(2.5)

In which $n$ is a material parameter. For many materials, the Norton-Bailey creep law gives too small strains for high stress levels. Dorn proposed equations (2.6) and (2.7) for $Z$. This was later replaced by a single relation (McQueen and Jonas [24]), equation 2.8):

For small stress levels: $Z = A \cdot \sigma^n$

(2.6)

For high stress levels: $Z = A' \cdot e^{B \sigma}$

(2.7)

$$
Z = A' \left( \sinh \alpha \sigma \right)^n
$$

(2.8)

In which parameters $B$, $n$, $A$, $A'$, respectively $A''$, $\alpha$ and $n$ are material parameters independent of temperature and stress level.

For other materials and/or other stress levels, equation (2.9) is proposed.

$$
\dot{\varepsilon}_{\text{cr}} \sim \left( \sigma - \sigma_{\text{th}} \right)^n \cdot f (T)
$$

(2.9)
In which $\sigma_{th}$ is a temperature-dependent threshold stress below which no significant creep strains develop.

### 2.3.2 Addition of primary creep by Harmathy

Harmathy extended Dorn’s equation with primary creep. The following equation was suggested for primary and secondary creep strain in time:

$$
\dot{\varepsilon}_t = Z \cdot e^{\frac{-Q}{RT}} \cdot \coth^2 \left( \frac{\varepsilon_t}{\varepsilon_{t0}} \right) 
$$

Although the equation is derived for a constant stress in time ($d\sigma/dt = 0$), Harmathy suggested that equation (2.10) can also be used in case of a varying stress in time, at least when $\sigma$ varies slowly in time.

Harmathy pointed out that the Dorn equation implicitly assumes that $\varepsilon_{t0}$ is a function of the stress level ($\sigma$) only. In studies to creep of steel, Harmathy pointed out that a power law relation is appropriate for the description of the relation between $\sigma$ and $\varepsilon_{t0}$:

$$
\varepsilon_{t0} = D \cdot \sigma^m
$$

In which $D$ and $m$ are material-dependent parameters. Results of measurements on $\varepsilon_{t0}$ of steel are given by Thor [31].
Harmathy [7] and Thor [31] applied this equation to determine the deflection of a fire exposed steel beam. In the current research, it is studied whether the stress-strain relations of fire exposed aluminium, including visco-elastic and visco-plastic behaviour, can be determined based on the Dorn-Harmathy creep model. This is elaborated in chapter 9.
3 Description of different types of tests

From the explanation of material behaviour in chapter 2, it is concluded that the following parameters influence the material behaviour and thus the results of the tensile tests:
- Time [min]
- Temperature [°C]
- Heating rate [°C / min]
- Load level [N/mm²]
- Strain rate [1/min]

It depends on the type of tests which of these parameters are varied, which are kept constant and which are the output of the tests. The following types of tests are distinguished:
- Steady state tensile tests;
- Transient state tensile tests;
- Creep tensile tests;

3.1 Steady-state tensile test

In tensile tests, the temperature remains constant and the specimen is pulled with a certain strain rate until rupture occurs. Based on paragraph 2.1, it is expected that a higher strain rate results in a higher ultimate tensile strength and a lower strain at rupture. The procedure is given in Figure 3-1.

![Figure 3-1 – Input and output of tensile tests](image)

In a test procedure according to NEN-EN 10005, the strain rate should have a higher value after reaching the 0,2 % proof stress than before. It is assumed that this procedure is prescribed for test convenience; the test period is reduced significant when applying a higher strain rate after reaching the 0,2 % proof stress.

However, a test carried out on aluminium applying the prescribed strain rates shows a ‘jump’ in the strength because of the strain rate dependency (Figure 3-2).
Therefore, when testing aluminium at elevated temperature, it seems more appropriate to apply one constant strain rate throughout the tests.

- **Input parameters:** temperature [°C], strain rate [1/min]
- **Output parameters:** load at a certain strain (or time) [N/mm²], strain at rupture [-]
- **Advantage of the test:**
  The desired stress-strain curve is determined directly
- **Disadvantages of the test:**
  The first tests revealed that at elevated temperature, the stress-strain relation depends on the strain rate. In a real fire situation, with constant load instead of a constant strain rate, creep influences the stress-strain relation. Although creep and strain rate dependency are actually the same phenomena from a material point of view, it is not possible to experimentally determine the relation between the influence of creep and the influence of the strain rate on the strength. A result is, that it is unknown what strain rate should be applied in order to approach the behaviour when exposed to fire.

### 3.2 Creep test

In creep tests, a constant, elevated temperature and a constant load is applied. In time, the strain will increase until rupture occurs. The time at creep rupture and the strain at different times are monitored. Tests can be carried out at various temperatures and load levels. The procedure is given in Figure 3-3.
Figure 3-3 – Input and output of creep tests

- Input parameters: temperature [°C], load level [N/mm²]
- Output parameters: strain and strain rate as a function of time, strain at rupture [-]
- Advantage of the test:
  o Creep deformations and creep rupture stress can be determined without influence of thermal expansion on the results, because the temperature remains constant;
  o Results of creep tests on aluminium alloys are given in Kaufman. If these tests are representative, this possibly reduces the amount of tests to be carried out.
- Disadvantages of the test:
  Creep tests cannot be used directly in fire design, as a constant temperature does not agree with fire exposure. Instead, in case of a transient state test, the temperature increases form room temperature to the critical temperature.

3.3 Transient state test

In transient state tests, the load remains constant and the temperature increases from room temperature to collapse, as shown in Figure 3-4. By carrying out tests with a constant heating rate and various load levels, and measuring the strains at certain temperatures, stress-strain curves can be determined at these temperatures. The influence of creep is then implicitly incorporated in these stress-strain curves. The stress-strain curves are valid for the heating rate that was applied in the tests. This procedure has been followed for steel (see Annex A).

Figure 3-4 – Input and output of transient state tests

- Input parameters: heating rate [°C/min], load level [N/mm²]
- Output parameters: collapse temperature [°C], strain at a certain temperature (or time) [-]
- Advantage of the test:
- This test gives the best approximation of the real structural behaviour in a fire (although, in a real fire the load will in most cases not remain constant due to thermal expansion, creep and weakening of heavily exposed parts, resulting in redistribution of forces and moments).
- Creep deformations are in this case incorporated in the stress-strain diagram. This provides a simple way to model the material behaviour in a finite element model or in an analytical model.

- Disadvantages of the test:
  - Because of the changing temperature during the test, the specimen and the measuring equipment are subjected to thermal expansion. It is therefore difficult to detect small mechanical strains. As a result, it is expected that the stress-strain curves resulting from these tests are not reliable for small strains. For the local buckling tests, however, we are especially interested in the stress-strain curves for small strains (the finite element simulations show that especially the modulus of elasticity and the proportional limit are important parameters for the ultimate buckling resistance).
  - It is expected that a relatively large amount of tests has to be carried out in order to obtain the stress-strain relations (with sufficient accuracy).

Creep tests may give the essential information for creep models, with which it may be possible to simulate transient state tests. This is illustrated in chapter 9.
4 Test set-up

The creep tests and the transient state tests were carried out in a Gleeble 3800 test machine. The test machine was not suited to carry out steady-state tensile tests. Therefore, a separate test set-up was constructed for these tests. This test set-up is discussed in paragraph 4.1. Bending tests were carried out to determine the modulus of elasticity. The test set-up developed for these tests is elaborated in paragraph 4.2. The Gleeble 3800 is discussed in paragraph 4.3.

4.1 Test set-up for tensile tests

Paragraph 4.1.1 gives an overview of the set-up. Important parts of the set-up are discussed in paragraphs 4.1.2 and 4.1.3. Measurements of strains and deformations are elaborated in paragraphs 4.1.4.

4.1.1 Overview of the set-up

A schematic overview and a picture of the set-up are given in Figure 4-1. The specimen is supported by special clamps, which are connected by bars. The lower bar is connected to a stiff frame. The upper bar is connected to the actuator, which is also connected to the stiff frame. All connections are hinged, so that the specimen and the load cell are loaded in pure tension, without bending. The specimen and clamps are heated in an electrical furnace.

Figure 4-1 – Overview of the set-up
4.1.2 Heating

An electrical furnace was applied to heat the specimen. Heating takes mainly place by radiation of electric elements in the furnace walls. No active measures to generate air circulation were installed. A picture of the furnace is given in Figure 4-2.

The temperature in the furnace was measured with a thermocouple in the centre of the furnace. The temperature of the specimen was measured by several thermocouples along the specimen length that were spot-welded on the specimen.
The specimens heated slower than the air in the furnace. As the gas temperature was controlled, this control had to be determined by trial and error to obtain the desired heating rate and test temperature of the specimen. Figure 4-3 gives an example of the temperature of the furnace and the temperature along the specimen (parallel specimen length = 80 mm) in the steady state tests.

![Figure 4-3 – Example of temperature of the furnace and temperature division along specimen length in steady state tests](image)

4.1.3 Loading

The specimen was loaded with an actuator placed outside the furnace. A steel bar with length of approximately 1.5 m transferred the load from the actuator to the clamps inside the furnace (Figure 4-4). These clamps consisted of aluminium blocks with a slot for the specimen. The load transfer from clamps to specimen occurred with bolts or stainless steel bars. For this purpose, holes were drilled in the clamping edges of the specimen, which were centred in the middle of the specimen width with a tolerance of 0.01 mm in order to prevent bending in the specimen (Figure 4-5).
The load was measured with two load cells close to the actuator. A load cell with a range up to 100 kN was applied as control system, while a load cell with a range up to 10 kN was used to accurately measure the load. The distance between the load cells and...
the furnace was such that the temperature of the load cells remained at room temperature during the tests.

4.1.4 Deformations and strains

LVDT’s
Deformations of the specimens along the parallel length were measured with LVDT’s placed outside the oven. A clamp around the parallel length was used to guide the LVDT’s (middle picture of Figure 4-5) Bars of invar steel were applied between the clamp inside the oven and the LVDT’s inside the oven (Figure 4-6).

Two LVDT’s with a range of $\pm 1.5$ mm were applied to measure the level of the lower part of the parallel length at both sides of the specimen (numbered 1 and 2 in the middle picture of Figure 4-5). The upper part of the parallel length was measured with four LVDT’s (numbered 3 – 6 in Figure 4-5). Of these four LVDT’s, two had a range of $\pm 1.5$ mm in order to accurately measure small deformations, and two had a range of $\pm 10$ mm in order to measure larger deformations. The difference between the average displacement of no. 1 and 2 and the average displacement of no. 3 and 4, respectively 5 and 6 is the deformation of the specimen.

![Figure 4-6 – Deformation measuring with LVDT’s](image)

The part of the invar steel bars inside the furnace expands when exposed to a temperature increase. Although the length of the part of the bars inside the furnace was approximately equal for all bars, small differences in temperature and length of the bars causes an inaccurate measurement of the deformation of the parallel length in case of increasing temperatures. Therefore, it is expected that the LVDT measurement only gives a rough indication of the deformation in case of transient state tests.
In steady state tensile tests, the temperature is kept constant during the test and the length of the bars remains constant as well (Figure 4-7). In such tests, the LVDT measurement is expected to give accurate results.

Because of thermal expansion of the clamp, thermal expansion of the specimen, lateral contraction of the specimen during testing and relaxation of the springs of the clamp at elevated temperature, the clamp came loose of the specimen in a number of tests. In these cases, the deformation measurement with LVDT’s failed.

**Actuator**

In the tests where the LVDT clamp came loose and in case of deformations larger than the ranges of the LVDT’s, the deformation of the specimen was obtained by the measurement of the actuator. This measurement contained not only the deformation of the parallel length of the specimen, but also bearing near the bolt holes of the specimen and elastic deformation of the test rig. The measurement was corrected for these influences by extracting an off-set from the deformations and setting of the measuring length. Off-set and measuring length were determined by comparing the results of the actuator measurement with the strain measured after rupture and results of the LVDT measurements in cases where the LVDT clamp was still fixed. The measuring length determined in this way was 5 mm longer than the parallel length, while the offset depended on the temperature and the cross-section area of the specimen.

Note that the measurement of (large) deformations is not very accurate. However, for the research to local buckling, especially the initial part of the stress-strain curve is important. The research on material properties therefore focussed on small deformations.

**Strain gages**

In order to accurately measure small strains in order to determine the modulus of elasticity, the proportional limit, the 0,2 % proof stress, strain gages were applied in the direction of the load. Strain gages in the transverse direction were applied to measure the Poisson ratio.

At room temperature, strain gages were applied at both sides of the specimen, in order to correct for possible bending of the specimen in case of non-straight specimens. Each strain gage was measured separately, applying a so-called quarter bridge of Wheatstone
with three fixed resistances (Figure 4-8). The excitation applied in the tests was 10 Volt, the output depends on the resistance of the strain gauge, which is related to the elongation of the strain gage.

![Quarter bridge of Wheatstone](image)

Figure 4-8 – Quarter bridge of Wheatstone

The measurement with strain gages in tensile tests on aluminium specimens was validated by Mennink [20].

At elevated temperature, strain gages with temperature correction for steel were applied. Even for temperature corrected strain gages, an output signal is measured if the temperature changes. This signal is called “apparent strain” and is independent of the mechanical load on the specimen.

Two types of strain gage configurations were applied:

- In some tests, a quarter bridge of Wheatstone was applied to determine the mechanical strain (Figure 4-8). During heating, the measured strain should be equal to the thermal expansion of the aluminium strain gage plus the apparent strain minus the thermal expansion of steel at that temperature (since the strain gage was corrected for thermal expansion of steel). It was verified that the output corrected for thermal expansion of steel and apparent strain indeed corresponded with the coefficient of linear thermal expansion of aluminium (Figure 4-9);

- In other tests, a halve bridge of Wheatstone was applied to determine the mechanical strain. In this case, the fixed resistance R2 in Figure 4-8 is replaced by a strain gage. This strain gage is adhesive bonded to a dummy specimen, which had the same temperature as the real specimen but was not loaded. For this purpose, a dummy specimen was made with a slot hole (Figure 4-10). The real specimen and the dummy were placed side-to-side in the test frame, so that the temperature of the two strain gages was equal and the thermal expansion of the real specimen and the dummy was also equal. In this way, the output of the bridge should consist of only mechanical strain, it is corrected for the apparent strain and for thermal expansion. This procedure can be applied to obtain the mechanical strain in both steady-state and transient state test.

The excitation and output leads/cords were equally long, with an equal length inside the furnace, so that a difference in the resistance of these threads does not influence the test results.
Figure 4-9 – Theoretical thermal expansion compared with corrected output of the strain gages during heating of a steady state test (quarter bridges)

Figure 4-10 – Half bridge of Wheatstone with a real specimen and with the dummy

As an example, Figure 4-11 gives the output of the longitudinal and transverse strain gages of the procedure with a quarter bridge of Wheatstone applied in a steady-state test. Figure 4-12 gives the output in case of a half bridge of Wheatstone. Both test results are obtained for unloaded specimens.

The upper graphs in Figure 4-11 and Figure 4-12 give the temperature of the furnace and of the real specimen and the dummy. The thermocouples on the dummy and on the specimen (middle) are situated near the strain gages. It is shown that the dummy and the specimen have practically the same temperature at this spot.

The lower graphs give the output of both pairs of strain gages during heating.

After heating and before applying the mechanical load, the strain gages should not change in time, because otherwise the mechanical strain cannot be determined. In all tests, the output of the strain gage indeed remained approximately at the same level between heating and testing. The maximum variation of the strain in time detected in one of the tests during this period was 0,00001/min. This error is so small that it will not affect the results of the tensile tests.

In case of the measurement with a half bridge, the strain should remain at a constant value during heating. Up to 20 minutes, the strain indeed remains below 0.01 %, which
is regarded as negligible. Then, an unexpected jump in the strain occurs. After this jump, the strain remains constant again. This strain jump occurred at the moment the slope of the heating curve of the specimen changed. According to the manufacturer of the strain gages, this is due to lagging of strain gages at elevated temperature. The same phenomenon occurred in the test with the quarter bridge (Figure 4-9 and Figure 4-11).

Because the strains remain constant after the strain jump and before the load was applied, it is expected that the measurement with strain gages in the steady state tests only gives the mechanical strain, as was desired. For transient state tests it should first be determined whether these strain jumps occur when exposed to the temperature applied in these tests. Only in case the strain remains at zero during heating of an unloaded specimen, the strain gage measurement is sufficiently accurate for transient state tests.

Figure 4-11 – Temperature and output of a quarter bridge during heating (before loading) of a steady state test
4.2 **Test set-up for the steady-state bending tests**

In order to determine the modulus of elasticity, bending tests were carried out. The tests were carried out in the same furnace as used for the steady-state tensile tests.

4.2.1 **Overview of the set-up**

Strips supported with one hinge and one roll were loaded with a concentrated load at the middle of the span. Figure 4-13 gives an overview of the set-up and a detail of the roll support is given in Figure 4-14.

The displacements were measured with LVDTs at the supports and in the middle of the span. The LVDTs were situated outside the furnace. Bars of invar steel were applied...
between the clamp inside the oven and the LVDTs outside the oven, in the same way as described for the tensile tests. Nuts are adhesive bonded on the specimen as guidance for the invar steel bars.

Figure 4-13 – Overview of the set-up for bending tests

Figure 4-14 – Roll support
4.2.2 Loads

A steel bar was attached to the specimen at midspan. This bar was guided through a hole in the furnace floor. Outside the furnace, weights were suspended on the steel bar. During heating, mechanical loads were not applied. When the test temperature was reached, the weights were stepwise applied and for each load increase, the deformations were measured (Figure 4-15). The maximum amount of weights applied depended on the strength of the alloy at the test temperature. In most tests, the load was increased stepwise and subsequently decreased stepwise (Figure 4-16).

![Strip in loaded situation](image1)

Figure 4-15 – Strip in loaded situation

![Graph of applied load](image2)

Figure 4-16 – Example of the applied load on the strip in bending as a function of time

4.2.3 Displacements

The displacements at the supports and at midspan were measured with LVDTs with an amplitude of ±1.5 mm.

4.2.4 Temperature

The temperature of the specimen was measured with six thermocouples along the span. The temperature at the supports was approximately 5°C lower than the temperature at midspan. This temperature difference was neglected and in the evaluation of the tests, the average of the measured temperatures was taken.
4.3 Test set-up for creep and transient state tests

The creep tests and transient state tests were carried out in a standard Gleeble 3800 test bench.

4.3.1 Overview of test set-up

The Gleeble consists of a furnace with a specimen inside. The specimen is heated by conduction. The furnace is under vacuum in order to prevent the clamps to oxidize. A hydraulic actuator applies the load (Figure 4-17, Figure 4-18 and Figure 4-19).

Figure 4-17 – Overview of test set-up for creep tests

Figure 4-18 – Furnace for creep tests (left-hand: with closed door, right-hand: with door opened)
4.3.2 Heating

The specimen is heated by induction through the clamps and cooled by a water flow. The thermocouple attached in the centre of each specimen is used to control the induction current. This heating system has the following advantages:

- The actual temperature follows the specified temperature with an error of less than 1 °C;
- It is possible to apply a temperature increment in a relatively short time (in the tests, the specimen was heated from room to test temperature in 40 seconds and a temperature increment of 20 °C was applied in 5 seconds). After a temperature increment, it is possible to maintain the temperature constant without overshoot (Figure 4-20);
- The surroundings of the specimen remain relatively cool: heating occurs only by radiation from the specimen. This means that measuring equipment remains relatively cold and thermal expansion of the measurement equipment is less of a problem.

Some drawbacks of the heating system are:

- A temperature gradient is present along the specimen length: the temperature decreases from the middle of the specimen towards the clamps. Between the clamps and the specimen, a graphite layer of 0.2 mm was applied as an insulation layer in order to increase the current, so that the temperature is more uniform. Still, a difference in temperature between the middle and 10 mm out of the middle of the specimen was approximately 3 °C (Figure 4-21 and Figure 4-22).
- Because of the large temperature difference between the specimen and the surroundings, the radiative heat loss from the specimen is important. This results in a
temperature difference between the core and the surface of the specimen. This temperature difference has been measured in other tests on specimens with a circular cross-section and a diameter of 10 mm. It resulted in a temperature difference of approximately 2 ºC.

