Temperature induced stresses in a reactor containment building

A case study of Forsmark F1

MATTIAS KÖNÖNEN

Master of Science Thesis
Stockholm, Sweden 2012
Temperature induced stresses in a reactor containment building
A case study of Forsmark F1

Mattias Könönen

April 2012
TRITA-BKN. 349, 2012
ISSN 1103-4297
ISRN KTH/BKN/EX–349–SE
The aims of this thesis were to study two aspects of temperature induced stresses with reference to the nuclear power plant Forsmark F1. One aspect included the thermal cooling effect of the ventilated tendon ducts in the cylinder wall. It was of interest to study if the thermal cooling effect of the ventilated tendon ducts had a positive global effect which was relevant to consider in global three-dimensional models. The other aspect included the influence of embedded steel in the upper ring slab. With the purpose to study if embedded steel was an aspect that was considered necessary to include in transient analyses.

The used main analysis tool was the finite element method (FEM), through the use of the commercially available finite element program SOLVIA.

The influence of the thermal cooling effect of the ventilated tendon ducts indicated a stress reducing effect, with reduced cracked concrete in the cylinder wall at elevated temperatures. The influence of embedded steel indicated increased temperature differences between the embedded steel and the surrounding concrete, with cracked concrete locally between the steel and the concrete at elevated temperatures. The thermal cooling effect of the ventilated tendon ducts was considered relevant to consider in global three-dimensional models. Embedded steel was considered necessary to include in transient analyses.

Keywords: reactor containment building, temperature induced stresses, ventilated tendon ducts, embedded steel.
Sammanfattning

Målen med denna uppsats var att studera två aspekter av temperatur inducerade spänningar med referens till kärnkraftsanläggningen Forsmark F1. Ena aspekten inkluderade den termiska kyleffekten av de ventilerade spännkabelrören i cylinderväggen. Det var av intresse att studera om den termiska kyleffekten av de ventilerade spännkabelrören hade en positiv global effekt som var relevant att beakta i globala tredimensionella modeller. Den andra aspekten inkluderade inverkan av ingjutet stål i den övreringplattan. Med syftet att studera om ingjutet stål var en aspekt som ansågs nödvändigt att beakta i transiente analyser.

Huvudanalyseringsverktyg var finita element metoden (FEM), genom användning av det kommersiellt tillgängliga finita element programmet SOLVIA.

Inverkan av den termiska kyleffekten av de ventilerade spännkabelrören indikerade en spännings reducerande effekt, med reducerade zoner av sprucken betong i cylinderväggen vid förhöjda temperaturer. Inverkan av ingjutet stål indikerade ökade temperaturskillnader mellan det ingjutna stålet och den omgivande betongen, med sprucken betong lokalt mellan stålet och betongen vid förhöjda temperaturer. Den termiska kyleffekten av de ventilerade spännkabelrören ansågs relevant att beakta i globala tredimensionella modeller. Ingjutet stål ansågs vara nödvändigt att inkludera i transiente analyser.

Nyckelord: reaktor inneslutning, temperatur inducerade spänningar, ventilerade spännkabelrör, ingjutet stål.
Preface

The research presented in this thesis has been carried out from October 2011 to April 2012 at Vattenfall Research and Development AB in collaboration with the Division of Concrete Structures, Department of Civil and Architectural Engineering at the Royal Institute of Technology (KTH). The project was initiated by Dr. Richard Malm, who supervised the project with Daniel Eriksson and Tobias Gasch from Vattenfall.

I give my sincere appreciation and gratitude to Dr. Richard Malm for introducing me to this project and thank him for all the opportunities this has given me, during the project and for the future. I would like to thank Dr. Richard Malm for his invaluable advice, guidance and support through this thesis and for spreading his knowledge and founding my interest for the subject through his courses at KTH. His enthusiasm during and after lecturing has been a great inspiration for me during my educational years at KTH.

Furthermore, I would like to give my appreciation and thankfulness to Daniel Eriksson and Tobias Gasch for their invaluable advice, support and help through this thesis. Their advice and guidance have been a great learning experience.

At last, I would like to thank Magnus Lundin and Patrik Gatter at Vattenfall for giving me the opportunity to carry through this project and for their advice and support. I would also like to thank all other co-workers at the division at Vattenfall. Special thanks to Nick Zaraei for his advice and help to find important documentations and to Karl-Axel Bartholf at Forsmark for providing me with the three-dimensional model.

Stockholm, April 2012

Mattias Könönen
Contents

Abstract iii

Sammanfattning v

Preface vii

1 Introduction 1
   1.1 Nuclear containment buildings ........................................ 3
   1.2 Aims and scopes of the thesis ....................................... 8
   1.3 Structure of the thesis .............................................. 9

2 Temperature Induced Stresses 11
   2.1 Mechanical stresses and strains ...................................... 11
      2.1.1 Small strain formulation ........................................ 11
      2.1.2 Strains ....................................................... 11
      2.1.3 Stresses ..................................................... 12
      2.1.4 Principal stresses ............................................. 13
      2.1.5 Thermal expansion ............................................. 15
   2.2 Thermal conduction .................................................. 16
      2.2.1 Transient heat conduction ...................................... 17
      2.2.2 Steady state heat conduction .................................. 18
   2.3 Surface heat transfer ................................................ 19
      2.3.1 Convection .................................................... 19
      2.3.2 Radiation ..................................................... 21
A Hand Calculations

A.1 Pool construction and reactor tank pressure loads . . . . . . . . . . . 105
A.2 Convective heat transfer coefficients . . . . . . . . . . . . . . . . . 108
A.3 Vertical prestress . . . . . . . . . . . . . . . . . . . . . . . . . . . . 112
A.4 Horizontal prestress . . . . . . . . . . . . . . . . . . . . . . . . . . . 114
Chapter 1

Introduction

In a normal Swedish year, nuclear power along with hydropower delivers with about equal shares over 90% of the total electric production. Other energy sources such as renewable bio and wind power are slowly gaining larger production shares. Sweden is in the sixth place in the world in terms of the largest share of nuclear electricity and Sweden has the most nuclear power generation per capita, Swedish Energy Agency (2010). Hence, the nuclear power industry is of great importance. The Swedish parliament has decided to abolish the Nuclear Power Phase-Out Act. The parliament has also removed the prohibition to construct new nuclear reactors in the Act on Nuclear Activities. This is making it possible to gradually replace existing nuclear power reactors with new ones and has led to a strong development in the current Swedish nuclear power program. Large investments are made in the remaining operating reactors to prepare for a long term operation, modernisation and uprates of the plants, making them fit for 40 years of operation and beyond. In Sweden, nuclear technology started in 1947. Atomenergi AB was commissioned to carry through a development program decided by the Parliament and as a result, the first research reactor was started in 1954. The first prototype nuclear power plant (PHWR) Ågesta, located in a suburb of Stockholm was mainly used for district heating and operated from 1964 until 1974. The first commercial nuclear power plant Oskarshamn I was commissioned in 1972 and eleven additional reactors at Barsebäck, Oskarshamn, Ringhals and Forsmark were built to 1985. The Swedish commercial reactor fleet consisted of nine BWR(s) with ASEA-ATOM design and three PWR(s) with Westinghouse design. The nuclear reactors Barsebäck I and Barsebäck II were the first commercial reactors to be permanently taken out of operation in November 1999 and in May 2005. The Government decided that the reactors should be shut down as part of the policy to phase out nuclear power in Sweden. In 2004, Studsvik Nuclear decided to permanently shut down the two research reactors R2 and R2-0, due to economic reasons. They were closed in June 2005. In Fig. 1.1 the distribution of both operating and phased out nuclear facilities in Sweden are illustrated.
Nine of the reactors, including Barsebäck I and II, were uprated during the years 1982 to 1989. The uprates were about 6 – 10% of the original licensed thermal power levels. These uprates were possible without any major plant modifications due to better use of existing margins, better analysis methods and improved fuel design. The current uprate plans include major uprates for seven of the reactors and a minor uprate for one. A power increase can affect the facility in a number of ways and with a varying degree depending on the size of the uprate. The capacity of a number of components and systems in the nuclear power plant must be verified. The impact on safety occurs from the fact that the core will contain more reactivity. The residual heat of the reactor is proportional to the operating power and will therefore increase. The studied object Forsmark F1 is planned for a 20% uprate. This is a major uprate and will certainly change the load conditions. A higher uprate equals higher temperatures during operation, with a demand of new verifications as a result. (Ministry of the Environment Sweden, 2007)
1.1 Nuclear containment buildings