It appeared that these small temperature gradients do not influence the test results in a significant way, provided the gage length of the LVDT is not too large (20 mm was chosen in most tests, i.e. 10 mm out of the middle of the specimen at both sides).

![Figure 4-20 – Measured temperature in one of the tests (difference specified temperature < 1 ºC)](image)

![Figure 4-21 – Temperature along the specimen length determined with an infrared camera in a test heated to 300 ºC](image)
4.3.3 Loading

The load was applied with a 200 kN actuator. The actuator has, according to the manufacturer, an accuracy of 99.9%, meaning that the possible measuring error is approximately 0.2 kN. This error is actually large for the tests carried out: the lowest load to be applied in the tests was 1.5 kN. This may have consequences for the accuracy of the test results.

The specimens applied have a relatively thick wall thickness (5 mm) in order to obtain a relatively large area of the cross-section, and consequently a relatively large load. The wall thickness was not taken larger because it was expected that this would introduce a larger temperature gradient.

4.3.4 Deformations

Displacements were measured with a hot-zone LVDT. The clamps of this LVDT are of ceramic material with a low thermal conductivity, so that the LVDT does not heat through conductivity. An aluminium shield protects the LVDT against radiation from the specimen. As a result, the LVDT remained at a temperature lower than approximately 50 °C in all tests.

The measured gage length of the LVDT (i.e. distance between clamps) was 20 mm (10 mm out of the middle of the specimen at both sides).
The LVDT was checked with a micrometer. A displacement of 2,00 mm on the micrometer resulted in an LVDT measurement of 2,01 mm.

The accuracy of the LVDT was also checked by heating an unloaded specimen from room temperature to 350 ºC in 30 minutes. The strain resulting from the LVDT measurement corresponded with the theoretical thermal expansion of the tested aluminium alloys in series 5xxx and 6xxx (Kammer [10], Davis [2]).
5 Results of the steady state tensile tests

Steady state tests were carried out as described in paragraph 3.1. The following procedure was applied in these tests:
- Heating of the specimen to a certain temperature in approximately 30 minutes. No load is applied;
- Maintaining the specimen temperature at this temperature (= test temperature) for approximately 15 minutes. No load is applied;
- Pulling the specimen with a certain strain rate until rupture occurs.

The most important results are given in this chapter. The test results for validation of the compression tests are given in detail in Annex C.

5.1 Test temperatures

The relation between the temperature and the 0.2 % proof stress ($f_{0.2,\theta}$) and ultimate tensile strength ($f_{u,\theta}$) of the two alloys considered, 5083-O (similar to 5083-H111) and 6063-T6 (similar to 6060-T66), are given in Figure 5-1 and Figure 5-2, respectively. These curves are based on data of tensile tests by Kaufman [11] and Voorhees and Freeman [32], respectively denoted with (K) and (VF) in the legends in the figures.

The mechanical load during fire is, in the design of most aluminium structures, reduced to approximately 40% of the load in normal design. Many structures will therefore collapse around the temperature at which $f_{0.2,\theta}$ is reduced to 40 % of the $f_{0.2}$ at room temperature. The temperature at which this is the case, appears to be approximately 300 ºC and 260 ºC for alloys 5083-H111 and 6060-T66, respectively (indicated with blue lines in the figures). These temperatures are applied in the tests.

At temperatures between 180 ºC and 350 ºC, the curves are so steep that a small increase in temperature leads to a significant decrease of load bearing capacity. This means that a decrease in load level has hardly any effect on the critical temperature (or fire resistance) in this temperature range. Besides, the fact that the curves are steep complicates the steady state tests, because it means that a small variation (or error in the measurement) of the temperature may result in a large difference in the strength.

Only a limited amount of tests is therefore carried out in the steepest parts of the curves. The test temperatures selected are indicated with straight vertical lines in the figures. They include: tests at 20 ºC, 170-200 ºC, 300 ºC and 365 ºC for alloy 5083-H111 and 20 ºC, 180-200 ºC, 260 ºC and 300 ºC for alloy 6060-T66.

It is assumed that, if models to be developed are validated for these temperatures, the model may also give appropriate results for temperatures in between, i.e. for the relevant range of 200 up to 300 ºC.
5.2 Stress-strain curves alloy 5083-H111

A number of test series was carried out to determine the stress-strain relation of alloy 5083-H111. These are discussed per test series in the following sub-paragraphs. A comparison between the test results and the results from the literature study is given in paragraph 5.2.3.

5.2.1 Tests with thickness of 5 mm

Steady-state tensile tests were carried out on specimens with a thickness of 5 mm, a width of 50 mm and a parallel length of 80 mm. The aim of these tests was to measure the ultimate tensile strength and the strain at rupture. Strain gages were not applied in
these tests. The modulus of elasticity was not determined and consequently the 0.2 % proof stress could only be determined roughly. The tensile test specimens all origin from the same rolled plate.

The strain rate applied was approximately 0.006 / min up to the 0.2 % proof stress and subsequently increased to approximately 0.03 /min up to rupture. Results are given in Figure 5-3.

The stress-strain curves are given in Figure 5-3. In all tests at 300 °C and in one test at 200 °C (test 1), the clamp for the LVDT’s came loose, so that these curves do not cover the last part of the tensile tests.

![Figure 5-3 – Stress-strain curves of alloy 5083-H111 with specimen thickness of 5 mm](image)

Different tests at the same temperature give almost equal stress-strain curves, indicating that the tests are well reproducible.

The curves at room temperature show a vibrating strength at increasing strain. This was also found in other tests on this alloy described in literature. The effect is called serrated yielding or Portevin-Le Chatelier effect and it is attributed to dislocations that slip and subsequently catch on other dislocations. The effect occurs at certain alloys (in particular alloys in series 5xxx) at certain strain rates. At the other test temperatures, serrated yielding was not observed.

Especially at 300 °C, the strain rate has a significant influence on the strength, which is shown in Figure 3-2. The change in strain rate results in a significant increase in the strength for an almost equal strain. In other tests at elevated temperature, only one strain rate was applied throughout the entire test.

At room temperature, the specimen was unloaded near the 0.2 % proof stress and subsequently reloaded in order to determine the modulus of elasticity. This unloading-reloading cycle was also applied at elevated temperature. However, because of relaxation during this unloading-reloading cycle, the slope of the curve was not constant and the modulus of elasticity could not be obtained in this way (Figure
5-4). In other tests at elevated temperature, the unloading-reloading cycle was not applied, instead the modulus of elasticity was measured from the beginning of loading.

![Stress-strain curve at 300 °C of alloy 5083-H111 with specimen thickness of 5 mm](image)

**Figure 5-4 - Stress-strain curve at 300 °C of alloy 5083-H111 with specimen thickness of 5 mm**

### 5.2.2 Tests for validation of compression tests

Steady state tensile tests were carried out on the material of which the specimens of the compression tests are composed. The tensile tests are carried out for validation of the numerical model to simulate the compression tests. The specimens had a thickness of 1 mm, a width of 25 mm and a parallel length of 80 mm. Two strain rates were applied in different tests, approximately equal to 0.01 /min and 0.002 /min. The strain rate was kept constant during the test.

In order to accurately determine the initial part of the stress-strain curve, strain gauges were applied.

The stress-strain curves are given in Figure 5-5 and the initial part of the stress-strain curves are given in Figure 5-6. There is good agreement between the measurements with LVDT’s and with strain gages for small strains. Also in this case, the test at room temperature shows serrated yielding.

The influence of the strain rate on the tensile strength at 170 °C is clearly shown: higher strain rates result in a higher strength.
Figure 5-5 – Stress-strain curves of alloy 5083-H111 with specimen thickness of 1 mm

Figure 5-6 – Initial part of stress-strain curves of alloy 5083-H111 with specimen thickness of 1 mm
One of the tests at 175 °C has an almost equal modulus of elasticity as the test at room temperature, while the other test at 175 °C is significantly stiffer. It is expected that the clamp for the LVDT’s in this test not only clamped the real specimen, but also the dummy. As a result, also the dummy was loaded. The test is omitted in the discussion of the modulus of elasticity and the Poisson ratio.

The critical load of members in compression depends on the (non-linear) material properties. Stowell [30] showed that the critical load for local buckling of aluminium plates and sections depends on the secant modulus of elasticity \( (E_s = \frac{\sigma}{\varepsilon}) \) and the tangential modulus of elasticity \( (E_t = \frac{d\sigma}{d\varepsilon}) \), see literature study [17]. Hence, it is of interest to determine whether the non-linear material properties, indicated by \( E_s \) and \( E_t \), are different at elevated temperature compared to room temperature.

The values of \( E_s / E \) and \( E_t / E \) resulting from the tests are given as a function of \( \sigma / f_{0.2} \) in Figure 5-8 and Figure 5-9, respectively. The initial modulus of elasticity was not determined accurately in the tensile tests (paragraph 5.2.3). Therefore, the results are given for values of \( E \) obtained in the bending tests (chapter 6). The graphs show that at room temperature, the secant and tangential modulus of elasticity are almost equal to the initial modulus of elasticity up to a ratio between \( \sigma \) and \( f_{0.2} \) of 0.75, indicating that the proportional limit is relatively high (75 % of \( f_{0.2} \)). At elevated temperature, however, the ratios \( E_s / E \) and \( E_t / E \) reduce at much lower load levels, indicating that stress-strain curve becomes more non-linear at elevated temperature.

It should however be noted that the reliability of the graphs is questionable, as the stiffness at elevated temperature is not determined accurately in the tensile tests.

EN 1999-1-1 uses a classification of the material to indicate whether the material behaviour is more or less non-linear. Alloys having an inelastic stress-strain relation are classified as class ‘A’ while alloys with a more elastic-plastic stress-strain relation are classified as class ‘B’. The mechanical response models for buckling in the standard
depend on this classification\(^1\). Graphs such as shown in Figure 5-8 and Figure 5-9 could be used to classify the alloys at elevated temperature.

![Graph 5-8](image1.png)

**Figure 5-8** – Secant modulus of elasticity of alloy 5083-H111 with specimen thickness of 1 mm

![Graph 5-9](image2.png)

**Figure 5-9** – Tangential modulus of elasticity of alloy 5083-H111 with specimen thickness of 1 mm

The Poisson ratio for this uniaxial test can be determined by dividing the transverse strain by the longitudinal strain, measured with the strain gages. The results are given in Figure 5-10. The value for the Poisson ratio at room temperature is 0.32, which is close to the value found in literature for aluminium (0.33).

The figure shows that the Poisson ratio at elevated temperature is higher than that at room temperature. A reasonable explanation is that the material is more viscous at elevated temperature, so that the Poisson ratio tends to the value for incompressibility \((\nu = 0.5)\)

For the same reason of incompressibility, it was expected that the value of the Poisson ratio would increase when having reached plasticity. However, the test at room temperature shows a decreasing value of the Poisson ratio when having reached plasticity (Figure 5-10). An explanation for this is not yet available.

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\(^1\) In EN 1999-1-1, all alloys in temper T6 plus alloy 5083-H34 are assumed to have a more elast-plastic behaviour and are classified as class ‘B’, while all other alloys are in class ‘A’. However, Annex G shows that this assumption is not always correct: alloy 6060-T66 is more inelastic than alloy 5083-H111 at room temperature.
Figure 5-10 – Poisson ratio of alloy 5083-H111 with specimen thickness of 1 mm

For small values of the strain, the Poisson ratio in Figure 5-10 may not be reliable, as the value of $\nu$ is determined by dividing a small transversal strain by a small longitudinal strain, and is thus sensitive to measuring errors. The values for the Poisson ratio in the elastic range are therefore determined by dividing the measured transverse strain with a factor with such a magnitude, that the resulting curve is visually equal to the measured longitudinal strain (Figure 5-11). The factor through which the transverse strain is divided is thus equal to the Poisson ratio.

Figure 5-11 – Procedure to determine $\nu$: apply such a value for $\nu$ that the transverse strain divided by the $\nu$ is equal to the longitudinal strain

Figure 5-12 gives the Poisson ratio in the elastic range as a function of the temperature for alloy 5083-H111. The figure indicates a sudden increase in the value of $\nu$ at a temperature of approximately 275 °C.
5.2.3 Overview of material characteristics

This paragraph gives a comparison of the material properties of the current tests with data found in literature. Data on alloy 5083-H111 were not found in literature. Temper H111 indicates that a minimum amount of work was done. The mechanical properties of an alloy in temper H111 are therefore expected to be comparable to that of temper O. Therefore, the material properties are compared to data on alloy 5083-O. The 0.2 % proof stress, the tensile strength and the modulus of elasticity determined in the tests on alloy 5083-H111 are compared with the values found in literature in Figure 5-13 up to Figure 5-16.

The values in literature origin from Kaufman [11] and Voorhees and Freeman [32]. It should be noted that Kaufman gives no individual test results but average values, and that the values given by Kaufman are partially based on the tests also described by Voorhees and Freeman so that these sources cannot be regarded as independent.

The tensile tests carried out in the current research are noted as “Tensile tests, 5 mm” and “Tensile test, 1 mm” in order to distinguish between the test series and origin of the specimen. Due to the homogeneous composition of aluminium alloys, it is not expected that the stress-strain curves are influenced by the difference in thickness applied.

Figure 5-13 gives the 0.2 % proof stress as a function of the temperature. There is a reasonable correspondence between the values for $f_{0.2}$ in the tests and in literature. At 180 °C, however, the values of $f_{0.2}$ determined in the current tests are approximately 80 % of the values in literature. 

![Figure 5-12 – Poisson ratio of alloy 5083-H111 as a function of the temperature](image-url)
Figure 5-13 – 0.2% proof stress of alloy 5083-H111

Figure 5-14 gives the ultimate tensile strength. Also the ultimate tensile strength determined in the tests corresponds with the values in literature.

Figure 5-14 – Ultimate tensile strength of alloy 5083-H111

Figure 5-15 gives the 02% proof stress and ultimate tensile strength of all tests in one graph. It is shown that the difference between the 0.2% proof stress and the ultimate tensile strength decreases at increasing temperature. In the tests carried out at temperatures of 270 °C and higher, the 0.2% proof stress coincides with the ultimate tensile strength. According to the values in literature, however, there remains a difference between $f_{0.2}$ and $f_u$ at all temperatures.
Figure 5-15 – 0.2% proof stress and ultimate tensile strength of alloy 5083-H111

Figure 5-16 – Modulus of elasticity of alloy 5083-H111

The modulus of elasticity at room temperature was determined by a straight line drawn in the stress-strain diagram through stress levels of 100 and 20 N/mm². At stress levels lower than 20 N/mm², there may be influence of the adjustment of the specimen and the set-up, such as straightening of the specimen. The stress level of 100 N/mm² corresponded approximately with the proportional limit (Figure 5-9).

At elevated temperature, the stress-strain relation appeared to be nonlinear from the beginning of loading (Figure 5-9). The alloy thus has no proportional limit at elevated temperature. At elevated temperature, the modulus of elasticity was therefore determined between an arbitrary stress level of 50 and the stress level of 20 N/mm² for tests at a temperature up to 180 ºC and between 30 and 10 N/mm² for tests at temperatures higher than 180 ºC.

The modulus of elasticity at room temperature is approximately equal to that found in literature (Figure 5-16). However, at elevated temperature the values for $E$ determined
with the current tests show a large scatter and do not correspond well with the values in literature. The value at 180 ºC is higher than that at room temperature, which is another indication that the modulus of elasticity was not determined accurately. A possible explanation for this difference is that bending of the specimen is not restrained by the clamps. Bending could be introduced by deformation of the specimens at the bolt holes. The measurements were not corrected for bending as the strain gages were only applied at one side of the specimen. It should be noted that only a very small difference in the strain may cause a significant difference in the modulus of elasticity.

The modulus of elasticity was therefore evaluated based on bending tests as described in chapter 6.

For reference, the methods applied in other researches to determine the modulus of elasticity of aluminium alloys at elevated temperature is given in Annex B.

5.3 Stress-strain curves alloy 6060-T66

5.3.1 Pilot tests

Two tensile tests were carried out at room temperature for the validation of the pilot buckling tests. These tests were not carried out in the furnace, but in an alternative set-up (Figure 5-17). The entire stress-strain curves of these tests are given in Figure 5-18, and the initial part of the stress-strain curves are given in Figure 5-19.

The specimen origin from the same extrusion section. The strain rates applied in both tests were approximately $1 \times 10^{-4}$/min up to the proof stress and subsequently 0,015/min up to rupture.

Figure 5-17 – Set-up for the pilot tensile tests
5.3.2 Tests with thickness of 5 mm

Steady-state tensile tests were carried out on specimens with a thickness of 5 mm, a width of 50 mm and a parallel length of 80 mm. The aim of these tests was to measure the ultimate tensile strength and the strain at rupture. No strain gages were applied in these tests. The modulus of elasticity was not determined and consequently the 0.2 % proof stress could only be determined roughly.

The tensile test specimens all origin from the same extrusion length.
The strain rate applied in the tests at room temperature and at 200 °C was approximately 0.006 / min up to the 0.2 % proof stress and subsequently increased to approximately 0.03 / min up to rupture. The tests carried out at 300 °C were carried out with a constant strain rate of approximately 0.03 / min. Results are given in Figure 5-20.

![Figure 5-20 – Stress-strain curves of alloy 6060-T66 with specimen thickness of 5 mm](image)

There is a small variation in strength between different tests carried out at the same temperature. Although the differences are so small that the tests can still be regarded as reproducible, it is unknown why the differences are larger than in the tests carried out on alloy 5083-H111 (Figure 5-3).

### 5.3.3 Tests for validation of compression tests

Steady state tensile tests were carried out on the material of which the specimens of the compression tests are composed. The tensile tests are carried out for validation of the numerical model to simulate the compression tests. The specimens had a thickness of 2 mm, a width of 25 mm and a parallel length of 80 mm. Two strain rates were applied in different tests, approximately equal to 0.01 /min and 0.002 /min. The strain rate was kept constant during the test.

Strain gauges were applied to determine the initial part of the stress-strain curve. Two types sections are going to be applied in the compression tests. For both types (Rectangular hollow sections and angles), tensile tests were carried out to determine the material characteristics.

**Specimens originating from rectangular hollow sections (RHS)**

The stress-strain curves are given in Figure 5-21.
It is shown that, at elevated temperature, a lower strain rate results in a lower strength and a higher strain at rupture.

The initial parts of the stress-strain curves are given in Figure 5-22. The graph gives the results of the strain gage and the LVDT measurement. It is shown that the LVDT measurement in most tests failed, probably because the clamp got loose. Figure 5-22 gives the results of the strain gage measurement only.

The test at 300 ºC was carried out with single strain gages (i.e. applying a quarter bridge). As the test was carried out at a constant temperature, the strain gage measurement needed not to be corrected for thermal expansion.

The results of the measurements are given in Figure 5-23. The figure shows that there is a good correspondence between the strain gage measurement and the LVDT measurement. This indicates that the mechanical strain is determined correctly with the strain gages. The figure also gives the results of the transverse strain gage divided with
the Poisson ratio. The Poisson ratio applied was 0.4. For this Poisson ratio, the longitudinal strain and transverse strain correspond.

![Stress-strain curve](image)

Figure 5-23 – Initial part of stress-strain curve at 300 ºC of alloy 6060-T66 of RHS (t = 2 mm)

The ratios between the secant modulus of elasticity or the tangential modulus of elasticity and the initial value of the modulus of elasticity ($E_s / E$ and $E_t / E$, respectively) are given in Figure 5-24 and Figure 5-25. Because the initial modulus of elasticity $E$ was not determined accurately in the compression tests, the results are given for values of $E$ in the bending tests (chapter 6). At room temperature, the secant and tangential modulus of elasticity remain are almost equal to the initial modulus of elasticity up to a ratio between $\sigma$ and $f_{0,2}$ of 0.4, indicating that the proportional limit is lower than in case of alloy 5083-H111 (40% of $f_{0,2}$ for 6060-T66 and 75% of $f_{0,2}$ for 5083-H111). At temperatures up to 270 ºC, the relation between the stress level and $E_s / E$ respectively $E_t / E$ seem to be approximately equal to room temperature. At a higher temperature (around 300 ºC), the stress-strain relation seems to be more bi-linear as at lower temperatures. However, it should be noted that the reliability of the graphs is questionable, as the stiffness at elevated temperature is not determined accurately in the tensile tests.