A large variety of primary systems are used world-wide for nuclear power generation where the primary coolant consists of light water, gas, heavy water or liquid metal. There are two types of nuclear reactors:

- Pressurised water reactors (PWR(s))
- Boiling water reactors (BWR(s))

Most plants use some type of containment or confinement structure, as an additional barrier against the outside environment. There are many different containment types and configurations. There are some basic containment types where the designs primarily use the passive pressure suppression concept (PS-principle), or primarily rely on large strong volumes. Most of the containments are made of steel or reinforced concrete. These come with a variety of shapes such as:

- Steel spheres
- Double wall reinforced, unlined or with a steel or epoxy liner
- Double wall reinforced and prestressed concrete, unlined or with a steel or epoxy liner

Boiling water reactors

The BWR designs are evolved from the Mark I, Mark II and Mark III designs and they usually use a pressure suppression pool. The Mark III design is not presented in this thesis because it has little relevance to the studied reference object. The BWR Mark I, illustrated in Fig. 1.2, is a pressure suppression containment which allows the containment to have a smaller volume. The containment is divided into:

- Drywell, containing the reactor vessel
- Wetwell, containing the suppression pool

The containments are either of concrete or steel. The water in the suppression pool acts as an energy absorbing medium, in the event of an accident. At a Loss of coolant accident (LOCA), steam will stream from the drywell, through vent lines and down comers, into the suppression pool. Vacuum breakers equalise the pressure, if the pressure in the wetwell exceeds the pressure in the drywell. If the overall pressure in the containment becomes too high, lines connected to the wetwell can be used to vent the containment. (Hessheimer and Dameron, 2006)
The Mark II containment, illustrated in Fig. 1.3, is similar to the Mark I concept. The difference is a more unified containment structure and a simplified suppression pool design, instead of the complicated Mark I torus design. In the Mark II, the suppression pool sits in the wetwell region below the drywell. The containment heat removal systems, sprays and suppression pool cooling are the same and the containment venting is similar to the Mark I containment.
1.1. NUCLEAR CONTAINMENT BUILDINGS

Containment design and performance criteria

A containment system is primarily designed to; Contain radioactive material released from the primary system in case of an accident. Protect the nuclear system from external threats such as weather and missiles produced by tornadoes, wind, earthquakes or aircraft impact. Act as a supporting structure for operational equipment. Typical safety related concrete structures contained in BWR plants are; Primary containers, containment internal structures, secondary containments, reactor building and fuel storage pools, Hessheimer and Dameron (2006). The requirement for the BWR containments primary containment includes; To provide an essentially leak tight barrier against an uncontrolled release of radioactive substances in all design basis accident conditions. Be able to withstand the predicted pressure and temperature conditions resulting from a LOCA. At testing, withstand a leak rate at the peak calculated accident pressure up to the containment design pressure. As well as permit a periodic inspection and testing of all significant components and surfaces. The containment vessel can also provide structural support for the reactor tank system and other internal equipment. The primary BWR plant types: Mark I, Mark II exists with a number of internal containment structures consisting of reinforced concrete, Naus (2007). These structures may serve one or several functions, for example:

- Radiation shielding
- Provisions for personnel accessibility
- Anchorage, support and protection of the reactor tank system and other equipment
- Resistance to jet, pipe whip, and loads produced by emergency conditions
- A boundary of wetwells and pool structures, allowing communication between drywell and wetwell (Mark II)
- Lateral stability for the containment
- Transfer the containment loads to the foundation

All the presented BWR designs have reinforced concrete structures that serve as secondary containments or reactor buildings and provide support and shielding functions for the primary containment. The secondary containments are typically composed of beam, floor, and wall elements. These structures are often classed as safety related because they; Provide an additional radiation shielding or provide a resistance to environmental and operational loads. They also house safety related mechanical equipment, spent fuel, and the primary containment, Do and Chockie (1994). There are second requirements concerning special loads on the containment structures. These are in principle only issued due to a hypothetical break down where they need to be managed. These special loads are completely different from all loads that are treated in the usual building codes, Roth et al. (2002). These loads involve:
• Temperature transients and gradients
• Safe shutdown earthquake loads
• Internal (and external) missiles
• Mechanical loads from pipe rupture
• External pressures
• Winds and tornadoes

Particular severe accidents to regard for the containment include:

• Over-pressure
• Dynamic pressure (shock waves)
• Internal missiles
• External missiles
• Melt-through
• Bypass

If looking especially on the response of the containment to over-pressurisation and the corresponding thermal loads. Some of the severe accident pressure loads can be quite rapid from a thermo physical perspective, e.g. deflagrations, detonations, etc. But when looking at most scenarios, the typical rates of loading are essentially static from a structural perspective, Hessheimer and Dameron (2006).

**The PS-principle**

An important and particular severe accident scenario is an internal pipe rupture at a main steam pipe, inside a reactor containment building. At an internal pipe rupture scenario, radioactive steam will under a high pressure stream out in the reactor containment. The steam pressure is limited by the PS-principle. This mechanism is the so called PS-principle, where PS stands for pressure suppression. The PS-principle is mainly applied for BWR containments. A fictive first phase, describing the PS-principle is illustrated in Fig. 1.4, followed by a fictive second phase illustrated in Fig. 1.5.
If a pipe rupture occurs, high pressurised steam will stream out in the drywell with an increasing pressure as the result. Because of the pressure increase, steam will be forced down in the wetwell through pipe installations, called system 328. In the wetwell, the steam condenses and the overall pressure increase is limited. Since some gases in the steam, e.g. nitrogen, are non-condensable. The pressure in the gas phase of the wetwell will increase during this sequence.
By starting sprinkling in the drywell when the stream from the pipe rupture has stopped, a condensation of steam is obtained. The result is a lowered overall pressure in the drywell. When the pressure in the drywell is lowered, to a level where the pressure is lower than in the gas phase of the wetwell. A re-stream will occur by the vacuum valves from the gas phase of the wetwell to the drywell. This function requires that the connection between the cylinder wall and the middle slab is air tight.

1.2 Aims and scopes of the thesis

This thesis is a feasibility study and the aims of this thesis are to study and present results concerning the thermal cooling effect of the ventilated tendon ducts in the
1.3. Structure of the Thesis

The structure of this thesis is divided into six chapters.

Chapter 1 gives a basic introduction to the studied field. The aims and scopes of the thesis are presented.

Chapter 2 presents the basic theories used in the finite element method.

Chapter 3 presents general aspects about post-tensioning systems, the theories behind calculating prestress losses, general aspects about the finite element method and the finite element program SOLVIA.

Chapter 4 gives detailed information about the studied object and the finite element models.

Chapter 5 presents the numerical results from the finite element analyses.

Chapter 6 presents the discussion and conclusions from the presented results. Chapter 6 includes the discussion about further research and development. In the Appendix, the hand calculations are presented.
Chapter 2

Temperature Induced Stresses

2.1 Mechanical stresses and strains

This section presents the basic theory of elasticity.

2.1.1 Small strain formulation

The small displacement/small strain formulation is defined by infinitesimal deformations of a continuum body, in which the displacements and the displacement gradients are small compared to the unity. According to Ugural and Fenster (2003), their products (higher order terms) are therefore neglected. In other words, when using the small strain formulation, geometric non-linearity is ignored. Hence, the use of an isotropic linear elastic material with the small displacement/small strain formulation corresponds to a linear formulation. While the strain is attributed to the undeformed body the strain will be small, i.e. no second order effects. In the small strain formulation, the current area is always assumed to be equal to the initial undeformed area. In the following, small strains are defined as strains less than 2% for all materials. (Bathe, 2009)

2.1.2 Strains

An engineering strain is expressed as the ratio of total deformation to the initial dimension of the material body in which the forces are being applied. According to Ugural and Fenster (2003), the engineering normal strain is defined by Eq. (2.1) as the change in length per unit of the line element or fibres. The engineering strain is illustrated in Fig. 2.1.

\[
\varepsilon = \frac{\Delta l}{l_0}
\]

(2.1)

where:
\( \Delta l = \) Change in length, [m]
\( l_0 = \) Initial length, [m]

A positive sign corresponds to elongation while a negative sign corresponds to contraction of the structure/material.