![Modulus of elasticity](image)

Figure 5-24 – Secant modulus of elasticity of alloy 6060-T66 of RHS (t = 2 mm)
Mechanical properties at elevated temperature

**Figure 5-25** – Tangential modulus of elasticity of alloy 6060-T66 of RHS (t = 2 mm)

The Poisson ratios are given in Figure 5-26. Again, the Poisson ratio in general increased at increasing temperature.

**Figure 5-26** – Poisson ratio of alloy 6060-T66 of RHS (t = 2 mm)

**Specimens originating from angles**

The stress-strain curves are given in Figure 5-27 and the initial parts of the stress-strain curves are given in Figure 5-28. As the LVDT measurement failed, only results of the strain gage measurements are shown in Figure 5-28. Only one strain rate was applied at 180 °C, because the test with a strain rate of 0.002/min was carried out wrongly.
The strain at rupture of the test 180 ºC, 0,01/min is larger than that at room temperature. This is in contradiction with the same test carried out on the specimen originating from a RHS(Figure 5-21) and the tests with a plate thickness of 5 mm (Figure 5-20). It is unknown what caused the unexpected strain at rupture found in this test.

The secant modulus of elasticity ($E_s = \sigma/\varepsilon$) and the tangential modulus of elasticity ($E_t = \varepsilon/\sigma$) of the tests are given in Figure 5-29 and Figure 5-30, respectively. The same remarks apply as given for the specimens originating from the RHS sections.
The Poisson ratios are given in Figure 5-31. Again, the Poisson ratio increased at increasing temperature. However, for the test at 300 °C, the Poisson ratio decreases after a certain strain. This is possibly a measuring error.
5.3.4  **Overview of material characteristics**

No data of tensile tests of alloy 6060-T66 at elevated temperature were found in literature. However, data on the 0.2 % proof stress, the tensile strength and the strain at rupture were found for alloy 6063 T6, which has a similar chemical composition as alloy 6060-T66. The 0.2 % proof stress and the tensile strength determined in the tests on alloy 6060-T66 are compared with values found in literature in Figure 5-32 up to Figure 5-34 on alloy 6063 T66.

Data on the modulus of elasticity were neither found for alloy 6060-T66, nor for 6063 T6. According to the data in Kaufman [11], the modulus of elasticity is similar for alloys in the same series. Therefore, the values for the modulus of elasticity found in the current tests on alloy 6060-T66 are compared to the values found in literature for alloy 6061 T6 (Kaufman [11] and Voorhees and Freeman [32]) and alloy 6082 T6 (Langhelle [12]). Results are given in Figure 5-35.

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![Figure 5-32](image1.png)

**Figure 5-32 – 0.2 % proof stress of alloy 6060-T66**

![Figure 5-33](image2.png)

**Figure 5-33 – Ultimate tensile strength of alloy 6060-T66**
The 0.2 % proof stress and ultimate tensile strength at room and at elevated temperature for alloy 6060-T66 are slightly lower than the values found in literature for alloy 6063 T6.

Figure 5-15 gives the 0.2 % proof stress and ultimate tensile strength of all tests in one graph. It is shown that the difference between the 0.2 % proof stress and the ultimate tensile strength decreases at increasing temperature. In the tests carried out at temperatures of 270 °C and higher, the 0.2 % proof stress coincides with the ultimate tensile strength. According to the values in literature, however, there remains a difference between $f_{0.2}$ and $f_u$ at all temperatures.

![Graph showing the 0.2 % proof stress and ultimate tensile strength of alloy 6060-T66 compared with alloy 6063-T6](image)

Figure 5-34 – $f_u$ and $f_{0.2}$ of alloy 6060-T66 compared with alloy 6063-T6

![Graph showing the modulus of elasticity of alloy 6060-T66](image)

Figure 5-35 – Modulus of elasticity of alloy 6060-T66

The modulus of elasticity at room temperature was determined between stress levels of 70 and 20 N/mm$^2$. At elevated temperature, this was determined between stress levels of 50 and 20 N/mm$^2$ for a test temperature up to 180 °C and between 30 and 10 N/mm$^2$. 
for a test temperature higher than 180 °C. The modulus of elasticity at room temperature is approximately equal to that found in literature (Figure 5-16). However at elevated temperature, there is a large deviation between the modulus of elasticity found in the current research and the values found in literature.

The Poisson ratio resulting from the measurements on alloy 6060 as a function of the temperature is given in Figure 5-36.

![Figure 5-36 – Poisson ratio of alloy 6060-T66](image)

5.4 Chapter conclusions

The tests carried out in the furnace appeared to be well reproducible, both at room and at elevated temperature. For both alloys (5083-H111 and 6060-T66) the strength at room and at elevated temperature corresponded with data given in literature for similar alloys. The modulus of elasticity could not be determined accurately at elevated temperature with the tensile tests carried out in the furnace.

The Poisson ratio of the alloys increased slightly at increasing temperature, from 0.33 at room temperature to 0.4 at 300 °C.
6 Results of bending tests

The modulus of elasticity was not determined accurately in the tensile tests in the previous chapter, due to the fact that the elastic deformations in tensile tests are small. Bending tests, in which elastic deformations are larger, were carried out to determine the modulus of elasticity. An aluminium strip was subjected to a three point bending test and the deformation was measured with LVDTs.

6.1 Procedure to determine the modulus of elasticity

6.1.1 Theory

The set-up used for the bending tests is discussed in paragraph 4.2. The load on the specimen was increased and subsequently decreased stepwise and the deformations of the specimen were determined for each load step. At each test temperature, the procedure for the evaluation of the modulus of elasticity was as follows:

- For each step, the applied force was plotted as a function of the applied deformations, as shown with dots in Figure 6-1;
- A straight, best-fitted line was drawn through the data, which describes the relation between force and displacements;
- With the theoretical relation between force and displacements at midspan according to (6.1), the modulus of elasticity can be determined. Because the displacements were measured slightly out of the middle of the strip instead of at midspan, equation (6.2) was applied.

\[
\delta_{\text{mid}} = \frac{1}{48} \frac{F \cdot L^3}{E \cdot I_z} \quad (6.1)
\]

\[
\delta_i = \frac{1}{16} \frac{F \cdot L^2 \cdot l}{E \cdot I_z} - \frac{1}{12} \frac{F \cdot I^3}{E \cdot I_z} \quad \Rightarrow \quad E = \left( \frac{1}{16} \frac{L^2 l}{I_z} - \frac{1}{12} \frac{l^3}{I_z} \right) \frac{F}{\delta_i} \quad (6.2)
\]
With

- \( L \) = Span [mm]
- \( l \) = Distance from location where \( \delta \) is measured to nearest support [mm] (Figure 6-2)
- \( F \) = applied load [N]
- \( I_w \) = Moment of inertia for bending about the weak axis [mm\(^4\)]
- \( \delta_{\text{mid}} \) = Deflection at midspan [mm]
- \( \delta \) = Deformation at measured location [mm]
- \( E \) = Modulus of elasticity [N/mm\(^2\)]

\[ L \]
\[ l \]
\[ \delta_{\text{mid}} \]
\[ \delta \]

Figure 6-2 – Definition of \( L \) and \( l \) in equation (6.2)

### 6.1.2 Creep influence

At elevated temperatures, the deformation increased during constant loading, indicating that creep strains developed during the short loading period. Especially at high temperatures, these creep strains already developed at small loads (Figure 6-3). This complicates the evaluation of the modulus of elasticity.

![Figure 6-3](image)

Figure 6-3 – Measured displacements during loading and unloading for alloy 5083-H111 at 208 °C (left-hand) and 315 °C (right-hand) (different weights are applied at these temperatures)

At high temperatures, creep strains developed during loading (even though the duration of a load step was not more than 10 seconds). This is indicated by the difference in stiffness between loading and unloading (Figure 6-4 and Figure 6-5).
In paragraph 2.2, it was explained that, during loading at elevated temperature, the total mechanical deformation is composed of elastic strain, visco-elastic strain and visco-plastic strain. During unloading, the deformation is composed of elastic strain and visco-elastic strain. Visco-elastic strain is only a small fraction of the total creep strain. Evaluation of the modulus of elasticity based on the test data during unloading thus yields more accurate results than based on the data during loading. Besides, at low stress levels the influence of creep is smaller than at high stress levels. Therefore, the modulus of elasticity in the bending tests was evaluated based on the data during unloading at low stress levels.

In case of alloy 5083-H111 at temperatures larger than 300 °C, the influence of creep strains is so large that the resulting modulus of elasticity should be regarded with reserve (Figure 6-5).

6.1.3 Influence of restrained lateral contraction

Equations (6.1) and (6.2) apply for cross-sections for which lateral contraction is not restrained. For plates bended about the weak axis, however, membrane action of the plate may result in smaller vertical deformations than predicted by the equations. In
In the case of a plate with infinite width, lateral contraction is fully restrained and the deformation in equations (6.1) and (6.2) should be multiplied with a factor \((1-\nu^2)\). For plates with a finite width, lateral contraction may result in an unequal deformation along the width, where the edges of the plate ‘jump up’.

The ratio between plate width and plate thickness of the specimens of alloy 5083-H111 was \(b/t = 6\). In case of alloy 6060-T66 this ratio was \(b/t = 30\). With finite element models, the influence of restrained lateral contraction on the deformations of the specimens was evaluated. For the specimens with \(b/t = 6\) (alloy 5083-H111), the influence of restrained lateral contraction on the deformations was negligible. For the specimens with \(b/t = 30\) (alloy 6060-T66), however, the influence of restrained lateral contraction was significant: in case of \(\nu = 0.33\), the difference between the deformation according to equations (6.1) and (6.2) and the FEM models was 11%. For this reason, additional tests were carried out on specimens of alloy 6060-T66 with a plate width over plate thickness ratio of \(b/t = 7.5\). The results given below for tests on specimens with \(b/t = 30\) are corrected for the influence of lateral contraction as determined with the FEM models.

### 6.2 Results for alloy 5083-H111

The specimens of alloy 5083-H111 had a span of 250.0 mm and a cross-section \(b \times t = 30.00\, \text{mm} \times 4.948\, \text{mm}\). The resulting values for the modulus of elasticity are given as a function of temperature with solid dots in Figure 6-6. The same figure shows data for alloy 5083-O given by Kaufman [11], which are based on steady-state tensile tests. Because of the large influence of creep, the values for temperatures exceeding 300 °C (indicated with open dots) should be regarded with reserve.

![Figure 6-6 – Modulus of elasticity as a function of temperature for alloy 5083-O/H111](image)

There is a good agreement between the current test results and the data by Kaufman at low temperatures, but at high temperatures the modulus of elasticity determined in the current tests is higher. This difference may be due to the influence of creep: if the data of Kaufman are determined during the initial loading phase, creep deformations - which are present even at the lowest stress levels at these temperatures - are incorporated in the evaluation of the modulus of elasticity.

### 6.3 Results for alloy 6060-T66

In case of alloy 6060-T66, tests were conducted on specimens with a span of 250.0 mm and a width of 40.00 mm. The thickness was 1 mm or 4 mm. Figure 6-6 gives the
modulus of elasticity of alloy 6060-T66 determined in the current tests and the modulus of elasticity of similar alloys determined in tensile tests according to various authors. The orange squares are results of measurements with wall thickness of 1 mm (corrected for the influence of lateral contraction) and the green triangles represent measurements on specimens with a wall thickness of 4 mm. At temperatures higher than 250 °C, creep had such a large influence on the deformations of the specimens with wall thickness of 4 mm, that the modulus of elasticity could not be determined accurately.

Figure 6-7 – Modulus of elasticity as a function of temperature for alloy 6060-T66

There is a reasonable agreement between the current tests and the data in literature.

6.4 Chapter conclusions

The modulus of elasticity at room temperature determined in the bending tests was equal to the values determined in the tensile tests and the values in literature.

Due to the small elastic strain compared to the thermal strain in tensile tests, the modulus of elasticity could not be accurately determined at elevated temperature with tensile tests. The bending tests carried out in this study appeared to be appropriate to measure the modulus of elasticity.

The modulus of elasticity at elevated temperature of alloy 6060-T66 was approximately equal to the values given in literature for similar alloys. The modulus of elasticity of alloy 5083-H111 at elevated temperature was slightly higher than the values given in literature.
7 Results of creep tests

Creep tests were carried out as described in paragraph 3.1. In most tests, either the temperature or the load was changed stepwise during a test. The following procedure was applied in these tests:
- Heating of the specimen to a certain temperature in 40 seconds. No load is applied;
- Maintaining the specimen temperature at this temperature (= test temperature) for 5 seconds and applying the load in five seconds;
- Maintaining the specimen temperature and load at these levels for a certain time and measure the strain;
- In some cases: change the temperature or the load in 5 seconds and keep these constant for a certain time.

The tests were especially aimed at determining the strain rate during the secondary creep phase, also called the minimum or steady-state strain rate.

In publications concerning creep, it is customary to present the creep strain rate as strain per second. In fire design, however, it is customary to express time units in minute. In this document, the time units usually applied in fire design are chosen. This means that the creep strain is given as a function of the time per minute.

The most important results are given in this chapter. All test results are given in detail in Annex E.

Paragraph 7.1 gives the results of the creep tests on alloy 5083-H111. In case of alloy 6060-T66, two test series were carried out, on specimens originating from different batches. The results of these test series differed, therefore the test series are discussed separately. Paragraph 7.2 gives the results of the first series, carried out in 2005. Paragraph 7.3 gives the results of the second series, carried out in 2006. Chapter conclusions are given in paragraph 7.4.

7.1 Creep tests on alloy 5083-H111

Creep tests were carried out to determine the influence of temperature on the creep strain, and to determine the influence of the stress level on the creep strain. Both types of tests are discussed separately.

7.1.1 Influence of temperature

As an example, Figure 7-1 gives the result of a creep test with increased temperature on alloy 5083-H111. The upper graph gives the temperature as a function of the time. The dark grey curve of the middle graph gives the measured (total) strain as a function of the time. The engineering mechanical strain (black curve) in this graph is determined by subtracting the theoretical thermal expansion from the total strain. This theoretical thermal expansion is determined according to the equation in EN 1999-1-2 (equation (7.1)). This equation gave the same expansion as measured during a dummy test with an unloaded specimen and a constant heating rate up to 400 ºC. In the relevant strain range (up to approximately 2,5 %) thermal strain forms a significant part of the total measured strain.

The light grey curve gives the true mechanical strain, determined by dividing the change in length by the actual length of the specimen.
\[ \varepsilon_{\exp} = 1 \cdot 10^{-8} \cdot \theta_a^2 + 2,25 \cdot 10^{-3} \cdot \theta_a - 4,5 \cdot 10^{-4} \] (7.1)

For each temperature, the secondary strain rate was determined by drawing best-fitted straight lines through the parts of the curve with a constant strain rate. The secondary strain rates are indicated by straight lines in the lower graph of Figure 7-1. The graphs in Figure 7-2 display the same results as, but for smaller creep strains.

It is shown that for the lowest temperature (200 ºC), the creep behaviour is dominated by primary creep. At the end of this creep period, it is not clear whether or not the secondary creep phase has already started. At the highest temperature (240 ºC), the creep strain rate increases, indicating that the tertiary creep phase has started. In this test, the secondary creep strain could only be determined with certainty for the intermediate temperature (220 ºC). In such cases, only this creep strain rate was taken into account in the evaluation of the results.
Figure 7-1 – Result of a creep tests with stepwise increased temperature on alloy 5083-H111
One test with a certain load and temperature was carried out twice in order to determine the reproducibility. The results are given in Figure 7-3. The tests show an almost equal strain development.
Evaluating all tests, the relevant strains were dominated by the secondary creep phase. To evaluate the secondary strain, Figure 7-4 gives the logarithm of the secondary strain rate as a function of the reciprocal of the absolute value of the temperature. The different curves indicate different tests carried out, with various stress levels.

Figure 7-4 – Secondary strain rate as a function of the reciprocal of the temperature for alloy 5083-H111

The slopes of the lines are parallel, indicating that the relation between secondary strain rate and temperature is according to the equation proposed by Dorn [3]:

\[ \dot{\varepsilon}_{\text{cr,II}} \sim f(\sigma) \cdot e^{Q/RT} \]  

(7.2)

7.1.2 Influence of stress

Tests are also carried out with a constant temperature and a stepwise increased stress level. An example is given in Figure 7-5.
As in case of the temperature, the secondary creep strain rate increases at increasing load level. At each load increase, the strain curve shows an instant increase in strain. This instant increase is the sum of the elastic strain and primary creep. In the tests with stepwise increased temperature, this instant increase in strain was not present. Hence, for the temperature range studied, primary creep is a function of the stress only and not of the temperature.

For all tests carried out with increasing load level, Figure 7-6 gives the logarithm of the secondary strain rate as a function of the logarithm of the stress. The different curves indicate different tests carried out, with various temperatures.
The curves are more or less parallel. The logarithm of the strain rate increases with the logarithm of the stress. This relation can be described with the equation of McQueen and Jonas, [24], according to equation (2.8):

\[
\dot{\varepsilon}_c \sim (\sinh \alpha \sigma)^n \cdot f(T)
\] (7.3)

In which \( \alpha \) and \( n \) are material parameters independent of temperature and stress level. These values will be determined in chapter 9.

### 7.1.3 Primary creep

The tests were not carried out so accurately as to determine the primary creep strain. Voorhees and Freeman [32] provided data of creep tests on some aluminium alloys, among which alloy 5083-O. Mentioned was the projection back to zero time of the secondary creep strain, \( \varepsilon_{00} \), which is a measure for primary creep (Figure 2-4, paragraph 2.3.2). Equation (2.10) suggests that a straight line should fit the test data when the values for \( \varepsilon_{00} \) are plotted as a function of the stress on double-logarithmic scale. Figure 7-7 shows this diagram for the data on alloy 5083-O. The figure indicates that there exists indeed an approximately linear relation, apart from two test results which deviate from the other results.

Harmathy [8] indicates that \( \varepsilon_{00} \) is a poorly reproducible factor, and the parameters \( D \) and \( m \) are based on a plot with badly scattered points. Figure 7-7 shows that two test results deviate significantly from the other results. The relation according to equation (2.10) is therefore used, even though the two test results deviate from this relationship.

The grey line in Figure 7-7 represents equation (2.10), with the material parameters determined with the least square method, excluding the two test results that deviate from the other results.

![Figure 7-7 – \( \varepsilon_{00} \) as a function of the stress for alloy 5083-O, test data source is Voorhees and Freeman (both axes on logarithmic scale)
7.2 Creep tests on alloy 6060-T66, series 2005

Similar tests as described for alloy 5083-H111 are carried out on alloy 6060-T66. Two test series were carried out, on different batches of the material. The results of these test series differed, therefore the test series are discussed separately. The first creep test series was carried out in 2005.

7.2.1 Influence of temperature

In case of alloy 6060-T66, the tertiary creep phase started at a much lower strain level as in case of alloy 5083-H111. This is illustrated in Figure 7-8, which gives the mechanical strain as a function of the time in a creep test with constant temperature and stress. The primary stage seems to transfer into the tertiary phase without a period with constant strain rate. This complicates the analysis of the creep behaviour, because the minimum creep rate detected in the tests was so small that the LVDT may be not accurate enough.

A second phenomenon detected was that the creep strain is extremely sensitive to small changes in temperature and stress level. In a number of creep tests, the strain was so small that it could not be measured accurately, while for a small temperature or stress increase, the tertiary creep stage started immediately (Figure 7-9). Therefore, small temperature (and stress) increments had to be applied (Figure 7-10).

As alloy 6060-T66 is heat treatable, it is possible that the strain rate depends on the thermal exposure period. This time-dependency was not studied in the test series 6060-T66-2005 (it was studied in test series 6060-T66-2006, paragraph 7.3).

![Figure 7-8 – Creep strain in a tests on alloy 6060-T66 with constant temperature and stress of 104 N/mm²](image-url)
Several tests were carried out with a stepwise-varied temperature. Additional tests were carried out with a single value for the temperature and the stress level. Despite the difficulties described above, it was attempted to determine the minimum creep strain rate, as most information in literature concerns this parameter and analytical creep models are usually based on this parameter.

Figure 7-11 gives the logarithm of the secondary strain rate as a function of the reciprocal of the absolute value of the temperature. At low stress levels, some tests are carried out at the same temperature and load level as applied in other tests. These equal
tests, however, give a considerable different strain rate (encircled in Figure 7-11). Also, the slopes of the curves at different stress levels are not equal.

![Graph showing secondary strain rate as a function of the reciprocal of the temperature for alloy 6060-T66 – all test data](image)

Figure 7-11 – Secondary strain rate as a function of the reciprocal of the temperature for alloy 6060-T66 – all test data

In order to investigate the differences, a distinction was made between the tests with one temperature level and the tests with a stepwise-varied temperature:
- Figure 7-12 gives the results of different tests with one stress level and a one temperature. Each curve gives the results of different tests, carried out at the same stress level. There are significant differences in the slopes of the curves.
- Figure 7-13 gives the results of tests with a stepwise-varied temperature. Each curve represents one test, carried out with a constant load and a stepwise increased temperature. The curves are more parallel, although differences in the slopes are still apparent. The tests are carried out with an increasing temperature. At the lowest temperatures, i.e. at the first temperature increment, the slopes are lower than at higher temperatures, i.e. at following temperature increments. The same difference appears when Figure 7-13 is compared with Figure 7-12. A possible explanation is that in tests with several temperature increments, the results of the last temperature increments are influenced by the appearance of tertiary creep. The start of tertiary creep may depend on the strain developed and/or on the thermal exposure time, the latter influencing the temper;
- To exclude tertiary creep, Figure 7-14 gives the results of the same tests as in Figure 7-13, however, only for low stress levels, so that tertiary creep does not influence the results.
Figure 7-12 - Secondary strain rate as a function of the reciprocal of the temperature for alloy 6060-T66 – data of tests with one value for the temperature

Figure 7-13 - Secondary strain rate as a function of the reciprocal of the temperature for alloy 6060-T66 – data of tests with stepwise increased temperature
7.2.2 Influence of stress

Tests are carried out with a constant temperature and a stepwise increased load. Also, tests are carried out with both a constant temperature and load level.

In case only the data of tests with a stepwise increased temperature and small strains are taken into account, the slopes of the lines are almost parallel (Figure 7-14). This indicates that, for small strains, Dorn’s equation describes the temperature dependency on the creep strain of alloy 6060-T66 with reasonable accuracy. The activation energy for creep of alloy 6060-T66 is appropriately determined if they are based on tests with a stepwise increased temperature and relatively low stress levels.
The minimum creep rate is given as a function of the stress level in Figure 7-16 for all tests carried out. Figure 7-17 gives these results of the tests with a stepwise-varied temperature only. Figure 7-18 gives the results of the same tests as in Figure 7-17, but only for small strains so that the creep strain is not in the tertiary stage. Each curve in Figure 7-17 and Figure 7-18 represents one test, carried out with a constant temperature and a stepwise increased load.

Figure 7-16 – Secondary strain rate as a function of the stress for alloy 6060-T66 series 2005 – all test data

Figure 7-17 – Secondary strain rate as a function of the stress for alloy 6060-T66 series 2005 – data of tests with stepwise increased load
Figure 7-18 – Secondary strain rate as a function of the stress for alloy 6060-T66 series 2005 – data of tests with stepwise increased temperature and small strains

In case all test data are taken into account (Figure 7-16), there is a large variation in slopes of the curves for different temperatures and for different tests carried out at the same temperature. In case only tests carried out with a stepwise varied stress level are considered (Figure 7-17), the curves are more or less parallel. In case only the results for small strains are considered in tests carried out with a stepwise varied stress level (Figure 7-18), the curves of four tests are parallel, except for one test with low creep strain rates. Only in case of Figure 7-18 can the relation between stress and secondary strain rate be described by equations (2.6) up to (2.9).

The results of the individual tests are given in Annex D.

Appropriate tests to determine the parameter \( \varepsilon_{t0} \) (measure for primary creep strain) were not carried out. Tests results were not found in literature for alloy 6060-T66. Consequently, no information is available on primary creep of test series 6060-T66-2005.

### 7.2.3 Tertiary creep

Tertiary creep is often associated with the occurrence of necking, causing inhomogeneous stress in the specimens. However, measurements on the dimensions of the specimen after testing did not reveal a necking area. Either the creep strain at the end of the test was too small to result in a measurable neck, or the start of the tertiary creep stage is not caused by necking at one position. Test series 2006 (paragraph 7.3) showed that the tertiary creep strain is homogeneous at least for the first part of the tertiary creep stage.

As tertiary creep already occurs for small strains, so that it dominates creep curves, it seems relevant to find a law for the tertiary creep strain rate. As tertiary creep evolves, the creep strain rate increases. There may be a relationship between the strain rate and the strain in the tertiary stage. To see whether such a relationship exists, the strain rate was plotted as a function of the strain for all creep tests of alloy 6060-T66 exhibiting tertiary creep.
In all cases, an approximately linear relation was present between the creep strain rate and the creep strain in the tertiary stage. The relation could be represented by a straight line starting at the origin of the graph. Figure 7-19 and Figure 7-20 give some examples.

![Figure 7-19](image1.png)

Figure 7-19 – Strain as a function of time (left-hand) and strain rate as a function of strain (right-hand) in a creep test with $\sigma = 30$ N/mm$^2$ and temp = 330 $^\circ$C on alloy 6060-T66

![Figure 7-20](image2.png)

Figure 7-20 – Strain as a function of time (left-hand) and strain rate as a function of strain (right-hand) in a creep test with $\sigma = 35$ N/mm$^2$ and temperature steps 300, 310, 320 and 330 $^\circ$C on alloy 6060-T66

### 7.3 Alloy 6060-T66, series 2006

The scatter in the temperature and stress dependency of the secondary strain rate of alloy 6060-T66 in series 2005 was large. Possible reasons for this large scatter are the short period of secondary creep, which causes small secondary creep strains, and dependency of the thermal exposure period. Tests to determine primary creep strain were not carried out.

A second test series on alloy 6060-T66 was carried out in 2006, in which it was focussed on finding solutions for the problems detected.

- The LVDT was equipped with heavier springs, so that there exists tighter contact between the specimen and the LVDT. In this way, slip between the specimen and the LVDT was prevented, so that small strains could be determined with more accuracy;
- The tests were divided into blocks with equal thermal exposure periods. Most tests were conducted with typical exposure periods of approximately 20 minutes, and the
temperature and stress dependency of the strain rate was determined for this period. A number of tests were carried out under similar load conditions, but after being subjected to a thermal exposure period of 90 minutes.

The specimens originated from a different batch as the series in 2005. The chemical composition was slightly different (Annex H).

### 7.3.1 Tests with a test period of 20-30 minutes

Tests were carried on specimens that were subjected to elevated temperature and directly loaded when the temperature was reached.

Figure 7-21 gives the secondary strain rate as a function of the reciprocal of the temperature. Each curve gives the result of a test carried out with constant stress and a stepwise increased temperature. For this test series, the different curves are parallel, indicating that Dorn’s equation for the temperature dependency of the secondary strain rate is applicable on alloy 6060-T66, at least for test temperatures of 20 up to 30 minutes.

![Figure 7-21 – Secondary strain rate as a function of the stress for alloy 6060-T66 series 2006](image)

### 7.3.2 Influence of thermal exposure period

In order to determine the influence of overageing and / or annealing on the strain rate, a number of tests were first subjected to an constant elevated temperature for 90 minutes, and then a mechanical load was applied, i.e. the specimens were first subjected to a thermal exposure period and then the creep tests were carried out (schematically shown in Figure 7-22). The results were compared to the tests described in the previous paragraph, i.e. specimens loaded directly after the test temperature was reached.
The strain rate in the tests carried out at relatively low temperatures (185-230 °C) agreed with the tests directly loaded: the difference in strain rate was less than 10%. In case of higher temperatures (250-320 °C), the strain rate of the specimens subjected to a constant elevated temperature for 90 minutes, was three to four times higher than in the tests loaded directly, indicating that overageing and / or annealing indeed influence the creep rate at high temperatures.

However, a constant elevated temperature for 90 minutes plus loading time is not relevant for fire exposure. In a fire, structures are (first) subjected to an increasing temperature. Therefore, two additional tests were carried out which were first heated from room temperature to test temperature in 90 minutes, and then loaded (schematically shown in Figure 7-23). A constant heating rate was applied in the first 90 minutes and during loading the temperature was constant. In case of these tests, the strain rate was approximately equal to the tests loaded directly after heating (compare black curves, dark grey curves and light grey curves in Figure 7-24 and Figure 7-25). Table 7.1 gives an overview of the tests that were subjected to a certain period at elevated temperature before the load was applied.

It is concluded that, although overageing and / or annealing influence the strain rate of alloy 6060-T66, it is not necessary to take this influence into account in creep models used in fire design.
Figure 7-23 – Temperature and load in a test heated from room to test temperature in 90 minutes and then loaded.

Figure 7-24 – Tests on alloy 6060-T66 series 2006 with $\sigma = 35$ N/mm$^2$ and various temperature conditions prior to loading.
7.3.3 Primary creep

With the modified LVDT, it was possible to accurately determine the primary creep strain. Figure 7-26 gives $\varepsilon_0$ as a function of the stress on double logarithmic scale. It is shown that the relationship is approximately linear.

The two tests with the lowest stress levels, indicated with circles in Figure 7-26, deviate from the average trend (grey line). When plotting the results on a linear scale (Figure 7-27), it is obvious that the two deviating results result in such small values for $\varepsilon_0$ that primary creep is difficult to measure (which explains the deviation) and not very relevant.

The grey line in Figure 7-27 represents equation (2.10), with the material parameters determined with the least square method, excluding the two test results that deviate from the other results.
Figure 7-26 – $\varepsilon_{t0}$ as a function of the stress for alloy 6060-T66 series 2006 – double logarithmic scale

Figure 7-27 – $\varepsilon_{t0}$ as a function of the stress for alloy 6060-T66 series 2006 – linear scale

7.3.4 Tertiary creep

Necking causes an inhomogeneous stress and strain distribution. Therefore, the strain resulting from the measurement depends on the measured length when necking occurs. In order to check whether or not a homogeneous strain is present in the first part of the tertiary creep stage, tests were carried out with different gauge lengths of the LVDT.

A temperature gradient exists along the specimen length. A possible difference in strain rate for different gauge lengths may therefore not only be due to necking, but could also be due to difference in temperature. A detected difference in strain rate could also be due to scatter in creep test results. In order to determine whether difference in
temperature and scatter in test results influence the measurements with different gauge lengths, the tests with different gauge lengths were first subjected to a period of secondary creep, before the temperature was increased such that the tertiary creep phase started.

The resulting strain of the tests is given in Figure 7-28. The gauge lengths used in the different tests were selected such that the difference in temperature along the gauge length was limited (10 °C for a gauge length of 30.7 mm) and that the strain could still be measured accurately (which limits the smallest gauge length used, of 13.9 mm). In all tests, the maximum temperature of the specimen, i.e. the position where necking might occur, is within the gauge length.

The figure shows that the strain rates differ in the secondary creep stage. In the tertiary stage, the curves are almost parallel. Figure 7-29 gives the strain rate as a function of the strain for all three tests. As observed for test series 2005, there exists a linear relation between the strain and the strain rate (paragraph 7.2.3). The curves of the three tests coincide for the tertiary creep stage, which indicates that the linear relation between the strain and the strain rate is independent of the gauge length, so that the strain has to be approximately homogeneous in the first part of the tertiary creep stage.

![Figure 7-28 – Strain as a function of time for various gauge lengths](image-url)
As necking is associated with tension, but does not necessarily occur in compression, it is checked whether the same strain development occurs when the specimen is loaded in compression.

For this purpose, a test was carried out in which the specimen was loaded in compression. After a period of secondary creep, the temperature was increased. As in tension, the compressed specimen was subjected to tertiary creep, again with a linear relation between the creep strain and the strain rate (Figure 7-30).

It is concluded that the creep strain in the first part of tertiary creep is still homogeneous. This allows for a relatively simple mathematical description of the first stage of tertiary creep.
It should be noted that an increasing creep strain rate while the load and temperature remained constant could also be due to the fact that the cross-section decreases as the strain evolves. Therefore, the true stress was compared with the engineering stress. During the first part of the tertiary creep stage, the true stress was approximately equal to the engineering stress, indicating that the decrease in cross-section is still too small to generate a significant increase in strain rate.

7.3.5 Visco-elastic strain

As mentioned before, Harmathy stated that most creep strain is irreversible (visco-plastic strain) while only a small portion is reversible (visco-elastic strain). In order to check whether this statement holds for the aluminium alloys considered, a test was carried out in which a specimen was subsequently loaded, unloaded and again loaded. The creep strain in this test is determined by subtraction of the instant elastic strain (and the thermal strain) from the total measured strain. The load and temperature are given in Figure 7-31 and the creep strain is given in Figure 7-32.

![Figure 7-31](image_url)  
Figure 7-31 – Stress and temperature in a test on alloy 6060-T66-2006, loaded, unloaded and again loaded
Figure 7-32 shows that, conform Harmathy's statement the reversible creep strain (i.e. the visco-elastic strain) is only a small portion of the total creep strain.

This test is only carried out at one load and temperature level. It is assumed that the conclusion is valid for the entire range of stresses and temperatures.

7.3.6 Creep strain in compression

In order to check whether or not the creep strain in compression is equal to the strain found when a specimen is loaded in tension, two creep tests were carried out which were first loaded in tension and subsequently in compression. Absolute values for the load were equal.

In the first test, the temperature was increased after a certain creep period in compression. Before this temperature increase was applied, the load and temperature in tension was equal to that in compression. During the period that the conditions in compression were equal to that in tension, the strain rates were approximately equal (1.1 \times 10^{-4} for compression and 1.25 \times 10^{-4} for tension, Figure 7-33).
A second test was carried out which was loaded in tension, in compression, again in tension and again in compression (Figure 7-34). During the second time loaded in tension and compression, the creep strain was larger than the first time. This is attributed to tertiary creep. The strain rates of the first times loading in tension and compression were equal as in the test shown in Figure 7-33 (Tension $1.25 \times 10^{-4}$ and compression $-1.1 \times 10^{-4}$, which is a difference of 12 %).

The measure for primary creep strain, $\epsilon_{t0}$, is in compression also approximately equal to $\epsilon_{t0}$ in tension, in both tests.

These creep tests, loaded in tension and subsequently in compression, are only carried out at one stress level and one temperature. In these tests, the strain rate in compression
was approximately equal to that in tension. It is assumed here, that the strain rate in compression is approximately equal to that in tension for the entire temperature and stress range relevant for fire. In this case, the relations between temperature and creep strain and stress and creep strain, as determined in tension, are also valid with reasonable accuracy in compression.

7.4 Chapter conclusions

When comparing the results of alloy 5083-H111 and 6060-T66, it appears that the creep strain of alloy 5083-H111 is dominated by secondary creep in the range of relevant creep strains, while in case of alloy 6060-T66 it is dominated by tertiary creep.

For alloy 5083-H111, the relations between the temperature and the secondary creep strain rate, and between the stress and the secondary creep strain rate were according to the equations in Dorn’s theory.

Due to overageing and / or annealing, the creep strain rate of alloy 6060-T66 depends on the thermal exposure period at high temperatures. However, tests show that the strain rate is approximately equal for the heating rates relevant for fire design. It is therefore not necessary to take the influence of overageing and / or annealing into account in creep models for fire design.

In one creep test on alloy 6060-T66, carried out at one temperature and load level, Harmathy’s statement was confirmed that the reversible part of the creep strain (visco-elastic strain) is only a small part of the total creep strain. It is assumed that this applies to the entire stress and temperature range that are relevant for structural fire design, for both alloys.

With two creep tests on alloy 6060-T66, carried out at one temperature and load level, it was determined that the creep strain rates in compression are approximately equal to the rates in tension. It is assumed that this applies to the entire stress and temperature range that are relevant for structural fire design, for both alloys.

In chapter 9, the information in this chapter is used to draft an analytical model for material properties.
8 Results of transient state tests

In the Gleeble test machine, transient state tests with linear increasing temperature are carried out. The LVDT used in these tests was accurately enough to be able to accurately measure the thermal and mechanical strains. These tests are discussed in this chapter. Results of pilot tests are shown in Annex E.

8.1 Tests on alloy 5083-H111

8.1.1 Tests with constant load

Tests are carried out with a constant heating rate and a constant load. An example of one of the tests, with a constant stress level of 41 N/mm², is given in Figure 8-2. The upper graph of this figure gives the temperature of the specimen as a function of the time. The lower graph gives the mechanical strain as a function of the time, which is obtained by subtracting the theoretical thermal strain according to equation (7.1) from the total measured strain.

At low strain levels, the thermal strain may be several orders larger than the mechanical strain. Because of this, the mechanical strain at small strain levels may be inaccurate.

![Figure 8-1 – Stress and temperature in a transient state test on alloy 5083-H111](image-url)
Figure 8-2 – Strain in a transient state test on alloy 5083-H111 (test no. 20)

Figure 8-3 up to Figure 8-7 give the results of transient state tests carried out with a linear increasing temperature and a constant load. Some graphs give the results of two tests, carried out with different heating rates. The heating rates applied were such that failure occurred after approximately 30 and 120 minutes. Other graphs give the results of one test, carried out with such a heating rate that failure occurred after approximately 30 minutes. The heating rates are indicated in the legend of each graph.

Figure 8-3 – Transient state test on alloy 5083-H111 with a stress level of 20 N/mm\(^2\)
Figure 8-4 – Transient state tests on alloy 5083-H111 with a stress level of 41 N/mm$^2$

Figure 8-5 – Transient state test on alloy 5083-H111 with a stress level of 60 N/mm$^2$
The figures show that there is a difference in critical temperature for the two heating rates applied. The heating rates are selected in such a way, that the extreme values of the range of heating rates encountered in practice for insulated sections are covered (fire resistance periods of 30 and 120 minutes). The difference in critical temperature for the heating rates applied in the tests was 25 to 30 °C, i.e. approximately 12 minutes in case of the low heating rate.

8.1.2 Test with varying load

In addition, two transient state tests are carried out with both a linearly varied temperature and a linearly varied load. Figure 8-8 gives the result of a test with
increasing temperature and increasing load, Figure 8-9 gives the result of a test with increasing temperature and decreasing load.

5083-H111, stress rate = 2 N/mm²/min
heating rate 9.3 °C/min

Figure 8-8 – Transient state tests on alloy 5083-H111 with an increasing temperature and load level
5083-H111, $\sigma = 120 \text{ N/mm}^2 - 2 \text{ N/mm}^2/\text{min}$
heating rate 9.3 $^\circ\text{C/min}$

Figure 8-9 – Transient state tests on alloy 5083-H111 with an increasing temperature and a decreasing load level

In the transient state test with increasing stress (Figure 8-8), a mechanical strain results that is smaller than zero at the beginning of loading. Figure 8-10 gives a detailed view of the total and mechanical strain after a short period of this test. The reason for the negative stress level appears to be that no stress is measured at the beginning of loading, while the temperature and force are already applied. This is attributed to the fact that the clamps of the LVDT, which are attached to the specimens, have to set before the LVDT detects a displacement (Figure 8-11). This also occurred in some other tests.
The tests described in this paragraph are used to validate the analytical model for material properties in fire (chapter 9).

8.2 **Tests on alloy 6060-T66, series 2005**

Transient state tests are also carried out on alloy 6060-T66, batch 2005. All tests are carried out with a constant heating rate and a constant load in time. Figure 8-12 up to Figure 8-14 give the results of tests with a linear increasing temperature and a constant load. Each graph gives the results of two tests, carried out with different heating rates. The heating rates applied were such that failure occurred after approximately 30 and 120 minutes. Additionally, one test was carried out at a high stress level and such a heating rate that failure occurred after approximately 30 minutes Figure 8-15.
Figure 8-12 – Transient state tests on alloy 6060-T66 with a stress level of 76 N/mm$^2$

Figure 8-13 – Transient state tests on alloy 6060-T66 with a stress level of 50 N/mm$^2$
The figures show that there is a difference in critical temperature for the two heating rates applied. The heating rates are selected in such a way, that the extreme values of the range of heating rates encountered in practice for insulated sections are covered (fire resistance periods of 30 and 120 minutes). The difference in critical temperature for the heating rates applied in the tests was 15 to 20 °C, i.e. approximately 9 minutes in case of the low heating rate.