### 2.1.3 Stresses

A mechanical stress is defined as an internal force per unit area. There are different types of stresses. Stresses acting perpendicularly to a surface are called normal stresses, and are designated with \( \sigma \). In this thesis, \( \sigma_t \) represents a tensile and positive stress directed away from the surface, pulling the material and \( \sigma_c \) represents a compressive and negative stress directed towards the surface, compressing the material. Stresses acting parallel to the surface are called shear stresses, and are designated with \( \tau \). Shear stresses can be both positive and negative.

#### States of stress

Fig. 2.1 shows a rod under uniaxial tension.

![Figure 2.1: Rod under uniaxial tension.](image)

According to Bathe (2009), the engineering stress is defined by Eq. (2.2), as force per undeformed area in relation to the undeformed body. Note that the area is perpendicular to the force. The engineering stress is illustrated in Fig. 2.1.

\[
\sigma = \frac{F}{A_0} \tag{2.2}
\]

where:
\( F = \) Force, [N]
\( A_0 = \) Undeformed area, [m\(^2\)]
If shear stresses are added to the picture, the state of stress becomes biaxial. A biaxial state of stress exists when the stresses and body forces are independent of one of the coordinates, e.g. \(z\)-coordinates. Such a state considers only one plane, described by the stresses \(\sigma_x\), \(\sigma_y\) and \(\tau_{xy}\) and the \(x\) and \(y\) body forces. The general case of a three-dimensional state of stress is that stress is mainly not uniformly distributed over the cross section of a material body. Consequently, the stress at a given point differs from the average stress over the entire area. Therefore, it is necessary to define the stress in a specific point in the body. The stress at any point in an object, assumed to behave as a continuum, is completely defined by nine stress components: Three orthogonal normal stresses and six orthogonal shear stresses, Ugural and Fenster (2003). The stress components can be assembled into the following matrix form, see Eq. (2.3).

\[
[\sigma] = \begin{bmatrix}
\sigma_{xx} & \tau_{xy} & \tau_{xz} \\
\tau_{yx} & \sigma_{yy} & \tau_{yz} \\
\tau_{zx} & \tau_{zy} & \sigma_{zz}
\end{bmatrix}
\] (2.3)

Each row represents the group of stresses acting on a plane illustrated by Fig. 2.2.

The stresses in Fig. 2.2 are only marked on the three visible sides of the cube. Since most of the construction materials used in structures has different material strength in tension, compression and shear. The stresses have to be examined in all the different directions to be able to estimate the structural strength of a building.

### 2.1.4 Principal stresses

According to Ugural and Fenster (2003), the value and direction of the maximum normal stress are obtained by derivation of the transformation equation, see Eq. 2.4, for normal stress in the \(x'\)-direction.
\[ \frac{d \sigma_x'}{d \theta} = -(\sigma_x - \sigma_y) \sin 2\theta + 2 \tau_{xy} \cos 2\theta = 0 \]  
(2.4)

The expression for the principal directions is then according to Eq. (2.5).

\[ \theta_p = \frac{1}{2} \arctan \left( \frac{2 \tau_{xy}}{\sigma_x - \sigma_y} \right) \pm n \cdot 90^\circ \]  
(2.5)

In the principal directions, the shear stresses are equal to zero. The normal stresses in the principal directions are the principal stresses. Eq. (2.6) is the expression for the maximum and minimum principal stresses.

\[ \sigma_{\text{max,min}} = \frac{\sigma_x + \sigma_y}{2} \pm \sqrt{\left( \frac{\sigma_x - \sigma_y}{2} \right)^2 + \tau_{xy}^2} \]  
(2.6)

The maximum and minimum principal stresses are used as comparison stresses in this thesis.

**Stress/strain formulation**

For linear elastic behaviour, all elements and material models uses the engineering stress/engineering strain relationship. The materials exhibit an initial region in the stress/strain diagram where the material behaves elastic and linear. In this initial region, stress is directly proportional to the strain, Ugural and Fenster (2003). If the normal stress acts in the \( x \)-direction, the relationship can be defined according to Hooke’s law, see Eq.(2.7).

\[ \sigma_x = E \varepsilon_x \]  
(2.7)

where:

\( E = \) Young’s modulus, [Pa]

A subscript zero indicates initial values (\( \sigma_0 \)). For linear elastic material models, the stress/strain relation for one-dimension is stated by Hooke’s law in Eq. (2.7), with the initial values into Eq. (2.8).

\[ \sigma = E \varepsilon + \sigma_0 \]  
(2.8)

The initial values, \( \sigma_0 \), are defined according to Eq. (2.9)

\[ \sigma_0 = -E \varepsilon_0 \]  
(2.9)

Eq. (2.8) can now be written as Eq. (2.10)
\[ \sigma = E(\varepsilon - \varepsilon_0) \quad (2.10) \]

In the finite element method, the stresses are calculated using the strains according to Eq. (2.10), at the point of interest. (Bathe, 2009)

### 2.1.5 Thermal expansion

By increasing or decreasing the uniform temperature of an unconstrained elastic body, the consequences are expansion or contraction on the body. If this expansion or contraction occurs in a way that affects the shape of a cubic element of the solid to remain cubic. But with increased or decreased change of length on each of its sides. Normal strains occur in each direction of the sides without any normal stresses and there are neither shear strains nor shear stresses. Thermal stresses will occur if the body is heated and produces a non-uniform temperature field. If the thermal expansions are constrained by e.g. a fixed boundary condition, even thermal stresses will occur if the temperature is uniform, Ugural and Fenster (2003). When a material is heated, its particles begin moving more and thus usually maintain a greater average separation. The degree of expansion divided by the change in temperature is called the materials thermal expansion coefficient. The thermal expansion coefficient varies between different material types. Steel and concrete have approximately the same thermal expansion coefficient. This is one of the prerequisites for the functionality of reinforced concrete, Burström (2001). When a thermal load is applied to the structure the material characteristics are considered to be temperature independent (constant). The temperature in the finite element method is evaluated based on the nodal temperature and the element shape functions and then used to calculate the thermal strains. Initial strains, \( \varepsilon_0 \), may be due to e.g. temperature change or moisture, Cook et al. (2002). For an isotropic material where the initial strains are due to temperature change, \( \Delta T \), then the following expression is used for thermal expansion, see Eq. (2.11)

\[ \varepsilon_0 = \varepsilon_t = \alpha \Delta T = \alpha (T_t - T_0) \quad (2.11) \]

where:

- \( \varepsilon_t \) = Thermal strain
- \( \alpha \) = Thermal expansion, \([K^{-1}]\)
- \( T_t \) = Temperature at time \( t \), \([K]\)
- \( T_0 \) = Initial temperature, \([K]\)

The strains are calculated relative to a reference temperature, \( T_{\text{ref}} \), at which the body is free of stress. The thermal strain is considered in the finite element method by replacing the total incremental strain in the governing incremental equation, Eq. (2.10), with the strain \( (\varepsilon - \varepsilon_t) \). (Ugural and Fenster, 2003)
2.2 Thermal conduction

Temperature differences will with time reach thermal equilibrium. Heat will always transfer from a region of a higher temperature to a region of a lower. Heat transfer is also called heat flux and refers to the total amount of heat loss from the structure per time unit. The heat transfer through an area unit is called heat flux density, Burström (2001). From a physical point of view, there are three different heat transfer mechanisms:

- Conduction
- Convection
- Radiation

Heat conduction is the flow of heat directly through a physical material. The temperature of a material is a measure of the disordered motion of the materials molecules. A higher temperature corresponds to stronger movement. Eq. (2.12) is the governing heat conduction equation, due to the equilibrium.

\[ \Phi = (T_1 - T_2) \Lambda \]  \hspace{1cm} (2.12)

where:

- \( \Phi \) = Heat flow, [W]
- \( T \) = Temperature, [K]
- \( \Lambda \) = Thermal conductance, [W/K]

At one-dimensional heat conduction with a temperature gradient within a body of an isotropic material the density of heat flow, \( q \), can be calculated according to Eq. (2.13).

\[ q = -\lambda \text{ grad } T \]  \hspace{1cm} (2.13)

where:

- \( q \) = Density of heat flow, [W/m\(^2\)]
- \( \lambda \) = Thermal conductivity of the material, [W/(mK)]
- grad \( T \) = Temperature gradient, [K/m]

Eq. (2.13) is often referred to as Fourier’s law written in the equivalent differential form. Fourier’s law states that the time rate of heat transfer through a material is proportional to the (negative) temperature gradient and to the area, at right angles to that gradient, through which the heat is flowing. The temperature distribution at stationary heat transfer in a homogeneous material is always linear. Therefore, if the temperature only depends on the \( x \)-direction. Eq. (2.13) then simplifies into Eq. (2.14).
Eq. (2.14) is the density of heat flow written in its more general, one-dimensional form. The minus sign shows that the heat flows from a higher to a lower temperature and that the heat flow is considered as positive when it goes in the positive \( x \)-direction.

### 2.2.1 Transient heat conduction

Consider a three-dimensional solid body with heat flow in the principal axis directions \( x, y, z \), according to Eq. (2.15)

\[
q_x = -\lambda_x \left( \frac{\partial T}{\partial x} \right); \quad q_y = -\lambda_y \left( \frac{\partial T}{\partial y} \right); \quad q_z = -\lambda_z \left( \frac{\partial T}{\partial z} \right)
\]  
(2.15)

Equilibrium of heat flow in the interior of the body thus gives Eq. (2.16).

\[
\frac{\partial}{\partial x}(\lambda_x \frac{\partial T}{\partial x}) + \frac{\partial}{\partial y}(\lambda_y \frac{\partial T}{\partial y}) + \frac{\partial}{\partial z}(\lambda_z \frac{\partial T}{\partial z}) = -q_b
\]  
(2.16)

The governing heat conduction equation is based on Fourier’s law, Eq. (2.13). According to Cook et al. (2002), the three-dimensional governing heat conduction equation, Eq. (2.17), is an equation which describes the distribution of heat in a given region over time.

\[
\frac{\partial}{\partial x}(\lambda_x \frac{\partial T}{\partial x}) + \frac{\partial}{\partial y}(\lambda_y \frac{\partial T}{\partial y}) + \frac{\partial}{\partial z}(\lambda_z \frac{\partial T}{\partial z}) + I = C \frac{\partial T}{\partial t} = -q_b
\]  
(2.17)

where:

- \( I \) = Rate of internal heat generation, [W/m³]
- \( \lambda_x, \lambda_y, \lambda_z \) = Thermal conductivities in the \( x, y, z \)-direction, [W/(mK)]
- \( C \) = Volumetric heat capacity, [J/(m³K)]
- \( q_b \) = Rate of heat generated per unit volume, [W/m³]

Eq. (2.18) defines the volumetric heat capacity.

\[
C = \rho \ c_p
\]  
(2.18)

where:

- \( \rho \) = Density, [kg/m³]
- \( c_p \) = Specific heat capacity, [J/(kgK)]
Eq. (2.17) considers time dependent temperature distributions. For steady state conditions without internal heat generation the equation, Eq. (2.17), will be simplified into Eq. (2.23). (Bathe, 2009)

2.2.2 Steady state heat conduction

In heat transfer calculations, steady state heat transfer means that the temperatures of the system are not time dependent. In the steady state, heat is not being stored in or removed from any part of the system since this implies a change in temperature. For a homogenous material slab, with a temperature gradient in the direction normal to the surface. The steady state case implies that heat can’t be stored at any point in the material. Therefore, the temperature gradient has to be constant. This means that the temperature will be linearly distributed between the surfaces. For an isotropic and homogeneous material without internal heat generation and steady state condition the governing equation for heat conduction is reduced. Since the temperature is time independent (constant) and if we further assume that the temperature is constant in the y and z-directions. The general equation for heat conduction reduces to Eq. (2.19).

\[ \frac{\partial^2 T}{\partial x^2} = 0 \] (2.19)

For a wall slab with the thickness, \( d \), in the \( x \)-direction and temperatures, \( T_1 \) and \( T_2 \), on both sides. Eq. (2.19) can be solved by integrating on both sides, giving Eq. (2.20).

\[ \frac{\partial T}{\partial x} = \text{constant} \] (2.20)

Due to the steady state condition, the heat flow through the wall is constant according to Eq. (2.20) and the temperature distribution through the wall will be linear. The temperature at an arbitrary point in the wall can be calculated according to Eq. (2.21).

\[ T(x) = T_1 + \frac{x(T_2 - T_1)}{d} \] (2.21)

Eq. (2.22) is the expression for the constant heat flow through the wall.

\[ q = -\lambda \frac{(T_2 - T_1)}{d} \] (2.22)

For a three-dimensional case, the thermal conductivities in the three directions are usual set to the same \((\lambda_x = \lambda_y = \lambda_z)\) due to an isotropic and homogenous material.
In the steady state case the internal heat generation and volumetric heat capacity are set to zero. The modified general equation is according to Eq. (2.23).

$$\frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} = 0 \quad (2.23)$$

## 2.3 Surface heat transfer

In this section, the theories of surface heat transfer due to convection and radiation are explained. The surface heat transfer is a combination of convection and radiation. The heat transfer in a room is a complex system of the radiation exchange between the surfaces, parallel to the convective heat transfer between the surfaces and the ambient medium. It is assumed that the medium does not absorb radiation. This means that radiation and convective heat transfer can be regarded as two separate processes coupled through the surface temperature.

### 2.3.1 Convection

Convection, as a heat transport mechanism, is a streaming medium that transports heat between places with different temperatures.

**Convective surface heat transfer**

An important application of convective heat transfer is the surface heat transfer. The surface heat transfer is the heat transfer between the boundary surfaces and the medium within a given volume. If assuming that the medium in the volume is standing still, the heat is transferred with conduction in the medium only. At higher temperature differences between the surface and the medium, the density differences will generate movements in the medium. The heat transfer is then a combined process of conduction and convection.

**Natural convection**

If the medium is air, density difference in the air will generate air movements in the room. The density difference is a result of varying temperature within the room. Warmer parts of air will always tend to rise upwards and cooler air will sink downwards. The air movement is continued until equilibrium is gained. If heat is constantly supplied or removed at different surfaces in the room, the air movement will continue to circulate. In this thesis, only natural convection is assumed to occur.
CHAPTER 2. TEMPERATURE INDUCED STRESSES

Natural convection on surfaces

For natural convection, it is the temperature difference that generates the air movements and thereby the conditions for surface heat transfer. The air in the room will start circulating, due to the density differences. It can be assumed that there is a downward flow of air on the colder side and an upward flow of air on the warmer side. The result is a temperature and heat flow gradient along the surface.