The test high stress levels (Figure 8-14 and Figure 8-15) show a creep strain that develops in time. On the contrary, in case of the lower stress levels almost no creep strains develop for a temperature just below the critical temperature, but at the critical
temperature the strain increases from negligible to high values during a temperature increase of only a few degrees Celsius.

The tests described in this paragraph are used to validate the analytical model for material properties in fire (chapter 9).

8.3 Tests on alloy 6060-T66, series 2006

8.3.1 Tests with constant load

Transient state tests are conducted on test series 2006 of alloy 6060-T66. The resulting strains of the test with a constant heating rate and a constant load are given in Figure 8-16 up to Figure 8-19.

![Figure 8-16](image_url)  
6060-T66, $\sigma = 40 \text{ N/mm}^2$

![Figure 8-17](image_url)  
5083-H111, $\sigma = 76 \text{ N/mm}^2$
The figures show that there is a difference in critical temperature for the two heating rates applied. The heating rates are selected in such a way, that the extreme values of the range of heating rates encountered in practice for insulated sections are covered (fire resistance periods of 30 and 120 minutes). As in case of series 2005, the difference in critical temperature for the heating rates applied in the tests with $\sigma = 40$ N/mm$^2$ and $\sigma = 93$ N/mm$^2$ was 15 to 20 $^\circ$C, i.e. approximately 9 minutes in case of the low heating rate. However, the tests carried out with $\sigma = 126$ N/mm$^2$ (Figure 8-19) show unexpected behaviour at high strains. The test with a lower heating rate initially results in larger strains, but as the test continues, the strains of the two tests are approximately equal. This could be due to a measurement error, or it could be due to differences in temper
due to the different periods of moderately elevated temperature of the two tests. It is possible that the material structure of the test with a lower heating rate is more optimal than that of the test with a higher heating rate due to the fact that the specimen with a lower heating rate has obtained a longer treatment during heating.

8.3.2 Tests with varying load

In addition, two transient state tests are carried out with both a linearly varied temperature and a linearly varied load. Figure 8-20 gives the result of a test with increasing temperature and increasing load, Figure 8-21 gives the result of a test with increasing temperature and decreasing load.
8.3.3 Tests with compression load

Three transient state tests were conducted subjected to a compression load. One of the specimens had equal dimensions as the specimens used in the tension tests. This specimen failed by flexural buckling. Two other specimens had a shorter length, with ratios between free length and thickness of 1 and 3. These specimens failed by shear (stub column tests). The shape of the specimens after testing is shown in Figure 8-22.

The load applied in all three compression tests was equal to 76 N/mm² and the heating rate was equal to 8.0 °C/min, i.e. the same conditions as the tensile test in Figure 8-17.

The LVDT was not applied in the compression tests, because there was no space for the LVDT clamps in case of the stub column tests and it would have been possible that the LVDT would get damaged in case of the test that failed through flexural buckling.
As the specimen length of the flexural buckling specimen was equal to the length of the tensile test specimen, it is assumed that the cross-head deformation measured by the actuator of these tests can be compared with reasonable accuracy. The comparison is given in Figure 8-23.

![Deformed specimens of the transient state compression tests](image)

Figure 8-22 – Deformed specimens of the transient state compression tests

![Cross-head deformation of the transient-state tensile and compression test](image)

Figure 8-23 – Cross-head deformation of the transient-state tensile and compression test on alloy 6060-T66 with $\sigma = 76 \text{ N/mm}^2$,

$6060$-$T66, \sigma = 76 \text{ N/mm}^2, \text{heating rate } 8.0 \degree\text{C/min}$

Figure 8-23 shows that the critical temperature (defined as the asymptote of the strain curve) of the compression test is equal to that of the tensile test (294 °C). The critical temperature of the two stub column tests was also approximately equal to this temperature (291 and 297 °C for the tests with $L/t = 1$ and $L/t = 3$, respectively). Based on this, it is concluded that the strength in compression is equal to the strength in tension for a stress of 76 N/mm$^2$. It is assumed that the strength in compression is equal to the strength in tension for the entire temperature and stress range, relevant for fire.

The tests described in this paragraph are used to validate the analytical model for material properties in fire (chapter 9).
8.4 Discussion of test results

Based on the transient state tensile tests carried out, it is concluded that the heating rate of aluminium alloys influences the stress-strain relation, but only marginally. In case of alloy 5083-H111, the difference in critical temperature between heating in 30 minutes and heating in 120 minutes to collapse was approximately 25 °C and 15 °C for alloys 5083-H111 and 6060-T66, respectively. This conclusion agrees with an analysis of test results by Langhelle [12], who carried out transient state tests on columns of alloy 6082 T6 subjected to flexural buckling, and found that the difference in fire resistance between heating in 25 minutes and heating in 60 minutes is small.

When the results of alloy 5083-H111 are compared with that of alloy 6060-T66, it appears that the strain development in time is gradual in case of 5083-H111, i.e. creep strains develop at temperatures considerably lower than the critical temperature. In case of alloy 6060-T66, almost no creep strain develops at temperatures below the critical temperature (apart from tests with high stress levels). At the critical temperature, large strain rates are observed.
9 Material model for aluminium at elevated temperature

In this chapter, it is confirmed that the Dorn-Harmathy creep model, as described in paragraph 9.1, is suited as a base for the stress-strain relations of fire exposed aluminium including the influence of creep. In paragraphs 9.2 and 9.3, the material dependent parameters of this material model are determined based on the creep tests. An extension of the existing model for tertiary creep is given in paragraph 9.4. Paragraphs 9.5, 9.6 and 9.7 give the validation of the material model by simulating creep tests, transient state tests and steady state tests, respectively. Chapter conclusions are given in paragraph 9.8.

The material model is based on uniaxial tests and describes the strain development in case of uniaxial stress conditions. With the aid of some assumptions, the model is extended for multiaxial stress condition. This is elaborated in Annex I.

9.1 Description of the Dorn-Harmathy model

In chapter 0, the Dorn-Harmathy model for primary and secondary creep of metals is described. The resulting equation is repeated here:

\[
\dot{\varepsilon}_{I+II} (\sigma, t, T) = Z(\sigma) \cdot e^{-\frac{Q}{RT(t)}} \cdot \coth^2 \left( \frac{\varepsilon_{r}(\sigma, t, T)}{\varepsilon_{r0}(\sigma)} \right)
\]  

(9.1)

In which subscript \( I+II \) indicates that it concerns primary and secondary creep. Dorn noted the creep model is also suited for creep under variable temperature. In accordance with Harmathy’s proposal for fire exposed steel, it is assumed that the total mechanical strain of aluminium at elevated temperature is the sum of the creep strain according to equation (9.1) and the elastic (instant) strain, which is determined in chapter 6:

\[
\varepsilon_{tot}(\sigma, T, t) = \varepsilon_{E}(\sigma, T) + \int_{0}^{t} \dot{\varepsilon}_{I+II}(\sigma, T, t) \, dt
\]  

(9.2)

This implies that all creep strains are considered as irreversible (visco-plastic) strains. The small part of the creep strain that is recoverable (visco-elastic strain), is neglected. This is illustrated in Figure 2-1, where the modelled strain is according to the dashed grey curve, while the strain in reality follows the black curve. The consequences of this simplification are assumed to be small because of the noted small ratio between visco-elastic strain and visco-plastic strain. Besides, it is expected that unloading plays a relatively minor role in problems concerning fire exposed structural members.

Almost all creep tests were carried out in tension with a uniaxial stress condition. In structural design of aluminium at room temperature, it is custom to assume that the material in compression has equal strength as in tension. The same assumption is made here for elevated temperature (checked for one stress level in creep tests and one stress level in transient state tests, both on alloy 6060-T66). This implicates that phenomena
such as the Bauschinger effect, which may affect the material properties of aluminium alloys (Mazzolani, [19]) is neglected in the design.

9.2 Parameter determination for secondary creep strain

The material-dependent parameters in the Dorn-Harmathy model are the activation energy \( Q \) describing the temperature influence and the parameters of the Zener-Holloman parameter \( Z \) (i.e. \( A, \alpha \) and \( n \)) describing the stress influence on the secondary creep strain rate. These parameters are determined based on the creep tests.

Dorns equation for secondary creep implies that the influence of the temperature and the influence of the stress on the secondary strain rate are separable:

\[
\dot{\varepsilon}_{cr,H}(\sigma, T) = Z(\sigma) \cdot e^{Q/RT} = f_1(\sigma) \cdot f_2(T)
\]

This allows for a relatively simple determination of the temperature and stress influence on the secondary strain rate.

9.2.1 Activation energy

The parameter describing the influence of temperature on the secondary strain rate, the activation energy, can be determined in two ways:

- By carrying out two creep tests with equal stress levels (\( \sigma \)) at different temperatures \( (T_1 \) and \( T_2) \), and of these tests data are available on the times \( t_1 \) and \( t_2 \) at which the creep strains (\( \varepsilon_{cr} \)) are equal. In such a test, the strain at a certain time is equal to

\[
\varepsilon_t = f(\sigma) \cdot t \cdot e^{Q/RT}.
\]

The activation energy can then be determined with equation (9.4):

\[
t_1 \cdot e^{Q/T_1} = t_2 \cdot e^{Q/T_2} \quad \rightarrow \quad Q = \frac{R \cdot T_1 \cdot T_2}{T_2 - T_1} \ln \left( \frac{t_1}{t_2} \right)
\]

- By carrying out two tests with equal stress levels (\( \sigma \)) at different temperatures \( (T_1 \) and \( T_2) \) and determine the minimum creep strain rate in both tests. The activation energy is determined using equation (9.5). Alternatively, this procedure can be applied on the results of a single test with a constant stress level (\( \sigma \)) and a stepwise varied temperature.

\[
\frac{\dot{\varepsilon}_{cr,2}}{\dot{\varepsilon}_{cr,1}} = e^{Q/T_1} \quad \rightarrow \quad Q = \frac{R \cdot T_1 \cdot T_2}{T_2 - T_1} \ln \left( \frac{\dot{\varepsilon}_{cr,1}}{\dot{\varepsilon}_{cr,2}} \right)
\]

In case of the first method, the creep strains have to be determined by subtraction of the elastic and thermal strain from the total measured strain. This method is therefore

\(^2\) After being loaded in tension in the inelastic range, the proportional stress in compression is normally lower than determined in the tension test (which was not preceded by a test of opposite sign). This so-called Bauschinger effect is important in extruded sections, as these are stretched in the inelastic range after extrusion.
sensitive to measuring errors. The second method is less dependent on measuring errors and is therefore used in this research.

In chapter 7, creep tests were carried out with a stepwise varied temperature and a constant stress. Based on Figure 7-4, the value of the activation energy for alloy 5083-H111 was taken as the average slope of the curves and determined at:

Alloy 5083-H111: \( Q = 152000 \text{ J/mol} \)

In case of alloy 6060-T66, test series 2005, a large scatter was present in the relation between temperature and minimum strain rate when all tests are taken into account. The scatter was smaller when only the test results with a stepwise increased temperature and small strains are taken into account. For both cases, the activation energy was determined.

Alloy 6060-T66, series 2005, all creep test data: \( Q = 170000 \text{ J/mol} \)
Alloy 6060-T66, series 2005, selection of test data: \( Q = 195000 \text{ J/mol} \)

In case of alloy 6060-T66, series 2006, the scatter in test results was considerably smaller than in case of series 2005. This is either due to the fact that the LVDT was adapted, so that small strains could be determined with more accuracy, or it is due to the fact that the secondary strain rate depends on the thermal exposure period: in series 2006, \( Q \) was determined in tests with short periods at elevated temperature (up to 20 min). The activation energy for series 2006 was determined based on the curves in Figure 7-21.

Alloy 6060-T66, series 2006: \( Q = 190000 \text{ J/mol} \)

The value for the activation energy used for alloy 6060-T66 is 195000 J/mol. Table 9.1 gives an overview of the activation energy given in literature for aluminium alloys and for steel. When comparing this with the values given above, it appears that the activation energy for alloy 5083-H111 is close to the values in literature for other alloys in series 5xxx. The activation energy for alloy 6060-T66, however, is considerably higher than the values in literature for other alloys in series 6xxx. Note that the activation energy for alloy 6060-T66 by Oertel et al. [21] is much lower than the values given for other alloys.

<table>
<thead>
<tr>
<th>Material</th>
<th>( Q ) [kJ/mol]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pure commercial aluminium (Dorn [3])</td>
<td>151</td>
</tr>
<tr>
<td>Alloy with 1.6 % Mg (series 5xxx) (Dorn [3])</td>
<td>151</td>
</tr>
<tr>
<td>Alloy DTD 5070 A (Webb [33])</td>
<td>153</td>
</tr>
<tr>
<td>Alloy 6092 (Li and Langdon [13])</td>
<td>135</td>
</tr>
<tr>
<td>Alloy 6082 T6 (Rolstad [25])</td>
<td>120-150</td>
</tr>
<tr>
<td>Alloy 6082 T6 (Lundberg [14])</td>
<td>139</td>
</tr>
<tr>
<td>Alloy 6060-T66 (Oertel et al. [21])</td>
<td>39-65</td>
</tr>
<tr>
<td>Steel grade 2172 (Thor [31])</td>
<td>416</td>
</tr>
</tbody>
</table>
9.2.2 Zener-Holloman parameter

Using Dorn’s equation (2.3) and the value determined for the activation energy, the Zener-Holloman parameter (Z) can be determined for each creep test. Figure 9-1 gives Z (on logarithmic scale) as a function of the stress for alloy 5083-H111.

The data resulting from different tests show a consistent relation between Z and σ. Analytical expressions for Z are obtained by determining the parameters of equation (2.8). Also, the parameters for the set of equations (2.6) and (2.7) are determined. The expressions are according to (9.6) and (9.7) are represented by curves in Figure 9-1.

Alloy 5083-H111 (Q = 152000 J/mol):

Z-equation I: \[ Z = 6.7 \cdot 10^{10} \cdot \left( \sinh \left( 0.025 \cdot \sigma \right) \right)^3 \] [/min] (9.6)

Z-equation II: \[ Z = \begin{cases} 3.27 \cdot 10^7 \cdot \sigma^{3.3} & \text{if } \sigma \leq 35 \text{ N/mm}^2 \\ 3.62 \cdot 10^{11} \cdot e^{(0.0773 \cdot \sigma)} & \text{if } \sigma > 35 \text{ N/mm}^2 \end{cases} [/min] (9.7)

The figures show that both sets of analytical expressions are able to describe the Z-function. In the remaining of this document, equation (9.6) is used because it is nowadays commonly applied and it requires less material-dependent parameters.
In Figure 9-2 and Figure 9-3, the Zener-Holloman parameter is given as a function of the stress for alloy 6060-T66, series 2005. There is a large scatter in the relation between $Z$ and $\sigma$ in case all test data are taken into account (Figure 9-2). If only the test data for small strains are taken into account (Figure 9-3), this scatter reduces significantly.

![Figure 9-2](image)

Figure 9-2 – Relation between log $Z$ and $\sigma$ for alloy 6060-T66, series 2005 (all test results)

![Figure 9-3](image)

Figure 9-3 - Relation between log $Z$ and $\sigma$ for alloy 6060-T66, series 2005 (only results for small strains)
The relation between $Z$ and $\sigma$ for the stress range covered by the creep tests on alloy 6060-T66, series 2005, is approximately linear. This implies that, when relation (2.8) is applied, there is one parameter too much, so that various combinations of parameters can be set to determine the relation between $Z$ and $\sigma$ for the stress range indicated in Figure 9-2 and Figure 9-3. Arbitrary values for the parameters with which the relation is described is used in the remaining of this document.

In case all test results are taken into account, equations (9.8) and (9.9) are determined for the Zener-Holloman parameter. The equations are represented by curves in Figure 9-2.

Model 1 Alloy 6060-T66, series 2005 ($Q = 170000 \text{ J/mol}$):

\[
Z\text{-equation 1: } Z = 2.5 \times 10^{10} \cdot \left( \sinh \left( 0.067 \cdot \sigma \right) \right)^{1.6} \quad \text{[min]} \quad (9.8)
\]

\[
Z\text{-equation 2: } Z = 4.43 \times 10^{11} \cdot e^{(0.1074 \cdot \sigma)} \quad \text{if } \sigma > 30 \text{ N/mm}^2 \quad \text{[min]} \quad (9.9)
\]

In case only the test results for small strains are taken into account, equations (9.10) and (9.11) are determined for the Zener-Holloman parameter. The equations are represented by curves in Figure 9-3.

Model 2 Alloy 6060-T66, series 2005 ($Q = 195000 \text{ J/mol}$):

\[
Z\text{-equation 1: } Z = 7 \times 10^{12} \cdot \left( \sinh \left( 0.04 \cdot \sigma \right) \right)^3 \quad \text{[min]} \quad (9.10)
\]

\[
Z\text{-equation 2: } Z = 4.43 \times 10^{11} \cdot e^{(0.1074 \cdot \sigma)} \quad \text{if } \sigma > 30 \text{ N/mm}^2 \quad \text{[min]} \quad (9.11)
\]

The figures show that both analytical expressions for $Z$ are suited. In accordance with the model for alloy 5083-H111, equations (9.8) respectively (9.10) are used for $Z$.

Although the values for the material dependent parameters of both models (i.e. taking into account all test data or only test data for small strains) are different, it appears that both models result in almost equal creep strains for the temperature and stress range covered in the research. In the remaining of this document, model 2, taking into account test data for small strains only, is applied. The reason is that the scatter in test results is much smaller in this case.

The relation between $Z$ and $\sigma$ for alloy 6060-T66, series 2006, is given in Figure 9-4. The test results are shown for all tests in which the test temperature and load were applied directly. There is a consistent behaviour between the Zener-Holloman parameter and the stress. The figure indicates that the relation between (log $Z$) and $\sigma$ is nonlinear for high stress levels. This indicates that it is not possible to describe the relation between $Z$ and $\sigma$ with equation (2.8) or the set of equations (2.6) and (2.7).

For stress levels lower than $\sigma = 100 \text{ N/mm}^2$, equation (2.8) is proposed, while for larger stresses, a modification of equation (2.8) is proposed:

Model Alloy 6060-T66, series 2006 ($Q = 170000 \text{ J/mol}$):

\[
Z = \begin{cases} 
1 \times 10^{12} \cdot \left( \sinh \left( 0.019 \cdot \sigma \right) \right)^{3.3} & \text{if } \sigma \leq 100 \text{ N/mm}^2 \\
3 \times 10^{13} \cdot \sinh \left( 5.1 \cdot 10^{-11} \cdot \left( \sigma \right)^{5.2} \right) & \text{if } \sigma > 100 \text{ N/mm}^2 
\end{cases} \quad \text{[min]} \quad (9.12)
\]
The equation is represented with a black curve in Figure 9-4.

Figure 9-4 – Relation between log $Z$ and $\sigma$ for alloy 6060-T66, series 2006

Figure 9-5 gives the results of creep tests that were heated to test temperature in 5 seconds and loaded directly after the test temperature was obtained. These tests results are indicated with black circles. A limited number of additional tests were conducted with different heating procedures:
- Grey diamonds represent tests in which the temperature was kept constant for 90 minutes before loading;
- Grey stars represent tests which were heated from room to test temperature in 90 minutes before loading.

As already observed in paragraph 7.3.2, the thermal exposure period influences the strain rate at high temperatures, but for the temperature histories relevant for fire, the influence of the thermal exposure period is negligible, so that it does not have to be explicitly taken into account in the material model.

The relations for $Q$ and $Z$ of series 2006 are based on creep tests exposed to an elevated temperature during 10 to 20 minutes. It is expected that this period is so short, that it does not yet significantly influence the strength. This assumption will be checked by comparing the strain development in a transient state test with the strain according to the model (paragraph 9.6).
9.3 Primary creep strain function

With curve fitting, the parameters in relation (2.10), for the description of $\varepsilon_{t0}$ are determined using the data of the tests tabulated in Voorhees and Freeman for alloy 5083-H111 (Figure 7-7) and the data of the current test series for alloy 6060-T66 (Figure 7-26). The relations according to equations (9.13) and (9.14) are indicated with black lines in Figure 7-7 and Figure 7-26, respectively.