Convection boundary condition

According to Bathe (2009), the heat flux associated with conduction along the surface only can be calculated according to Eq. (2.24).

\[
q_s = h_c(T_e - T_s) \tag{2.24}
\]

where:

- \( h_c \) = Convective heat transfer coefficient, \([\text{W/(m}^2\text{K})]\)
- \( T_e \) = External environment temperature, \([\text{K}]\)
- \( T_s \) = Unknown body surface temperature, \([\text{K}]\)

Convective heat transfer coefficient

The convective heat transfer coefficient is used in calculating the heat transfer by convection between a fluid and a solid. The convective heat transfer coefficient is defined according to Eq. (2.25).

\[
h_c = \frac{Nu \lambda_{\text{fluid}}}{l} \tag{2.25}
\]

where:

- \( Nu \) = Nusselt’s number
- \( \lambda_{\text{fluid}} \) = Thermal conductivity of the fluid, \([\text{W/(m} \cdot \text{K}])\)
- \( l \) = Characteristic length, \([\text{m}]\)

The mode of flow and properties of the flowing medium is characterised by dimensionless numbers. In heat transfer at a boundary surface within a fluid, Nusselt’s number is the ratio of convective to conductive heat transfer along (normal to) the boundary. Typically for natural convection, Nusselt’s number is expressed as a function of Prandtl’s and Grashof’s number, according to Eq. (2.26).

\[
Nu = f(Pr, Gr) \tag{2.26}
\]

Prandtl’s number is defined according to Eq. (2.27).
2.3. SURFACE HEAT TRANSFER

\[ Pr = \frac{\nu \rho c_p}{\lambda} \]  
(2.27)

where:
\[ \nu = \text{Kinematic viscosity for fluid, [m}^2/\text{s]} \]
\[ \rho = \text{Density of the fluid, [kg/m}^3\text{]} \]
\[ c_p = \text{Specific heat capacity, [J/(kgK)]} \]

Grashof’s number is defined according to Eq. (2.28).

\[ Gr = \frac{g \beta l^3 \Delta T}{\nu^2} \]  
(2.28)

where:
\[ g = \text{Gravity constant, [m/s}^2\text{]} \]
\[ \beta = \text{Volume expansion coefficient, [K}^{-1}\text{]} \]
\[ \Delta T = \text{Temperature difference, [K]} \]

Eq. (2.28) is a criterion for fluid movements due to natural convection. Since the gravity constant, the volume expansion coefficient and the kinematics viscosity, for air are fairly constant in the normal temperature range. Grashof’s number will depend strongly on the geometry represented by the characteristic length and the temperature difference.

2.3.2 Radiation

Material surfaces emit long wave radiation. The emitted radiation is a function of the temperature on the surface. The characteristics of the radiation are depending on the properties of the surface material and on the surface temperature. When treating radiation from a real body surface, a first step is to find the radiation from a black body at the same temperature. A black body is defined as a body with a surface that absorbs all incident radiation for all wave lengths, directions and polarisation. The radiation from the real body can then be expressed as the black body radiation multiplied by the emissivity of the real surface. Black body total radiant exitance is expressed by Stefan Boltzmann’s law, according to Eq. (2.29). Stefan Boltzmann’s law states that the total energy radiated per unit surface area of a black body per unit time directly proportional to the fourth power of the black body thermodynamics absolute temperature.

\[ M^0 = \sigma T^4 \]  
(2.29)

where:
\[ M^0 = \text{Black body irradiance or emissive power, [W/m}^2\text{]} \]
$\sigma =$ Stefan Boltzmann’s constant, \([W/(m^2K^4)]\)

$T =$ Black body absolute temperature, \([K]\)

Stefan Boltzmann’s constant, $\sigma$, is used in the finite element method for calculating the density of heat flow input to a body, along a surface, due to radiation. The emissivity of a surface is defined, according to Eq. (2.30), as the ratio between the emitted radiation and the radiation of a black body at the same temperature.

$$\varepsilon = \frac{M}{M^0}$$  \hspace{1cm} (2.30)

where:

$\varepsilon =$ Emissivity of a surface

$M =$ Total radiant exitance of the considered surface, \([W/m^2]\)

**Radiation boundary condition**

According to Bathe (2009), the heat flux associated with radiation can be calculated according to Eq. (2.31).

$$q_s = \kappa(T_r - T_s)$$  \hspace{1cm} (2.31)

where:

$T_r =$ Temperature of the external radiation source, \([K]\)

The coefficient, $\kappa$, is given by Eq. (2.32).

$$\kappa = h_r(T_r^2 + T_s^2)(T_r + T_s)$$  \hspace{1cm} (2.32)

where:

$h_r =$ Heat transfer coefficient due to radiation, \([W/(m^2K)]\]

The heat transfer coefficient due to radiation is determined from Stefan Boltzmann’s constant, the emissivity of the radiant and absorbing materials and the geometric view factors. Eq. (2.31) and (2.32) can be written as Eq. (2.33).

$$q_s = \sigma f\varepsilon(T_r^4 - T_s^4)$$  \hspace{1cm} (2.33)
Chapter 3

Post-tensioning and FEM

In this chapter, general aspects about post-tensioning systems are discussed and the theory behind the calculation of prestress losses is presented. General aspect about the finite element method is presented.

3.1 Post-tensioning systems

World-wide, both grouted and ungrouted tendon ducts are used. In Sweden, half of the nuclear power plants contain ungrouted tendon ducts. The ungrouted choice is based on a condition that all tendons must be inspected for defects e.g. corrosion, Bangash (1982). The Swedish State Power Board used a specially designed dehumidification and ventilation system for all the containment tendons of Forsmark F1, F2 and F3, Aeberhard et al. (1992). The intention of a well functioning dehumidification system is to prevent corrosion. This is done by blowing dry air through the duct, keeping the atmosphere in the duct dry and preventing corrosion from occurring, Bloomstine and Sørensen (2006). For the studied object Forsmark F1, a fan is used to blow dry reactor building air into the tendon duct from one end. Without the corrosion problematic the level of prestress loss due to damage on the tendons will decrease. According to Andersson (2007), the overall influence of corrosion in the Swedish containments on the measured tendon forces is assumed to be negligible.

3.1.1 Calculating prestress losses

Prestressed tendons will be exposed to pure tension. The intent of prestress is to achieve high traction force in the tendons. The prestressed tendons will counterbalance tensile stresses in the containment structure by inducing high compressive stresses to the concrete. Due to unavoidable effects, so called prestress losses, the level of prestress will decrease. The prestress losses consist mainly of:

- Losses due to locking (anchoring) at the tensioning stage
• Losses due to friction between strands and the protective duct
• Time-dependent losses (creep, relaxation and shrinkage)

**Friction losses**

Normally the alignment of the tendons is of the type that the tendon will be in direct contact with the duct. At the tensioning stage, friction between the duct and the tendon will reduce the force from the tensioning end towards the midpoint of the duct in the cylinder wall, Petersson and Sundquist (2001). Friction influences all tendons, even nominally straight tendons are in practice influenced due to wobbling. For the vertically prestressed tendon it is normally assumed that no friction losses occur. The horizontally prestressed tendons have almost constant curvature where the wobble effects are included in the friction coefficient. The horizontally prestressed tendons in containment walls have a total bending angle of more than 360°, with a significant friction loss as a result, Andersson (2007). According to M. Collins and D. Mitchell (1991), the force balance for the variation in axial force in a tendon influenced by friction is according to Eq. (3.1).

\[ F(\alpha) = F_0 e^{\pm \mu \alpha} \]  \hspace{1cm} (3.1)

where:

- \( F_0 \) = Force at the active end where the tendon is tensioned, [N]
- \( \mu \) = Friction coefficient between the tendon and the duct
- \( \alpha \) = Accumulated change of angle along the tendon, [Radian]

The ± sign of the exponent defines the direction of the friction force acting on the tendon. A positive value indicates a direction towards the end. To this friction loss a certain wobbling effect, dependent on variation in the duct and the tendon position, is included, Petersson and Sundquist (2001). This loss is usually assumed to be dependent of the tendon length according to the combined equation, Eq. (3.2).

\[ F(\alpha) = F_0 e^{-(\mu |\alpha| + k_1 s)} \]  \hspace{1cm} (3.2)

where:

- \( k_1 \) = Coefficient dependent on the tendon system design (wobbling)
- \( s \) = Distance (arc length) along the tendon, from the tensioned end to the measured point, [m]

According to Eq. (3.2), friction force will only be made active or changed if there is a relative displacement between the tendon and the duct. Practically, the displacement magnitude has some importance since the full friction does not occur at zero displacement, Andersson (2007). Fig. 3.1 illustrates a force distribution in a tendon, tensioned at its end anchorage. The negative friction force along the whole tendon is because the tendon is sliding towards the active end. For a constant radius, the
accumulated change of angle along the tendon can be replaced with $x/R$, where $x$ is the distance from the tendon end. By calculating the integral of the curve in Fig. 3.1 and divide it with the total tendon length, the average force, $F_a$, along the tendon is obtained.

\[
F_a = \frac{\int F \, dx}{L} = \frac{\Delta \mu_0}{EA}
\]

where:
- $I_f$: Integrated force change, [N]
- $\Delta \mu_0$: Total elongation at the active end
- $E$: Young’s modulus, [Pa]
- $A$: Cross section area of the tendon, [m²]

When the tensioning is completed and the tendon is anchored, a change in length occurs ($\Delta \mu_1$) with a decreased force ($\Delta F = F_0 - F_1$) at the tendon end as a result. Fig. 3.2 illustrates the force distribution due to a slip where the tendon will slide from the end towards a point $x_1$. This leads to a positive, increasing friction force for this part of the tendon. The tendons are conscious tensioned to a higher stress level than intended. The tendons are then released with a more smoothen stress distribution as a result.

Figure 3.1: Force-length diagram of a tensioned tendon.

The integral $I_f$ of the curve in Fig. 3.1 is calculated according to Eq. (3.3)

\[
I_f = \Delta \mu_0 EA
\]

Figure 3.2: Force-length diagram of a tensioned tendon with anchor slip.
CHAPTER 3. POST-TENSIONING AND FEM

Non-uniform elongation in the nuclear containments occurs due to long time losses along the tendons. Varying long time losses are i.e. temperature variations, concrete stress and thickness, moisture content, etc. Therefore, the calculated average force loss along the tendon is normally different from the measured force loss at the end of the tendon. (Andersson, 2007)

3.2 Finite element method

This section describes the finite element method (FEM) and the FE program SOLVIA.

General about the Finite element method (FEM)

The Finite element method (FEM) is a numerical method for solving physical problems. A physical problem often consists of different variables. The variables are dependent and distributed in a coordinate system, describing the room. In mathematical terms, a physical problem is described by differential equations or by integral expressions. These descriptions are being used to define the finite element formulations. These formulations are usually written in a ready to use form and implemented in general purpose finite element analysis programs. Finite elements can be described as small pieces, which together describes a structure. A structure defines a geometrical body or a region of a material or fluid. Each finite element has a physical quantity with a simple variation in the coordinate system. This means that a finite element analysis provides an approximate solution. The actual variation of a physical problem in a region spanned by an element is often complex and is guaranteed to be more complicated than the simplifications, finite element analysis provides. Even though, the results of a finite element analysis can be near the truth of the more complex reality. The finite elements are interconnected at points, called nodes. The arrangement of the elements is the FE mesh, which is numerically represented by a system of equations. The equations are algebraic with unknowns. The unknown values are solved at the element nodes. The nodal unknowns are values of the physical quantity which depends on the specific element type. A physical quantity over the structure is approximated element by element and is the solution for nodal quantities combined with the assumed field in any given element. This determines the variation in the coordinate system (room) of the field in that element. The accuracy of the solution can be improved by refining the mesh. (Cook et al., 2002)

SOLVIA FE Program

SOLVIA is a finite element program for linear and non-linear analysis of displacements, stresses and temperatures under static or dynamic conditions. The SOLVIA system consists of:
• SOLVIA-PRE: For input generation
• SOLVIA: For stress/displacement analysis
• SOLVIA-TEMP: For temperature analysis
• SOLVIA-POST: For post-processing of the results

All input to SOLVIA and SOLVIA-TEMP is generated by the SOLVIA-PRE program. The analysis is stored in the SOLVIA-PRE database and the model can be built up in any logical sequence of input to SOLVIA-PRE. The SOLVIA-PRE input is free format and is given as commands, parameters and data lines. A separate temperature analysis is made by building up a model in SOLVIA-PRE, which generates all the input to SOLVIA-TEMP. SOLVIA-TEMP is the heat transfer analysis part of SOLVIA with set linear, steady state or transient heat transfer analysis. In SOLVIA-TEMP, conduction and boundary convection and boundary radiation conditions can be taken into account. The SOLVIA-TEMP program calculates temperatures that can be loaded into the stress analysis in SOLVIA. According to Cook et al. (2002), this is an indirect or sequential coupling in which only the temperatures influence stresses but the stresses have negligible influences on the temperatures. SOLVIA-PRE generates separate input to SOLVIA and SOLVIA-TEMP. SOLVIA and SOLVIA-TEMP data is used together in a coupled analysis. The coupled analysis generates input to SOLVIA-POST, where SOLVIA-POST is used for displaying the results, SOLVIA FE System (2008). Fig. 3.3 shows the execution sequence of SOLVIA-PRE, SOLVIA including SOLVIA-TEMP and SOLVIA POST.

![SOLVIA execution sequence](image)

Figure 3.3: SOLVIA execution sequence.
Chapter 4

Case Study: Forsmark I

Forsmark F1 is a light water reactor of BWR type. ASEA-Atom selected containments with prestressed concrete and steel liner for the Nordic boil water reactors. They had GE:s Mark I as a sort of reference. The containment in Forsmark F1 is similar to GE:s Mark II confinements, described in Sec. 1.1. The containment has a building structure of prestressed concrete with steel liner. The prestressing load was chosen to prevent cracking in the concrete up to a level of one and a half time the construction pressure level. The cracking could be controlled with moderate reinforcement amounts, for over-pressure up to a level high above the construction pressure, and with moderate strains in the steel liner. The structure required pre-stress in the vertical and horizontal directions of the cylinder wall and the long sides of the pool constructions. Fig. 4.1 illustrates a two-dimensional section of Forsmark F1 and Fig. 4.2 shows a three-dimensional section of Forsmark F1.
Figure 4.1: 2D section of Forsmark F1, Roth et al. (2002).
4.1 Structural components

According to Roth et al. (2002), the structural concrete in the cylinder wall of Forsmark F1 contain conventional reinforcement with a concrete-reinforcement ratio up to approximately 0.2%. The conventional reinforcement, for all structural components, is not considered and therefore not presented in this thesis.
CHAPTER 4. CASE STUDY: FORSMARK I

Ground and bottom slab

The base structure, shown in Fig. 4.2, is composed of a lower and an upper circular ring shaped slab (ground and bottom slab). The thickness of the ground slab varies between 1.3 and 1.7 m. The bottom slab has a thickness of 1.5 m. Between the two slabs there are two cylindrical walls, both with a thickness of 1.1 m which are connected through eight radial walls, with a thickness of 1.0 m each. The vertical tendons in the cylinder wall are anchored with its passive ends in the bottom of the outer cylindrical wall.

Cylinder wall

The cylindrical wall, shown in Fig. 4.2, has an internal radius of 11.0 m and a total thickness of 1.1 m at its uniform cross section. The cross section is thicker at the connections with the bottom slab and the upper ring slab. The continual cross section is divided into three layers (starting from the inside); the inner missile protection layer, the steel liner and the outer layer. The inner missile protection consists of a 0.26 m thick concrete layer, with the primary function to protect the 0.06 m thick steel liner from internal missiles. The outer layer consists of a 0.834 m thick prestressed concrete which includes vertical and horizontal tendons. The cylinder wall has installation penetrations and personnel hatches.

Upper ring slab

The upper ring slab, shown in Fig. 4.2 and Fig. 4.3, consist of a 2.3 m thick and circular ring shaped concrete slab with a 0.2 m thick steel plate at the top, as the pool bottom. In the center of the upper ring slab there is an opening with a diameter of 8.7 m covered by a massive steel lid. The steel lid is covering and protecting the reactor tank. Besides the centric opening there is a transport opening with a diameter of 2.5 m. The transition between the steel plate and the steel lid consists of a steel ring embedded with a large amount of heavy iron anchors.

Figure 4.3: 3D top view of the upper ring slab with the transport opening.
4.1. STRUCTURAL COMPONENTS

Biological screen

The biological screen (internal structure), illustrated in Fig. 4.2, consists of the reactor tank support, the middle slab and the central parts and columns. There is a support wall, shown in Fig. 4.4, which supports the internal structure.