Alloy 5083-H111: 

$$\varepsilon_{t0} = 3.94 \cdot 10^{-10} \cdot \sigma^{3.4}$$  \hspace{1cm} (9.13)

Alloy 6060-T66, series 2006: 

$$\varepsilon_{t0} = 2 \cdot 10^{-18} \cdot \sigma^{7.45}$$  \hspace{1cm} (9.14)

The strain in series 2005 of alloy 6060-T66 was not determined accurately enough to find an accurate relation between $\varepsilon_{t0}$ and $\sigma$. In the evaluation of the material model, the same relation is applied as for series 2006 of alloy 6060-T66.

In the simulations of the tests carried out on alloy 6060-T66, it appeared that the equation proposed by Harmathy (2.10) does not agree well with the measured creep strain. Harmathy noted that this function was not selected because of agreement with test results, but because it allows for an explicit function for the creep strain. However, after simulating the creep and transient state tests, it appeared that the error caused by scatter in $\varepsilon_{t0}$ is more important than the disagreement in the description of primary creep. Moreover, in transient state tests, the influence of the description of primary creep on the resulting strain in time is small.
Figure 9-6 – Comparison between measured primary creep strain and Harmathy’s equation

The results of simulations of the creep tests with equation (9.16) were not significantly more accurate than simulations with equation (9.15). This is mainly attributed to the large scatter in data for $\varepsilon_{t0}$, so that equations 9.13 and 9.14 deviate from the measured value of $\varepsilon_{t0}$ in a number of tests. A good agreement between the measured strain and the calculated strain depends more on a proper value for $\varepsilon_{t0}$ than on the description of the creep curve for primary creep (equations 9.15 and 9.16).

9.4 Extension of the model with tertiary creep

The creep tests on alloy 6060-T66 showed that creep deformation is dominated by tertiary creep. Therefore, the Dorn-Harmathy model is extended in the current research for the first stage of tertiary creep.

In paragraphs 7.2.3 and 7.3.4, it was determined that, in the first stage of tertiary creep, there exists a relation between the strain rate and the strain. In all tests showing tertiary creep, the relation could be represented by a straight line starting at the origin of the plot of the strain rate against the strain. The creep model can thus be extended in a relatively simple way with the first stage of tertiary creep:

$$\varepsilon_t < \varepsilon_{lim}: \quad \dot{\varepsilon}_t = \dot{\varepsilon}_{t,1+II}$$
$$\varepsilon_t > \varepsilon_{lim}: \quad \dot{\varepsilon}_t = \dot{\varepsilon}_{t,1+II} \cdot C \cdot \varepsilon_t$$

In which $\varepsilon_{lim}$ represents the strain at which tertiary creep starts. Based on the creep tests, this model is applicable for alloy 6060-T66 for strains up to approximately 2%. It was not investigated whether this relation also applies for other alloys and for larger strains.

As the strain rate at the start of the tertiary stage should be equal to the secondary strain rate, the following relation yields for $C$:

$$C = \frac{1}{\varepsilon_{lim}}$$
The problem is now reduced to finding the strain $\varepsilon_{\text{lim}}$ or, alternatively, the tangent $C$ at which the tertiary stage starts. The values were evaluated for each test in three ways:

- Determination of the strain at which the tertiary creep stage starts, i.e. finding $\varepsilon_{\text{lim}}$;
- Determination of the tangent of the strain vs. strain rate curves, and calculate $C$ using equation (9.19). $\dot{\varepsilon}_{1,1+II}$ in this equation was taken as the strain rate measured during the test;
- Determination of the tangent in a similar way as described above, however, $\dot{\varepsilon}_{1,1+II}$ was not taken as the measured, but as the calculated value using the Dorn-Harmathy creep model.

The resulting values for $\varepsilon_{\text{lim}}$ or $C$ were different among the different tests. Also the different ways of evaluating $\varepsilon_{\text{lim}}$ or $C$ resulted in different values. However, the average value of all tests was approximately equal for the three types of evaluation. This average value was $\varepsilon_{\text{lim}} = 0.0025$ for series 2005 and $\varepsilon_{\text{lim}} = 0.002$ for series 2006. These values were applied in the model.

### 9.5 Model check: simulation of creep tensile tests

The parameters of the Dorn-Harmathy model are determined based on the strains in the creep tensile tests carried out. A first check of the models is to simulate the creep tensile tests.

#### 9.5.1 Alloy 5083-H111

Some results of simulated creep tensile tests on alloy 5083-H111 are given in Figure 9-7 for a creep test with stepwise varied temperature and in Figure 9-8 for a stepwise varied stress. Simulations of all creep tests are shown in Annex D. In general, there is a reasonable agreement between the simulated strain and the measured strain.
Figure 9-7 – Calculated and measured strain (upper graph) and ratio between calculated and measured strain (lower graph) for a creep test with stepwise increased temperature and constant stress of 26 N/mm² on alloy 5083-H111.
9.5.2 Alloy 6060-T66, series 2005

Results of simulated creep tensile tests on alloy 6060-T66 are given in Figure 9-9 for a creep test with stepwise varied temperature and in Figure 9-10 for a stepwise varied stress. The agreement between measured and simulated strains as presented in these graphs is representative for all creep tests carried out on this alloy. Simulations of all creep tests are shown in Annex D. In general, the agreement between the simulated strain and the measured strain is worse than in case of alloy 5083-H111. This is attributed to the fact that the scatter in test data was relatively large for series 2005 of this alloy.
9.5.3 Alloy 6060-T66, series 2006

In case of test series 2006 of alloy 6060-T66, the agreement between measured and simulated strains is in general good for low stress levels (and high temperatures). At high stress levels (and low temperatures), the secondary strain rate agrees reasonable, while the agreement in primary creep is worse. This is mainly attributed to scatter in test data on $\varepsilon_{0\theta}$, resulting in an inaccurate model description of primary creep.
As an example, Figure 9-11 and Figure 9-12 give the results of simulated creep tensile tests with a low and a high stress level, respectively.

Figure 9-11 – Calculated and measured strain (upper graph) and ratio between calculated and measured strain (lower graph) for a creep test with stepwise increased temperature and constant stress of 50 N/mm² on alloy 6060-T66, series 2006.
9.5.4 Creep tests in tension and subsequently compression

In two creep tests, a tensile load was applied during a certain period, and subsequently a compression load was applied. The strain is modelled appropriately, provided that the value for the creep strain in the equation for the primary creep strain rate starts at zero when the stress changes from tension to compression (or vice versa). This is indicated with symbol $\varepsilon_t^*$.
\[
\dot{\varepsilon}_t = \dot{\varepsilon}_{t,II} \cdot \coth^2\left(\frac{\varepsilon_t^*}{\varepsilon_{t0}}\right) \quad (9.18)
\]

\[
\varepsilon_t < \varepsilon_{lim} : \quad \dot{\varepsilon}_t = \dot{\varepsilon}_{t,II} \cdot \coth^2\left(\frac{\varepsilon_t^*}{\varepsilon_{t0}}\right) \quad (9.19)
\]

\[
\varepsilon_t > \varepsilon_{lim} : \quad \dot{\varepsilon}_t = \dot{\varepsilon}_{t,II} \cdot \coth^2\left(\frac{\varepsilon_t^*}{\varepsilon_{t0}}\right) \cdot \frac{\varepsilon_t}{\varepsilon_{lim}} \quad (9.20)
\]

In which \( \varepsilon_t^* = \int_0^t \dot{\varepsilon}_t \, dt \), but starts again at 0 when stress changes from sign.

An example of the simulation of a creep test loaded in tension and subsequently in compression is given in Figure 9-13.

![Graph](image.png)

**Figure 9-13** – Calculated and measured strain for a creep test with constant temperature of 229 °C and stress levels of +104 and -104 N/mm²

### 9.6 Model check: simulation of transient state tests

In this paragraph, the Dorn-Harmathy models are used to simulate the transient state tests carried out.

#### 9.6.1 Alloy 5083-H111

The measured creep strains in the transient state tests with linear increasing temperature and constant stress and the simulation of the creep strains with the Dorn-Harmathy creep model are given in Figure 9-17 up to Figure 9-18.

There is a good correspondence between the measured and the calculated strain, indicating that the strain development of transient state situations is simulated accurately with the Dorn-Harmathy creep model.
A difference between measured and calculated strain occurs for the test with the highest stress level, Figure 9-18. A possible reason is that there exists a large scatter in test values for $\varepsilon_{t0}$, so that the function for this parameter is inaccurate. $\varepsilon_{t0}$ only has a significant influence on the results in case of large stress levels.

In case of some of the tests, there is a small difference between the measured and calculated strain at low strain levels. A possible reason is that the thermal strain in this area is several orders larger than the mechanical strain, which may cause a significant measuring error.

![Graph](image1)

Figure 9-14 - Simulation of a transient state test on alloy 5083-H111, $\sigma = 20 \text{ N/mm}^2$

![Graph](image2)

Figure 9-15 – Simulation of transient state tests on alloy 5083-H111, $\sigma = 41 \text{ N/mm}^2$
Figure 9-16 – Simulation of a transient state test on alloy 5083-H111, $\sigma = 60$ N/mm$^2$

Figure 9-17 – Simulation of transient state tests on alloy 5083-H111, $\sigma = 70$ N/mm$^2$
Figure 9-18 – Simulation of a transient state test on alloy 5083-H111, $\sigma = 101$ N/mm$^2$.

Figure 9-19 and Figure 9-20 give the results of the simulations of the transient state tests with a constant heating rate and respectively an increasing and decreasing stress (see Figure 8-8 and Figure 8-9 for loading).

For both tests, there is a good agreement between the measured and simulated strain.

Figure 9-19 – Simulation of transient state test of alloy 5083-H111 with an increasing stress level (rate 2 N/mm$^2$/min) and a heating rate of 9.2 ºC/min.
9.6.2 Alloy 6060-T66, series 2005

The creep development in the transient state tests on series 2005 of alloy 6060-T66 was simulated with the Dorn-Harmathy creep model including the addition of tertiary creep. For series 2005, a function for $\varepsilon_{\text{t0}}$ was not determined. In the simulations, the function found for test series 2006 was applied. The results of the simulations of the tests are given in Figure 9-21 up to Figure 9-24.

There is a good agreement between the measured and calculated creep strain. In some tests, the measured strain does not agree well with the simulated strain for low strain levels. This is attributed to measurement errors: such small mechanical strains are only a fraction of the thermal strain.
Figure 9-21 – Simulation of transient state tests on alloy 6060-T66, series 2005, with $\sigma = 50 \text{ N/mm}^2$.

Figure 9-22 – Simulation of transient state tests on alloy 6060-T66, series 2005, with $\sigma = 76 \text{ N/mm}^2$. 
9.6.3 Alloy 6060-T66, series 2006

The simulations of the mechanical strain of the transient state tests with constant stress on series 2006 of alloy 6060-T66 are given in Figure 9-25 up to Figure 9-28. The agreement between tests and simulations is good, except for the tests carried out with the highest stress level ($\sigma = 126$ N/mm$^2$, Figure 9-28) for large strains. The results of
these tests show an unexpected strain development: the test with the lowest heating rate has a lower strain than the test with the highest heating rate. Based on this, it is expected that the disagreement between tests and simulations is due to inaccurate tests and not due to an inaccurate material model.

Figure 9-25 – Simulation of transient state tests on alloy 6060-T66, series 2006, with $\sigma = 40 \text{ N/mm}^2$

Figure 9-26 – Simulation of a transient state test on alloy 6060-T66, series 2006, with $\sigma = 76 \text{ N/mm}^2$
Figure 9-27 – Simulation of transient state tests on alloy 6060-T66, series 2006, with $\sigma = 93$ N/mm$^2$.

Figure 9-28 – Simulation of transient state tests on alloy 6060-T66, series 2006, with $\sigma = 126$ N/mm$^2$.

The results of transient state tests with a varying stress in time are given in Figure 9-29 and Figure 9-30.
6060-T66, stress rate = 1,67 N/mm²/min

Figure 9-29 – Simulation of transient state tests on alloy 6060-T66, series 2006, with a stress rate of 1,67 N/mm²/min

6060-T66, stress = 100 N/mm² - 1,67 N/mm²/min

Figure 9-30 – Simulation of transient state tests on alloy 6060-T66, series 2006, with $\sigma = 100 \cdot 1,67$ N/mm²/min

The model parameters were based on creep tests with a thermal exposure period of 15 to 20 min at constant, elevated temperature. The temperature as a function of time is different in the transient state tests. Nonetheless, the simulation of the transient state tests with the material model agrees well with the measurements. This may indicate that the temperature influence on the temper is approximately equal for the two different temperature-time relations.

9.7 Model check: simulation of steady-state tensile tests

The models are used to simulate the steady-state state tests carried out.
It should be noted that in simulations of steady-state tensile tests on steel, Harmathy [8] found that the results are unsatisfactory for the initial portion of the stress-strain curve. This was attributed to the fact that \( \frac{\sigma}{dt} \) is large, while in conventional creep tests this value is 0. However, near the ultimate tensile strength \( (f_u) \), \( \frac{\sigma}{dt} \approx 0 \), so that the creep model may be applicable for simulation of \( f_u \). Harmathy found that the simulated value for \( f_u \) corresponds reasonably with the measured value on steel specimens (ratio \( f_u,\text{test} / f_u,\text{simulation} = 0.75 \) to 0.9).

This paragraph gives the comparison between the measured and the simulated values of the ultimate tensile strength of the steady-state tests described in chapter 5. The measured specimen temperature and the strain rate measured with the strain gages were used as input parameters for the simulation. Output of the simulation is the stress.

Figure 9-31 gives \( f_u,\text{test} \) and \( f_u,\text{simulation} \) for alloy 5083-H111. Figure 9-32 gives the ratio \( f_u,\text{simulation} / f_u,\text{test} \) for this alloy. There is a good agreement between measured and simulated values of the ultimate tensile strength (ratio \( f_u,\text{simulation} / f_u,\text{test} = 0.91 \) to 1.07).

![Figure 9-31](image1.png)

Figure 9-31 – Ultimate tensile strength as a function of the temperature of alloy 5083-H111, according to steady-state tests and according to simulation with model

![Figure 9-32](image2.png)

Figure 9-32 – Ratio between simulated and measured value of ultimate tensile strength as a function of the temperature of alloy 5083-H111
In case of alloy 6060, simulations were made with model parameters based on creep test series 2005.

Figure 9-33 and Figure 9-35 give $f_{u,test}$ and $f_{u,simulation}$ for the square hollow section tests and the angle tests of alloy 6060-T66, respectively. Figure 9-34 and Figure 9-36 give the corresponding ratios $f_{u,simulation} / f_{u,test}$. There is a worse agreement between measured and simulated values of the ultimate tensile strength as in case of alloy 5083-H111 (ratio $f_{u,simulation} / f_{u,test} = 1.05$ to $1.42$). One reason for the fact that the simulated strain is larger than the measured strain, especially at higher temperatures, is that the material model is based on tests with a constant elevated temperature for approximately 15 minutes, while the period with constant temperature in the steady-state tensile tests was longer (approximately 40 minutes at $f_u$). This might have influenced the temper. Another reason for the difference between the measured and simulated strain is that the strain measurement with the strain gage was inaccurate, so that the strain rate applied as input parameter in the material model is inaccurate.

![Figure 9-33](image_url)

**Figure 9-33 – Ultimate tensile strength as a function of the temperature of extruded square hollow sections of alloy 6060-T66, according to steady-state tests and according to simulation with model**

![Figure 9-34](image_url)

**Figure 9-34 – Ratio between simulated and measured value of ultimate tensile strength as a function of the temperature of extruded square hollow sections of alloy 6060-T66**
Chapter conclusions

The material-dependent parameters in an analytical creep model (Dorn-Harmathy creep model) were determined based on the creep tests. The analytical model showed a consistent behaviour with the tests on alloy 5083-H111. The analytical model for this alloy is described with equations (9.21) up to (9.23). For alloy 6060-T66, there was a larger scatter in results of the creep tests. Nonetheless, the creep model was also applied for this alloy. Equations (9.24) up to (9.26) are proposed.

Alloy 5083-H111:

\[
\dot{\varepsilon}_t = Z \cdot e^{-\frac{Q}{RT}} \cdot \coth^2 \left( \frac{\varepsilon_t}{\varepsilon_{t0}} \right) \quad [\text{min}^{-1}]
\]  

(9.21)
\[ Z = 2,67 \cdot 10^{10} \cdot (\sinh (0,025 \cdot \sigma))^3 \text{ [min]} \tag{9.22} \]

\[ \varepsilon_{\text{f0}} = 3,94 \cdot 10^{-10} \cdot \sigma^{3.4} \tag{9.23} \]

\[ Q = 152000 \text{ [J/mol]} \]

Alloy 6060-T66:

\[ \varepsilon < \varepsilon_{\text{lim}}: \quad \dot{\varepsilon} = Z \cdot e^{-\frac{Q}{RT}} \cdot \coth^2 \left( \frac{\varepsilon}{\varepsilon_{\text{f0}}} \right) \text{ [min]} \tag{9.24} \]

\[ \varepsilon > \varepsilon_{\text{lim}}: \quad \dot{\varepsilon} = Z \cdot e^{-\frac{Q}{RT}} \cdot \coth^2 \left( \frac{\varepsilon}{\varepsilon_{\text{f0}}} \right) \frac{\varepsilon}{\varepsilon_{\text{lim}}} \text{ [min]} \tag{9.25} \]

With parameters for batch 2005:

\[ Z = 7,0 \cdot 10^{12} \cdot (\sinh (0,04 \cdot \sigma))^3 \text{ [min]} \tag{9.26} \]

\[ \varepsilon_{\text{f0}} = 2 \cdot 10^{-18} \cdot \sigma^{7.45} \tag{9.27} \]

\[ Q = 195000 \text{ [J/mol]}, \varepsilon_{\text{lim}} = 0,002 + \varepsilon_{\text{f0}} \]

With parameters for batch 2006:

\[ Z = 1,0 \cdot 10^{12} \cdot (\sinh (0,019 \cdot \sigma))^3 \text{ [min]} \tag{9.28} \]

\[ \varepsilon_{\text{f0}} = 2 \cdot 10^{-18} \cdot \sigma^{7.45} \tag{9.29} \]

\[ Q = 170000 \text{ [J/mol]}, \varepsilon_{\text{lim}} = 0,002 + \varepsilon_{\text{f0}} \]

With these creep models, the transient state tests of chapter 8 were simulated. There was a good agreement between the creep strains developed during the tests and the simulation of the creep strain. These transient state tests are a good representation of fire exposure. Hence, with the analytical creep model it is possible to simulate the material properties in case of fire.

Also steady state tests were simulated with these models. In case of alloy 5083-H111 there was a good agreement between the tests and the simulations in the temperature range which applies to the model. In case of alloy 6060-T66, the agreement was worse, but this is attributed to the fact that the thermal exposure period, prior to loading, of the creep / transient state tests did not agree with the steady state tests.
10 Stress-strain relations of alloys 5083-H111 and 6060-T66 in fire

Using the Dorn-Harmathy material model from chapter 9, the stress-strain relations in fire can be determined for the alloys considered. Paragraph 10.1 gives the derived stress-strain relations for a constant heating rate. In some cases, a constant heating rate is not appropriate. In such a case, numerical simulations with the model are possible. The numerical procedure that is applied in DIANA is elaborated in Annex Error! Reference source not found.. Paragraph 10.3 gives information on the cases when a linear heating rate is a reasonable assumption in fire design.

10.1 Stress strain relations for a constant heating rate

The Dorn-Harmathy model is used to determine the stress-strain relations of fire exposed aluminium with a constant heating rate and a constant stress during heating. The procedure is explained in Figure 10-1 and Figure 10-2.