![Figure 4.4: 3D view of the biological screen with the support wall.](image)

Steel liner

The steel liner is shown in Fig. 4.1. The central cross section of the ground slab includes the steel liner, made of a high strength oxidation resistant (OX 520C) steel with a thickness of 0.08 m. The minimum thickness of the concrete cover is 0.25 m towards the indoor climate. The steel liner follows the inner layer of the cylindrical wall to the bottom slab. In this section the steel liner is exposed to the wetwell and consists of stainless steel (SS2343) with a thickness of 0.06 m. The steel liner is brought to a structural interaction with the concrete by shear connectors. The steel liner follows along the bottom slab up to the top of the thick connection between the cylinder wall and the bottom slab where it continues with a pressure vessel steel (SS2102) in the inner layer of the cylinder wall. The steel liner continues up to the connection between the cylinder wall and the upper ring slab where it continues in the upper ring slab up to the steel lid. (Roth et al., 2002) and (Kallersjö & Trepp AB, 1973)

Prestressed tendons

The ungrouted tendon system in Forsmark F1 is of type VSL 19 Ø 13 Supa, Kallersjö & Trepp AB (1973). The VSL-system is illustrated in Fig. 4.5.
The VSL-system consists of 19 strands per tendon where the strands have a nominal diameter of $\varnothing$ 13 mm. The strands are stretched by pulling one by one and fixed with wedges in the anchor head. The anchor head has direct contact with the bearing plate, Andersson (2007). The total number of tendons is divided into four groups:

- Horizontal and vertical tendons in the containment wall
- Horizontal and vertical tendons in the pool construction wall

The Forsmark F1 containment consists of a total 156 horizontal and 140 vertical tendons, Kallersjö & Trepp AB (1973). The horizontal containment tendons have a length of 89 m and a total bending angle of $390^\circ$. A single layer horizontal containment tendon is illustrated in Fig. 4.6.

Figure 4.6: 2D section view over one (single layer) horizontal containment tendon.
The vertical containment walls consist of 64 tendons with the length of 50 m and 76 tendons with the length of 41 m. The total bending angle of the tendons varies between 40 - 0°. The vertical containment tendons are illustrated in Fig. 4.1. The different lengths are due to the high difference between the pool construction and the upper ring slab, see Fig. 4.3.

4.2 Material properties

In this section, the used material properties for concrete and steel are presented. In this thesis, all the analyses are linear elastic and all the calculated stresses are controlled towards characteristic values. According to EC 2 (2008), the compressive strength of concrete is denoted by concrete strength classes which relate to the characteristic (5%) cylinder strength \( f_{ck} \) or the cube strength \( f_{ck,\text{cube}} \). The strength classes in EC2 are based on the characteristic cylinder strength \( f_{ck} \) determined after 28 days hardening. The characteristic cylinder strengths and the corresponding mechanical characteristics for the used concrete qualities are taken from EC2, Table 3.1 (EC 2, 2008).

4.2.1 Mechanical properties

The defined material properties for the stress/displacement analyses are set for the used isotropic linear elastic material models. Notice that the characteristic material properties have no dependency to the defined linear elastic material models. The cylinder wall consists of different concrete and steel qualities. The majority of the cylinder wall structure consists of concrete with the K500 (C40/50) quality and is therefore only presented in this thesis. The old design code for concrete structures in Sweden (BBK 79), suggest a characteristic tensile strength, \( f_{ct}=2.25 \) MPa, for concrete of quality K500. The old concrete quality corresponds to the new C40/50 quality in EC 2 (2008). EC2 suggest a characteristic tensile strength, \( f_{ctk,0.05}=2.5 \) MPa, for concrete quality C40/50. The material parameters in Tab. 4.1 are taken from Ljungkrantz et al. (1994) and EC 2 (2008).

<table>
<thead>
<tr>
<th>Description</th>
<th>Symbol:</th>
<th>Value:</th>
<th>SI unit:</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density</td>
<td>( \rho )</td>
<td>2400</td>
<td>kg/m³</td>
</tr>
<tr>
<td>Young’s modulus</td>
<td>( E_{cm} )</td>
<td>35·10⁹</td>
<td>Pa</td>
</tr>
<tr>
<td>Poisson’s ratio</td>
<td>( \nu )</td>
<td>0.2</td>
<td>-</td>
</tr>
<tr>
<td>Thermal expansion</td>
<td>( \alpha )</td>
<td>1.0·10⁻⁵</td>
<td>K⁻¹</td>
</tr>
<tr>
<td>Characteristic tensile strength</td>
<td>( f_{ctk,0.05} )</td>
<td>2.5·10⁶</td>
<td>Pa</td>
</tr>
<tr>
<td>Characteristic compressive strength</td>
<td>( f_{ck} )</td>
<td>40·10⁶</td>
<td>Pa</td>
</tr>
</tbody>
</table>

The different steel qualities in the cylinder wall, defined in the material models, are presented in Tab. 4.2 and Tab. 4.3. The material parameters in Tab. 4.2 and
Tab. 4.3 are taken from Tryckkärlnormer (1987) and Sundström (1999). The steel quality OX 520C is defined as a SS2343 due to similar material parameters.

Table 4.2: Material properties of SS2102

<table>
<thead>
<tr>
<th>Description</th>
<th>Symbol</th>
<th>Value:</th>
<th>SI unit:</th>
</tr>
</thead>
<tbody>
<tr>
<td>Young’s modulus</td>
<td>$E_{ck}$</td>
<td>210·10^9</td>
<td>Pa</td>
</tr>
<tr>
<td>Poisson’s ratio</td>
<td>$v$</td>
<td>0.3</td>
<td></td>
</tr>
<tr>
<td>Thermal expansion</td>
<td>$\alpha$</td>
<td>11.5·10^{-6}</td>
<td>K^{-1}</td>
</tr>
<tr>
<td>Density</td>
<td>$\rho$</td>
<td>7800</td>
<td>kg/m^3</td>
</tr>
</tbody>
</table>

Table 4.3: Material properties of SS2343

<table>
<thead>
<tr>
<th>Description</th>
<th>Symbol</th>
<th>Value:</th>
<th>SI unit:</th>
</tr>
</thead>
<tbody>
<tr>
<td>Young’s modulus</td>
<td>$E_{ck}$</td>
<td>200·10^9</td>
<td>Pa</td>
</tr>
<tr>
<td>Poisson’s ratio</td>
<td>$v$</td>
<td>0.3</td>
<td></td>
</tr>
<tr>
<td>Thermal expansion</td>
<td>$\alpha$</td>
<td>16·10^{-6}</td>
<td>K^{-1}</td>
</tr>
<tr>
<td>Density</td>
<td>$\rho$</td>
<td>7900</td>
<td>kg/m^3</td>
</tr>
</tbody>
</table>

4.2.2 Thermal properties

The defined material properties for the thermal heat transfer analyses are set for linear steady state and transient conditions. The material models in the steady state thermal heat transfer analyses have isotropic conductivities, this defines a constant thermal conductivity value for all directions. For the transient condition, according to Eq. (2.17), the volumetric heat capacities are calculated by Eq. (2.18). The defined materials, concrete and steel, for conduction and radiation with their specified material properties are presented in Tab. 4.4. The thermal material properties in Tab. 4.4 are taken from Ljungkrantz et al. (1994), Burström (2001), MC 90 (1993) and K. Sandin (1996).

Table 4.4: Thermal material properties of concrete and steel

<table>
<thead>
<tr>
<th>Material:</th>
<th>Description:</th>
<th>Symbol</th>
<th>Value:</th>
<th>SI unit:</th>
</tr>
</thead>
<tbody>
<tr>
<td>Concrete</td>
<td>Thermal conductivity</td>
<td>$\lambda$</td>
<td>1.7</td>
<td>W/(mK)</td>
</tr>
<tr>
<td>-</td>
<td>Emissivity</td>
<td>$\varepsilon$</td>
<td>0.85</td>
<td>-</td>
</tr>
<tr>
<td>-</td>
<td>Stefan Boltzmann’s constant</td>
<td>$\sigma$</td>
<td>5.669·10^{-8}</td>
<td>W/(m^2K^4)</td>
</tr>
<tr>
<td>-</td>
<td>Specific heat capacity</td>
<td>$c_p$</td>
<td>900</td>
<td>J/(kgK)</td>
</tr>
<tr>
<td>-</td>
<td>Volumetric heat capacity</td>
<td>$C$</td>
<td>2160000</td>
<td>J/(m^3K)</td>
</tr>
<tr>
<td>Steel</td>
<td>Thermal conductivity</td>
<td>$\lambda$</td>
<td>13.5</td>
<td>W/(mK)</td>
</tr>
<tr>
<td>-</td>
<td>Emissivity</td>
<td>$\varepsilon$</td>
<td>0.85</td>
<td>-</td>
</tr>
<tr>
<td>-</td>
<td>Stefan Boltzmann’s constant</td>
<td>$\sigma$</td>
<td>5.669·10^{-8}</td>
<td>W/(m^2K^4)</td>
</tr>
<tr>
<td>-</td>
<td>Specific heat capacity</td>
<td>$c_p$</td>
<td>500</td>
<td>J/(kgK)</td>
</tr>
<tr>
<td>-</td>
<td>Volumetric heat capacity</td>
<td>$C$</td>
<td>3900000</td>
<td>J/(m^3K)</td>
</tr>
</tbody>
</table>
The two defined fluids, air and water, for convection with their specified material properties, are presented in Tab. 4.5. The convective heat transfer coefficients for air and water are based on the hand calculations in Appendix A.2.