Transient state simulations were carried out at different stress levels for a linear heating curve, reaching 200 ºC in 30 minutes (Figure 10-1). The resulting strain curves can be plotted with the plastic strain on the horizontal axis. The stress used in each simulation can be plotted as a function of the strain at the end of the simulation (30 minutes). The resulting graph gives the stress-strain curve for the specified heating curve (linear heating in 30 minutes to 200 ºC) and a constant stress level. This stress-strain relation based on transient state simulations is different from the result of a steady state test, because the heating and loading regime are different, and therefore the influence of creep is different.

A similar procedure was followed to determine the stress-strain relations of fire exposed steel (Annex A). Instead of simulations, the strain development was based on tests in case of steel.

Note that at this moment, the Eurocode for fire design of aluminium structures gives stress-strain relations based on steady-state tests.

![Figure 10-1 – Simulations of transient state tensile tests of alloy 5083-H111 at a heating rate of 6 ºC / min](image-url)
Figure 10-2 – Stress-strain relation based on transient state simulations for alloy 5083-H111 after 30 minutes with a heating rate of 6 °C / min

Simulations as shown above were carried out at various heating rates, each curve determined for a constant stress level and a constant heating rate.

The resulting stress-strain relations, including elastic strain, are given in Figure 10-3, Figure 10-4 and Figure 10-5 for alloys 5083-H111, 6060-T66 series 2005 and 6060-T66 series 2006, respectively.

The temperatures for which the curves are valid, are indicated in the graphs. Different curves for one temperature indicate that the temperature is reached in 30, 60, 90 or 120 min. These times correspond to the fire resistance classes set in national regulations for different buildings.
Figure 10-3 – Stress strain relations of alloy 5083-H111 after exposure with constant heating rate and constant stress level

Figure 10-4 – Stress strain relations of alloy 6060-T66, series 2005, after exposure with constant heating rate and constant stress level
The figures show that the stress-strain relations for the different fire resistance classes are different. In paragraph 10.3, it is determined what the effect of these different curves is on the fire resistance.

10.2 Comparison between steady state and transient state strength

The relative values for the 0.2 % proof stress ($k_{0.2,0}$) in EN 1999-1-2 are based on steady-state tests, which have to be multiplied with the 0.2 % proof stress at room temperature according to EN 1999-1-1 ($f_{0.2,room}$) in order to obtain the 0.2 % proof stress for fire design ($f_{0.2,0} = k_{0.2,0} \cdot f_{0.2,room}$). Figure 10-6 gives a comparison between the 0.2 % proof stress according to steady-state tests carried out in this research on alloy 5083-H111, the values according to EC9 and the values according to the simulation of transient state tests as shown above. Figure 10-7 gives the results of the ultimate tensile strength according to steady-state tensile tests and simulations of transient state tests. (EN 1999-1-2 does not specify reduction factors for the ultimate tensile strength.)
Figure 10-6 – Steady-state and transient state values of $f_{0.2}$ as a function of the temperature for alloy 5083-H111

Figure 10-7 – Steady-state and transient state values of $f_u$ as a function of the temperature for alloy 5083-H111

Figure 10-6 shows that there is a large difference between the steady-state and the transient-state value for $f_{0.2}$, especially for the most relevant temperatures (200 – 300 °C). Also the values in EN 1999-1-2 are much larger than the values according to transient state tests. In background documents [15], [16], Lundberg explains that the values for $f_{0.2}$ at room temperature in EN 1999-1-1 are so conservative, that it is assumed that the influence of creep on the material properties may be neglected when multiplying the 0.2 % proof stress at room temperature with the relative values in EN 1999-1-2. Figure 10-6 shows that this assumption is apparently incorrect for alloy 5083-H111.

Note that EN 1999-1-2 specifies that the flexural buckling strength of a column must be divided by an additional creep parameter with a value of 1.2.

Figure 10-7 shows that the difference in the steady state and transient state values for $f_u$ is much smaller as in case of the 0.2 % proof stress.
It is noted again that the values for the strength in steady state test depend on the strain rate applied. Other strain rates may result in other differences between steady-state and transient state values for the strength.

EN 1999-1-2 does not specify relative values for $f_{0,2}$ of alloy 6060-T66.

10.3 Applicability of constant heating rate

The resulting stress-strain curves given in the previous paragraph are valid for a linear heating curve and constant stress levels. These simplifications are not always justified:

- In a fire exposed member that is part of a structure, the stress is in many cases not constant due to restrained thermal expansion and/or due to redistribution of forces when parts of the structure are weakened by the fire. Both situations occur in statically indeterminate structures. Note that in EN 1999-1-2, when individual members are analysed, the boundary conditions at the supports and ends of a member may be assumed to remain unchanged throughout the fire exposure and that the effects of axial thermal expansions may be neglected. This implicates that the stress may be assumed to be constant when individual members are analysed;

- The heating rate is not necessarily constant. This paragraph gives some examples of heating rates of fire exposed members and their impact on the stress-strain relations.

In the study on heating of aluminium members [18] it was noted that structural aluminium members need to be insulated in case of a fire design based on the standard fire. In case of a fire design based on a natural fire safety concept, aluminium structural members need to be insulated in almost all cases. Therefore, insulated members are studied in this chapter.

The temperature development in a member depends on the thermal properties of the insulation layer and on the section factor. Three different insulation materials with arbitrary but realistic thermal properties according to Figure 10-8 are investigated. Two section factors are considered, $A_m / V = 136 \text{ m}^{-1}$ (‘normal’ section) and $A_m / V = 1020 \text{ m}^{-1}$ (thin-walled section, sensitive to local buckling). The thicknesses of the insulation materials are chosen such as to result in a member temperature of 200 °C and 300 °C after 30 minutes and 120 minutes exposure to the standard fire.

Figure 10-9 gives the heating curves of the sections with the two section factors insulated with materials 2 and 3. It follows that the stocky section with insulation material 2 is closest to the curve for linear heating, while the slender section with insulation material 3 gives the largest deviation from the linear heating curve.
Figure 10-8 – Thermal properties of insulation layers considered

Figure 10-9 – Temperature-time relations of insulated aluminium exposed to the standard fire, temperature at end of exposure is 300 ºC
a. \( \frac{A_m}{V} = 1020 \text{ m}^{-1} \), fire resistance 30 minutes
b. \( \frac{A_m}{V} = 1020 \text{ m}^{-1} \), fire resistance 120 minutes
c. \( \frac{A_m}{V} = 136 \text{ m}^{-1} \), fire resistance 30 minutes
d. \( \frac{A_m}{V} = 136 \text{ m}^{-1} \), fire resistance 120 minutes
All curves in Figure 10-9 result in a maximum member temperature of 300 °C after the fire resistance period, but each curve has a different heating rate and therefore a different strength. As an illustration, Figure 10-10 and Figure 10-11 give the stress-strain curves of alloys 5083-H111 and 6060-T66, respectively, for a maximum member temperature of 200 °C (figures a.) and 300 °C (figures b.). The dotted curve gives the stress-strain relation for linear heating in 60 minutes. The grey curves give the stress-strain relations of insulated members exposed to the standard fire, with properties selected such as to result in the highest and lowest strength of all sections considered. The highest strength (light grey curve) was obtained for the slender member \((A_m/V = 1020/m)\) with a fire resistance of 30 minutes, insulated with material 3 while the lowest strength (dark grey curve) was obtained for the stocky member \((A_m/V = 136/m)\) with a fire resistance of 120 minutes, insulated with material 2.

Figure 10-10 – Stress-strain relationships of insulated members of alloy 5083-H111 and of this alloy exposed to linear heating during 60 minutes
a. Maximum member temperature of 200 °C
b. Maximum member temperature of 300 °C
The figures shown that, especially in case of high temperatures (300 °C), there is a considerable difference in strength between the (highest and lowest) stress-strain relations. However, for the simple calculation models in EN 1999-1-2, it would be advantageous if the stress-strain relations to be applied in the design could be specified which depend on the temperature only, and not on the heating rate. For this reason, it is determined what the difference in fire resistance would be if the stress-strain relation for linear heating in 60 minutes (called hereafter ‘approximated curve’) would be applied for all cases considered and for temperatures up to 350 °C.

If only the strength is decisive for the fire resistance, using the approximated curve instead of the highest stress strain relation would underestimate the fire resistance with 2 minutes maximum for heating in 30 minutes, which is equivalent with 7 %. Using the approximated curve instead of the lowest stress strain relation would overestimate the fire resistance with 4 minutes maximum for heating in 120 minutes, which is equivalent to 3 %.

It is concluded that, despite the relatively large influence of the heating rate on the strength, it is appropriate to provide data for the strength that are independent of the fire resistance class, the section factor and the insulation material.

In case of a natural fire safety concept, the temperature during heating of (insulated) members may exceed the temperature of the linear heating curve. As an example, Figure 10-12 gives the heating of an insulated member exposed to a natural fire for a specific office. The different curves give the member temperature for a certain insulation layer with different thicknesses. One of the curves is shown in Figure 10-13 together with the curve for a constant heating rate. Near the maximum temperature, where creep influence is largest, the member temperature is underestimated with the linear heating curve.

Also in case of a standard fire, the temperature of an insulation member may exceed the linear relation between temperature and time. This may be the case for insulation
materials based on the release of chemically bonded moisture (calcium silicate) or intumescent coating systems. Whether or not an approximation with a linear heating curve is justified in the fire design of such cases should be based on a reliability study. Such a study is beyond the scope of the current project.

For cases where a linear heating curve is not justified, or for cases with a varying stress in time (due to restrained thermal expansion or weakening of parts of the structure), a numerical procedure could be applied in order to determine the mechanical properties. For this case, the material model is implemented into DIANA (background report no. 8).

10.4 Chapter conclusions

The stress-strain relationships of fire exposed aluminium alloys, as based on transient state test simulations, depend on the heating rate. Nonetheless, using the stress-strain relationship for linear heating of 60 minutes to represent the mechanical properties of insulated members exposed to the standard fire for 30 up to 120 minutes, results in an
underestimation of the fire resistance of maximum 7 % (equal to 2 minutes for a fire resistance of 30 minutes) and an overestimation of the fire resistance of maximum 3 % (equal to 4 minutes for a fire resistance of 120 minutes). It is therefore justified to specify material properties independent of the fire resistance class, i.e. to use the stress-strain relation for linear heating in 60 minutes for insulated members exposed to the standard fire.

The values for the 0.2 % proof stress specified in EN 1999-1-2 are based on steady state tests. At least for alloy 5083-H111, these values are unsafe, because the transient state values of the 0.2 % proof stress are much lower than the values specified by the standard.
11 Conclusions

This report gives the results of tensile tests carried out at elevated temperature on alloys 5083-H111 and 6060-T66. The tests are carried out in order to determine the mechanical properties when exposed to fire. The following types of tests are carried out:
- Steady-state tensile tests with a constant strain rate;
- Creep tensile tests with a constant stress and temperature;
- Transient-state tensile tests with a linearly increasing temperature and a constant or linearly varied stress.

Additionally, bending tests were carried out to determine the modulus of elasticity. An analytical model was selected to represent the material properties at elevated temperature. This so-called Dorn Harmathy model was based on the creep tests, and the steady-state and transient-state tests were simulated with the model.

Based on the steady state tensile tests carried out in this research, the following conclusions were made:
- The tests are well reproducible, both at room and at elevated temperature;
- At elevated temperature, the strain rate with which a test is carried out has a significant influence on the tensile strength;
- The Poisson ratio of aluminium alloys remains approximately constant for moderately elevated temperature - 0.33 at room temperature to 0.35 at 250 ºC. From this temperature onwards, the Poisson ratio increases to approximately 0.45 at 350 ºC;
- The strengths at elevated temperature determined in the tests correspond reasonably with the strengths according to literature;
- The modulus of elasticity could not be determined accurately in tensile tests at elevated temperature, caused by the fact that elastic deformations are so small that measurements are dominated by thermal expansion of the specimen and measuring devices. In case of bending of a bar, the elastic deformations are larger. Therefore, the modulus of elasticity is based on bending tests instead of tensile tests.

Based on the steady state bending tests the following conclusions were made:
- The tests are well reproducible, both at room and at elevated temperature;
- The modulus of elasticity determined for alloy 6060-T66 is within 5 % of the values given in literature (Kaufman) for alloy 6063-T6;
- The values for the modulus of elasticity of alloy 5083-H111 at temperature higher than 250 ºC measured in the tests was 15 % higher than the values given in literature.

Based on the creep tensile tests, the following conclusions were drawn:
- The creep deformation of alloy 5083-H111 is dominated by secondary creep in the range of relevant creep strains;
- The creep deformation of alloy 6060-T66 is dominated by tertiary creep in the range of relevant creep strains;
- In the beginning of the tertiary creep stage of alloy 6060-T66, the strain is (approximately) homogeneous. In this part of the creep curve, there is an approximately linear relation between the creep strain rate and the creep strain;
- The relations between the temperature and the secondary creep strain rate and between the stress and the secondary creep strain rate were consistent with the equations given by Dorn;
In case of alloy 6060-T66, the creep strain rate depends on the thermal exposure period. This is attributed to overageing and annealing during the thermal exposure period. The strain rate in case of specimens heated by a fire prior to the creep test was approximately equal to the strain rate of specimens without thermal exposure before the creep tests;

- In one test (with a specific temperature and stress level) it was determined that the visco-elastic strain (i.e. reversible creep strain) is small compared to the visco-plastic strain. It is assumed that this is the case for the entire temperature range and stress range relevant for fire exposure. This is in agreement with statements in literature;

- In two tests (with a specific temperature and stress level), it was determined that the creep strain rate in compression is equal to that in tension. It is assumed that this is the case for the entire temperature range and stress range relevant for fire exposure.

Based on the transient state tensile tests, the following conclusions were drawn:

- For both alloys, a difference in heating rate has a small influence on the critical temperature. For the extreme values of the relevant range of heating rates, the difference in critical temperature was 10 to 30 ºC;

- In case of alloy 5083-H111, creep strains developed gradually during heating. Small creep strains already developed at temperatures far below the critical temperature;

- In case of alloy 6060-T66, the creep strains developing remained small up to a temperature just below the critical temperature. Near the critical temperature, the creep strain rate was large. In other words: creep is less dominant or important for this alloy.

Based on the evaluation of the Dorn-Harmathy model, the following conclusions were made:

- The relations between temperature or stress and the secondary creep strain on which Dorn’s equations is based are applied widely on aluminium alloys in literature;

- It was possible to accurately determine the creep material-dependent parameters based on the creep tests;

- For alloy 5083-H111, the simulations of the transient state tests resulted in strains that correspond well with the transient state tests. For alloy 6060-T66, the agreement was well for small creep strains (up to 0,004) but for larger strains, the material model underestimates the strain. Based on this, it is concluded that the model developed is suited to represent the mechanical properties of the alloys considered when exposed to fire;

- The values for the strength in Eurocode 9 part 1-2 are based on steady state tests. These values are unsafe for fire of (at least) alloy 5083-H111;

- Different heating rates result in different values for the 0,2 % proof stress. It is nevertheless appropriate to provide data for the strength that are independent of the fire resistance class, the section factor and the insulation material, in case of exposure to a standard fire.
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A  Stress-strain curves including creep derived for steel

Witteveen and Twilt [34] give the procedure followed to derive stress-strain curves for steel.

1. Transient state tests were carried out on small-scale columns with three different heating rates (slow heating, fast heating and an intermediate heating rate) and various stress levels. The test results showed that there was no significant difference in critical temperature (failure temperature) for the three heating rates. Based on this, it was concluded that, although creep occurs, the critical temperature is not affected significantly by differences in creep at the different heating rates applied. It is therefore possible to determine the stress-strain curves applying only one heating rate.

2. Transient state tensile tests were carried out with different stress levels (e.g. the five stress levels in the figure below). During heating, the strain is measured. Each test (with a specific load level) gives a different strain at a certain temperature. When plotting these load levels and strains in one graph, the stress-strain curve for this temperature is determined. The influence of creep is then incorporated in the stress-strain curve.

Figure A 1  – Procedure followed to determine the stress-strain relation of steel at elevated temperature
B  Measuring of stiffness applied in other researches

B.1  Kaufman, Voorhees and Freeman

A sketch of the measuring device applied by Kaufman is given in Figure B 1. A rod, attached to the upper part of the parallel length, runs through a tube which is attached to the lower part of the parallel length. The displacement difference between rod and tube is measured outside the furnace with an ordinary displacement indicator. A similar device was tried out in the current research, but was found inadequate because of a difference in temperature and thermal expansion of the tube and the bar. The difference with the device applied by Kaufman, however, is that the tube is cooled in the research of Kaufman.

Figure B 1 – Measuring device applied by Kaufman
B.2 Langhelle

Langhelle applied a water-cooled extensometer type Instron 2630-051 in the tensile tests (Figure B 2). With this extensometer strains up to 10 % could be determined.

The modulus of elasticity measured by Langhelle with this device do not entirely correspond to the values found in literature. The average value of the modulus of elasticity at room temperature was 71 GPa (ranging from 68 to 76 GPa) for alloy 6082, while in other references (Kaufman, Mennink) a value of 67 to 68 GPa was found. Besides, the scatter in test results was relatively large.

Figure B 2 – Water-cooled extensometer applied by Langhelle
C  Graphs of the steady state tensile tests

This Annex gives graphs of the temperature during heating and during the tensile tests and the stresses and strains determined in the tensile tests.
C.1 Alloy 5083-H111, plate material

C.1.1 Test P2 – 25 °C

![Graph 1](image1)

![Graph 2](image2)
C.2  Alloy 6060-T66, square hollow section
C.2.1 Test H1 – 25 °C

---

**Graph 1:**
- **Strain rate (strain gages)**: 0.002, 0.004, 0.006, 0.008, 0.01, 0.012, 0.014, 0.016, 0.018, 0.02
- **Stress (N/mm²)**: 0, 50, 100, 150, 200, 250
- **Legend:**
  - Actuator
  - Strain gages

---

**Graph 2:**
- **Stress [N/mm²]**: 0, 50, 100, 150, 200
- **Strain [-]:** 0, 0.005, 0.01, 0.015, 0.02
- **Legend:**
  - Long. str. gauges
  - LVDTs 1,5 mm
  - Transv. str. gauges
Mechanical properties at elevated temperature

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**Graph 1:**
- Stress vs. Strain for different measurement methods:
  - Actuator
  - Strain gauges
  - LVDTs 1.5 mm
  - LVDTs 10 mm

**Graph 2:**
- V vs. Stress for ELV and FPLV methods:
  - ELV
  - FPLV
Mechanical properties at elevated temperature

![Graph showing Stress vs. $E_s$ and $E_t$]

Test
D  Graphs of the creep tests

This Annex gives the graphs of the creep tests carried out. Each test is represented with two figures, the first figure giving the temperature and stress as a function of time and the second figure giving the resulting mechanical strain as a function of time. Both the strain resulting from the measurement and the strain resulting from the simulation with the material model are given.

D.1  Alloy 5083-H111

Figure D 1 – Stress, temperature and strain versus time of test no 00
Figure D 2 – Stress, temperature and strain versus time of test no 01
E  Graphs of the pilot transient state tensile tests

In the set-up developed for the steady-state tensile tests, some transient state tests were carried out with an increasing temperature in time. The LVDTs measuring the strain used in these tests are not accurate enough to determine small mechanical strains. The results of these tests are carried out in order to check the procedure. For reasons of completeness, they are indicated in this Annex. They are referred to as “pilot tests”. More information is given in Van Rosmalen [26]

E.1  Alloy 5083

Transient state tests were carried out on specimen of alloy 5083-H111. The parallel length of the specimen was 80 mm, the width was 50 mm and the thickness 5 mm. The applied load was equal to the ultimate tensile strength at 300 ºC, measured in steady-state tensile tests (paragraph 5.2.1). This stress level is 79 N/mm². However, because of the different heating curves applied to the steady-state tensile test and the transient-state tests, the results of these tests cannot be compared.

Two heating rates were applied, resulting in heating to rupture in 35 and 55 minutes. Heating curves and the developing strain are shown in Error! Reference source not found..

![Graph of Temperature vs. Time for Alloy 5083](image)

The different heating rates applied gave approximately equal strain development and rupture stress.

E.2  Alloy 6060-T66

Transient state tests were carried out with on tensile test specimen of alloy 6060-T66. The parallel length of the specimen was 80 mm, the width was 50 mm and the thickness 5 mm. The applied stress was 57 N/mm². Two heating rates were applied, resulting in heating to rupture in 35 and 55 minutes.
The low heating rate resulted in equal strain development at a temperature that was, approximately, only 10 ºC lower. Also creep rupture took place at a 10 ºC lower temperature (Error! Reference source not found.).

Figure E 2 – Results transient state tests on alloy 6060-T66
F  Graphs of the bending tests

This Annex gives graphs of the bending tests carried out. Shown are the forces applied as a function of the displacement measured as well as the best-fitted line through these data points, representing the modulus of elasticity.

Lateral contraction has a significant influence on the deflection in case of at a plate thickness of 1 mm. For these plates, the modulus of elasticity was determined based on the assumption that lateral contraction was fully restrained, i.e. taking into account a factor $1/(1+\nu^2)$ for the theoretical deformation.

F.1  Alloy 5083-H111 – plate thickness of 5 mm

![Graph for Alloy 5083-H111 at 20 ºC](image)

Figure F 1 – Bending test result and modulus of elasticity for alloy 5083-H111 at 20 ºC

![Graph for Alloy 5083-H111 at 70 ºC](image)

Figure F 2 – Bending test result and modulus of elasticity for alloy 5083-H111 at 70 ºC
Appendix F

Mechanical properties at elevated temperature

Figure F 3 – Bending test result and modulus of elasticity of alloy 5083-H111 at 138 ºC

![Bending test result and modulus of elasticity of alloy 5083-H111 at 138 ºC](image1)

E = 67000 N/mm²

Figure F 4 – Bending test result and modulus of elasticity of alloy 5083-H111 at 159 ºC

![Bending test result and modulus of elasticity of alloy 5083-H111 at 159 ºC](image2)

E = 65000 N/mm²

Figure F 5 – Bending test result and modulus of elasticity of alloy 5083-H111 at 208 ºC

![Bending test result and modulus of elasticity of alloy 5083-H111 at 208 ºC](image3)

E = 62000 N/mm²
### Mechanical properties at elevated temperature

**Figure F 6** – Bending test result and modulus of elasticity of alloy 5083-H111 at 260 ºC

![Graph showing bending test result and modulus of elasticity at 260 ºC](image)

- Measured
- \( E = 56000 \, \text{N/mm}^2 \)

**Figure F 7** – Bending test result and modulus of elasticity of alloy 5083-H111 at 313 ºC

![Graph showing bending test result and modulus of elasticity at 313 ºC](image)

- Measured
- \( E = 51000 \, \text{N/mm}^2 \)

**Figure F 8** – Bending test result and modulus of elasticity of alloy 5083-H111 at 367 ºC

![Graph showing bending test result and modulus of elasticity at 367 ºC](image)

- Measured
- \( E = 44000 \, \text{N/mm}^2 \)
F.2  **Alloy 6060-T66 – plate thickness of 1 mm**

![Graph](image1)

Figure F.9 – Bending test result and modulus of elasticity for alloy 6060-T66 – t = 1 mm at 20 °C

![Graph](image2)

Figure F.10 – Bending test result and modulus of elasticity for alloy 6060-T66 – t = 1 mm at 105 °C

![Graph](image3)

Figure F.11 – Bending test result and modulus of elasticity for alloy 6060-T66 – t = 1 mm at 167 °C
Mechanical properties at elevated temperature

Figure F 12 – Bending test result and modulus of elasticity for alloy 6060-T66 – t = 1 mm at 211 ºC

Figure F 13 – Bending test result and modulus of elasticity for alloy 6060-T66 – t = 1 mm at 259 ºC

Figure F 14 – Bending test result and modulus of elasticity for alloy 6060-T66 – t = 1 mm at 307 ºC
F.3 Alloy 6060-T66 – plate thickness of 4 mm

Figure F 16 – Bending test result and modulus of elasticity for alloy 6060-T66 – t = 4 mm at 20 °C
Figure F 17 – Bending test result and modulus of elasticity for alloy 6060-T66 – t = 4 mm at 138 °C

Figure F 18 – Bending test result and modulus of elasticity for alloy 6060-T66 – t = 4 mm at 197 °C

Figure F 19 – Bending test result and modulus of elasticity for alloy 6060-T66 – t = 4 mm at 252 °C
G  Secant and tangential modulus of elasticity of aluminium alloys at room temperature

The critical load of members in compression depends on the non-linear material properties. Stowell [30] showed that the critical load for local buckling of aluminium plates and sections depends on the secant modulus of elasticity ($E_s = \sigma / \varepsilon$) and the tangential modulus of elasticity ($E_t = d\sigma / d\varepsilon$), see literature study [17].

The mechanical response model for local buckling in EN 1999-1-1 starts with the elastic critical buckling load, applying the 0.2 % proof stress and initial modulus of elasticity, and subsequently takes into account the nonlinear material properties by introducing two material classes and defining a different buckling curve for these classes. Heat-treated and artificially aged alloys are assumed to have a more bi-linear stress-strain curve and are classified in class A, while alloys in other tempers are assumed to have a more non-linear stress-strain relation and are classified in class B (see literature study [17]).

![Figure G 1 – Difference between class A and class B](image)

Alloys in series Alloy 6060-T66 is classified in EN 1999-1-2 as class A, indicating that the stress-strain relation approaches a bi-linear relation (i.e. a purely elastic relation and a plastic plateau). Alloy 5083-H111 is classified as class B, indicating that the stress-strain relation is more inelastic. Indeed, tensile tests carried out at room temperature show that, for large strains, alloy 5083-H111 shows more strain hardening (Figure G 2).
However, buckling phenomena are dominated by the material properties for small strains. Figure G 3 gives the results of the tensile tests for small strains.

![Figure G 2 – Entire stress-strain diagram of alloys 5083-H111 and 6060-T66 at room temperature](image)

![Figure G 3 – Initial part of the stress-strain diagram of alloys 5083-H111 and 6060-T66 at room temperature](image)

In order to determine the inelasticity of the alloys, Figure G 4 and Figure G 5 give the normalised values of the secant modulus of elasticity (Et/E) and tangential modulus of elasticity (Es/E) as a function of the normalised stress (σ/f₀₂). It is shown that alloy 6060-T66 exhibits a more inelastic material behaviour. And yet, alloy 6060-T66 is classified as class A while alloy 5083-H111 is classified as class B. This indicates that the classification of the alloys is not based on the real (measured) stress-strain relation.
### Appendix G.3/3

#### Mechanical properties at elevated temperature

![Graph of normalized secant modulus of elasticity as a function of normalized stress for alloys 5083-H111 and 6060-T66 at room temperature.](image)

**Figure G 4** – Normalised secant modulus of elasticity as a function of normalised stress of alloys 5083-H111 and 6060-T66 at room temperature

![Graph of normalized tangential modulus of elasticity as a function of normalized stress for alloys 5083-H111 and 6060-T66 at room temperature.](image)

**Figure G 5** – Normalised tangential modulus of elasticity as a function of normalised stress of alloys 5083-H111 and 6060-T66 at room temperature

Instead of the real stress-strain relations, the classification may be based on the Ramberg Osgood relation:

\[
\varepsilon = \frac{\sigma}{E} + 0.002 \left( \frac{\sigma}{f_{0.2}} \right)^n
\]

(G.1)

In EN 1999-1-1, the exponents at room temperature of the Ramberg Osgood relation are provided for all alloys. The values for \(E_t / E\) and \(E_s / E\) can be determined using the following derivations of the Ramberg Osgood relation:

\[
\frac{E_t}{E} = \frac{1}{E} \frac{\sigma}{\varepsilon} = \frac{1}{1 + \frac{E \cdot 0.002}{f_{0.2}} \left( \frac{\sigma}{f_{0.2}} \right)^{(n-1)}}
\]

(G.2)
Using equation (G.3) and the exponent of the Ramberg-Osgood relation given in EN 1999-1-1, Figure G 6 gives the ratio $E_t / E$ as a function of the strain for the alloys classified in class A and figure Figure G 7 gives these results for class B alloys.

Figure G 6 – Normalised tangential modulus of elasticity as a function of normalised stress according using the Ramberg Osgood relation for class A alloys
The alloys showing the most inelastic behaviour are indeed classified as class B alloys. However, for the other alloys there seems to be no obvious relation between the classification and the inelastic behaviour as based on the Ramberg Osgood relation. An e-mail correspondence with Höglund revealed that this is due to the fact that the Ramberg Osgood exponent is based on the part of the stress-strain relation for larger strains than 0.2 % plastic strain. Other exponents may apply for the part of the stress-strain relation up to 0.2 % plastic strain. This part is the relevant part for buckling.

Background document xxx reveals that all alloys in temper T6 are classified as class A, as it is assumed that these alloys have most elastic-plastic properties. Apparently, alloy 5083-Hx4 was later added to this list. All other alloys are classified as class B. The tensile tests carried out on alloys 5083-H111 and 660-T66 indicate that the division between alloys may be incorrect. It is recommended here to classify the alloys based on measured stress-strain relations.
Chemical composition of the alloys tested

Small variations in the chemical composition of the alloys may be responsible for differences in strength. The chemical composition of the batches from which the specimens origin was determined with a spectrometer, available at the Faculty of 3ME, Delft University of Technology.

The following batches were used in the tests:
- Steady-state tensile tests on alloy 5083-H111, same batch (plate) as used for the compression specimens of alloy 5083-H111 (background report xxx);
- Creep and transient state tensile tests on alloy 5083-H111;
- Steady-state tensile tests on alloy 6060-T66, originating from the extrusion length of the square hollow sections;
- Steady-state tensile tests on alloy 6060-T66, originating from the extrusion length of the angles;
- Creep and transient state tensile tests on alloy 6060-T66, originating from an extruded plate of a batch from 2005;
- Creep and transient state tensile tests on alloy 6060-T66, originating from an extruded plate of a batch from 2006.

Figure H 1 and Figure H 2 give the mass contents of the four alloying elements with the highest content of alloy 5083-H111 and alloy 6060-T66, respectively. The black lines in the graph indicate the maximum (and minimum) values of the mass content according to the specifications of the Aluminium Association. The mass content of all other alloys was, in accordance with the specifications, below 0.05 %.

![Figure H 1 – Mass content of most important alloying elements for alloy 5083](image)

3 The reason that the creep and transient state tests are not taken from the same batch as the steady-state and compression tests, is that thin walls have to be applied for the compression tests in order to study local buckling. Specimens with such a thin wall would have to be tested with a low force, which was out of the range of the load cell of the Gleeble 3500.
The mass contents of the alloying elements are for all batches in between the allowed range (black lines). The compositions of the batches of alloy 5083-H111 are almost equal to one another. In case of alloy 6060-T66, however, the silicon and iron content of the batch used for the creep and transient state tests in 2006 are higher than that of the other batches.

The strength also depends on the treatment (and temper) of the batches. For example, if an alloy is artificially aged for 120 minutes at 180 °C or for 300 minutes at 140 °C, the temper is T6 in both cases. At elevated temperature, however, the overaging - accompanied by lower strength – may occur after a different period at elevated temperature for both cases. The temper of the batches was, however, not checked.
I Multi-axial material model

The material-dependent parameters in the material model presented in chapter 9 are determined in tests with a uniaxial stress condition. In expanding the theory to a multiaxial stress condition, the following assumptions are made:

1. Assuming isotropic material, the principal directions of stress and strain should coincide;
2. In creep tests, it has been observed that the volume remains constant (Kraus). Thus the plastic value for the Poisson ratio of $\nu_p = 0.5$ applies for creep strains;
3. In creep tests, it has been observed that creep strains do not develop under a hydrostatic stress condition, i.e. a situation in which a normal stress is applied in all directions (Kraus). The Von Mises yield criterion (equation (9)), usually applied for aluminium at ambient temperature, satisfies this observation. It is assumed that the Von Mises yield criterion, also applies for creep strains at elevated temperature;

$$\sigma_{VM} = \sqrt{\sigma_{xx}^2 + \sigma_{yy}^2 + \sigma_{zz}^2 - \sigma_{xx} \sigma_{yy} - \sigma_{yy} \sigma_{zz} - \sigma_{zz} \sigma_{xx} + 3\tau_{xy}^2 + 3\tau_{yz}^2 + 3\tau_{zx}^2}$$  \hspace{1cm} (1)

In which $\sigma_{VM}$ is the Von Mises yield stress, $\sigma$-symbols indicate normal stresses and $\tau$-symbols indicate shear stresses. The equation usually considered for the accompanying effective (or Von Mises) strain is:

$$\dot{\varepsilon}_{\text{eff}} = \frac{2}{3} \sqrt{\dot{\varepsilon}_{xx}^2 + \dot{\varepsilon}_{yy}^2 + \dot{\varepsilon}_{zz}^2 - \dot{\varepsilon}_{xx} \dot{\varepsilon}_{yy} - \dot{\varepsilon}_{yy} \dot{\varepsilon}_{zz} - \dot{\varepsilon}_{zz} \dot{\varepsilon}_{xx} + 3\dot{\gamma}_{xy}^2 + 3\dot{\gamma}_{yz}^2 + 3\dot{\gamma}_{zx}^2}$$  \hspace{1cm} (2)

The multiaxial stress formulation given below is based on the derivation in Kraus. The strain rate in the Dorn Harmathy model depends on the stress. At a certain time and temperature, the relation between strain rate and stress is written as:

$$\dot{\varepsilon}_t = C_1 \cdot \sigma$$  \hspace{1cm} (3)

In the same way, it is possible to write for the 3D case:

$$\dot{\varepsilon}_{t,ij} = C_2 \cdot S_{ij}$$  \hspace{1cm} (4)

In which $\dot{\varepsilon}_{t,ij}$ is the strain rate in direction $ij$ according to equation (12)

$$\dot{\varepsilon}_{t,ij} = \begin{bmatrix} \dot{\varepsilon}_{t,xx} & \dot{\gamma}_{t,xy} & \dot{\gamma}_{t,xz} \\ \dot{\gamma}_{t,xy} & \dot{\varepsilon}_{t,yy} & \dot{\gamma}_{t,yz} \\ \dot{\gamma}_{t,xz} & \dot{\gamma}_{t,yz} & \dot{\varepsilon}_{t,zz} \end{bmatrix}$$  \hspace{1cm} (5)

In which $\varepsilon$ are the normal components and $\gamma$ are the shear components of the strain tensor.
$S_y$ is a stress deviator tensor. As the hydrostatic stress has no effect on the creep strain rate, the stress deviator contains only the distortional components of the stress. The stress deviator thus consists of the following components:

$$S_y = \begin{bmatrix} \sigma_{xx} & \tau_{xy} & \tau_{xz} \\ \tau_{xy} & \sigma_{yy} & \tau_{yz} \\ \tau_{xz} & \tau_{yz} & \sigma_{zz} \end{bmatrix} - \frac{1}{3} \begin{bmatrix} \sigma_{xx} + \sigma_{yy} + \sigma_{zz} & 0 & 0 \\ 0 & \sigma_{xx} + \sigma_{yy} + \sigma_{zz} & 0 \\ 0 & 0 & \sigma_{xx} + \sigma_{yy} + \sigma_{zz} \end{bmatrix}$$

(6)

In which the quantity $1/3 (\sigma_{xx} + \sigma_{yy} + \sigma_{zz})$ is the hydrostatic stress.

If a hydrostatic stress is applied in equation (4), i.e. $\sigma_{xx} = \sigma_{yy} = \sigma_{zz} = \sigma$, it follows that all strain rate components are equal to 0, hence assumption 2 is satisfied.

The assumption of constancy of volume applied in equation (4) results:

$$\epsilon_t = \epsilon_t^{xx} + \epsilon_t^{yy} + \epsilon_t^{zz} = C_2 \left( S_{11} + S_{22} + S_{33} \right) = 0.$$  Thus the relation satisfies all assumptions.

$C_2$ is determined by recognising that equation (11) should reduce to equation (10) for the uniaxial stress case. Consider a uniaxial test with $\sigma_{xx} = \sigma$ and all other stress components equal to zero. In this case, $\epsilon_t^{xx} = \hat{\epsilon}_t$, $\epsilon_t^{yy} = \epsilon_t^{zz} = \hat{\epsilon}_t$. Using the assumption of constant volume, i.e. $\dot{\epsilon}_t^{xx} + \dot{\epsilon}_t^{yy} + \dot{\epsilon}_t^{zz} = 0$, it follows that $\hat{\epsilon}_t^{yy} = \hat{\epsilon}_t^{zz} = -1/2 \hat{\epsilon}_t$ and all shear strain rates are equal to zero. In the same way as done for the Von Mises stress, an effective strain is introduced with such a description that the effective strain reduces to the strain in x-direction for the uniaxial case.

If all stress and strain rate components of this uniaxial case are applied in equation (11), it follows that

$$\dot{\epsilon}_t^{eff} = \frac{2}{3} C_2 \sigma_{VM} \Rightarrow C_2 = \frac{3 \dot{\epsilon}_t^{eff}}{2 \sigma_{VM}}$$

(16)

Since the effective strain rate and the von Mises stress are defined so as to reduce to the uniaxial strain and stress, it is possible to extend the model for the uniaxial strain rate (equations(6a) and (6b)) to the multiaxial case by introducing the effective quantities:

$$\epsilon_t^{eff} \leq \epsilon_t^{eff,lim} \Rightarrow \dot{\epsilon}_t^{eff} = Z \left( \sigma_{VM} \right) \cdot e^{\frac{Q}{RT}} \left( \frac{\epsilon_t^{0,\sigma_{VM}} + 0.00001}{\epsilon_t^{eff} - \epsilon_t^{e,\sigma_{VM}} + 0.00001} \right)^4$$

(17 a.)

$$\epsilon_t^{eff} > \epsilon_t^{eff,lim} \Rightarrow \dot{\epsilon}_t^{eff} = Z \left( \sigma_{VM} \right) \cdot e^{\frac{Q}{RT}} \left( \frac{\epsilon_t^{0,\sigma_{VM}} + 0.00001}{\epsilon_t^{eff} - \epsilon_t^{e,\sigma_{VM}} + 0.00001} \right)^4 \frac{\epsilon_t^{eff}}{\epsilon_t^{eff,lim}}$$

(17 b.)

Equations (17) are applied in equation 16 to determine quantity $C_2$. The model for multiaxial creep is then completed by substitution of $C_2$ in equation 11:
\[
\begin{pmatrix}
\dot{\varepsilon}_{t,xx} \\
\dot{\varepsilon}_{t,yy} \\
\dot{\varepsilon}_{t,zz} \\
\dot{\gamma}_{t,xy} \\
\dot{\gamma}_{t,yz} \\
\dot{\gamma}_{t,zx}
\end{pmatrix}
= C_2 \cdot
\begin{pmatrix}
2 & -1 & -1 & 0 & 0 & 0 \\
-1 & 2 & -1 & 0 & 0 & 0 \\
-1 & -1 & 2 & 0 & 0 & 0 \\
0 & 0 & 0 & 3 & 0 & 0 \\
0 & 0 & 0 & 0 & 3 & 0 \\
0 & 0 & 0 & 0 & 0 & 3
\end{pmatrix}
\begin{pmatrix}
\sigma_{xx} \\
\sigma_{yy} \\
\sigma_{zz} \\
\tau_{xy} \\
\tau_{yz} \\
\tau_{zx}
\end{pmatrix}
\]

Where \( \varepsilon_{t,eff} \leq \varepsilon_{t,eff,lim} \):
\[
C_2 = \frac{1}{2} \frac{Z(\sigma_{VM}) \cdot e^{\frac{-Q}{RT(t)}}}{\sigma_{VM}} \cdot \coth^2 \left( \frac{\varepsilon_{t0}(\sigma_{VM})}{\varepsilon_{t,eff}} \right)
\]

\( \varepsilon_{t,eff} > \varepsilon_{t,eff,lim} \):
\[
C_2 = \frac{1}{2} \frac{Z(\sigma_{VM}) \cdot e^{\frac{-Q}{RT(t)}}}{\sigma_{VM}} \cdot \coth^2 \left( \frac{\varepsilon_{t0}(\sigma_{VM})}{\varepsilon_{t,eff}} \right) \cdot \frac{\varepsilon_{t,eff}}{\varepsilon_{t,lim}}
\]