<table>
<thead>
<tr>
<th>Fluid</th>
<th>Description</th>
<th>Symbol</th>
<th>Value</th>
<th>SI unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Air</td>
<td>Convective heat transfer coefficient</td>
<td>$h_c$</td>
<td>5.0</td>
<td>W/(m²K)</td>
</tr>
<tr>
<td>Water</td>
<td>Convective heat transfer coefficient</td>
<td>$h_c$</td>
<td>20.0</td>
<td>W/(m²K)</td>
</tr>
</tbody>
</table>

4.3 FE models

Two different models are used in the case study of Forsmark F1:

- An axisymmetric comprehensive model of the reactor containment
- A three-dimensional conceptual model of the upper ring slab

4.3.1 Loads

In this section, the loads considered in the FE models are presented.

Thermal loads

The thermal loads in the models are applied as boundary radiation and convection or as nodal temperatures. In an axisymmetric model, the design temperatures are applied at time step one as a steady state condition representing the serviceability state of the reactor containment building. Time steps higher than time step one represents elevated temperatures higher than the design temperatures. In a three-dimensional model, the temperatures are applied in two time step intervals:

1. For a steady state condition, from time steps $0$ to $9.1\cdot10^7$ s, representing the serviceability state
2. For a transient condition, from time steps $9.1\cdot10^7$ to $9.107\cdot10^7$ s, representing a simplified internal pipe rupture design scenario

Axisymmetric model

Fig. 4.7 illustrates the climate condition inside the reactor containment building at the serviceability state. The climate condition is defined by fifteen different temperature boundary conditions. The used temperature boundary conditions in the
axisymmetric model are boundary convection and radiation or nodal temperatures along the defined lines.

Figure 4.7: Axisymmetric model with illustrating climate zones and temperature boundary conditions.

The applied temperatures in the axisymmetric model are elevated and combined into different load combinations in 4.3.1 (Load combinations).
Three-dimensional model

Fig. 4.8 illustrates the steady state temperature boundary conditions for the time step interval 0 - 9.1·10\(^7\) s in the three-dimensional model. 

![3D model with the steady state temperature boundary conditions.](image)

Figure 4.8: 3D model with the steady state temperature boundary conditions.

The bottom side of the three-dimensional model is applied with radiation and convection boundary condition. This boundary condition is defined over the bottom surface with an area representing the surface, facing the climate zone in the upper drywell. Hence, the surface covered by the intended cylinder wall is excluded. Fig. 4.9 describes the simplified design curve of the transient temperature condition for the time step interval 9.1·10\(^7\) to 9.107·10\(^7\) s in the upper drywell applied in the three-dimensional model. Only the front and bottom surfaces (with the temperatures of 50°C) facing the upper drywell are defined with the temperature variations according to the simplified design curve in Fig. 4.9. Hence, defined boundary conditions on the top and back surface (with the temperatures of 25°C) are considered as fixed temperature values.

![Simplified design curve for temperatures in the upper drywell at an internal pipe rupture scenario, ASEA-ATOM (1997).](image)

Figure 4.9: Simplified design curve for temperatures in the upper drywell at an internal pipe rupture scenario, ASEA-ATOM (1997).
Load combinations

Eight of the defined temperature boundary conditions in the axisymmetric model are set to be variable and are combined into different load combinations. These combined temperature boundary conditions represent different climate zones or boundaries that are affected from elevated temperatures inside the reactor containment building. The variable temperatures are elevated with 10% for each calculated time step. These studied load combinations represent different fictive accident scenarios. The results from the elevated temperature study will be used to find the worst temperature load case acting on the cylinder wall. The eight studied boundary conditions, defining the climate zones inside the reactor containment are; The temperature boundary conditions near the reactor tank, the upper and lower drywell and the wetwell (condensation pool and compression area). The eight studied boundary conditions are illustrated in Fig. 4.10.

- 1a) Represents a high local temperature from the reactor tank on the ambient air near the left short side of the upper ring slab
- 1b) Represents a local temperature from the reactor tank on the ambient air near the left corner of the bottom side of the upper ring slab
- 1c) Represents a local temperature from the reactor tank on the ambient air near the reactor tank support base
- 1d) Represents a local temperature from the reactor tank on the ambient air near the vertical long side of the biological screen
- 2a) Represents a global temperature on the ambient air in the upper drywell
- 2b) Represents a global temperature on the ambient air in the compression area of the wetwell
- 2c) Represents a global temperature on the ambient water in the condensation pool of the wetwell
- 3a) Represents a global temperature on the ambient air in the lower drywell

The remainder boundary conditions are considered as fixed temperatures, these represent climate zones that are not affected from elevated temperatures inside the reactor containment building.
Figure 4.10: Variable boundary conditions 1a), 1b), 1c), 1d), 2a), 2b), 2c) and 3a).
Based on these temperature loads a number of load combinations have been defined, for different elevated temperature scenarios. Each variable temperature load consists of two possible temperature variables:

1. A fixed, serviceability value (design value)
2. A variable, elevated temperature value (> design value)

With eight different variable temperature loads, each with two possible values, the total number of load combinations is $2^8 = 256$. Only a few of the load combinations will be studied. These are considered to be most likely to occur, combined, at interference or breakdowns according to the PS-principle in Sec. 1.1. These load combinations are presented in Tab. 4.6.

Table 4.6: Studied load combinations

<table>
<thead>
<tr>
<th>LC</th>
<th>1a)</th>
<th>1b)</th>
<th>1c)</th>
<th>1d)</th>
<th>2a)</th>
<th>2b)</th>
<th>2c)</th>
<th>3a)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>X</td>
<td>X</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>2</td>
<td>-</td>
<td>-</td>
<td>X</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>3</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>4</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>X</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>5</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>6</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>-</td>
<td>X</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>7</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>8</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>X</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>9</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>X</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>10</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>X</td>
<td>-</td>
</tr>
<tr>
<td>11</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>X</td>
</tr>
<tr>
<td>12</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>-</td>
</tr>
<tr>
<td>13</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
</tr>
<tr>
<td>14</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>15</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>-</td>
</tr>
<tr>
<td>16</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>-</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
</tr>
<tr>
<td>17</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>X</td>
</tr>
<tr>
<td>18</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>-</td>
<td>-</td>
<td>X</td>
</tr>
<tr>
<td>19</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
</tr>
</tbody>
</table>

Dead-weight

A mass proportional load is used to specify constant and static gravity loads acting on the structure causing a uniform inertial load on the entire model in a selected direction. The constant gravity acceleration, $g$, is set to 9.81 m/s² in the global z-direction. The mass proportional load is considered automatically in the FE program. Geometries not included in the axisymmetric model due to non-axisymmetry are replaced as pressure loads, representing the dead-weight. These pressure loads
4.3. FE MODELS

are according to Tab. 4.7. More detailed calculations of the pressure loads are presented in Appendix A.1.

Table 4.7: Applied pressure loads

<table>
<thead>
<tr>
<th>Structural part:</th>
<th>Pressure load:</th>
<th>SI unit:</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pool construction with devices</td>
<td>$5.8 \cdot 10^4$</td>
<td>Pa</td>
</tr>
<tr>
<td>Reactor tank</td>
<td>$5.4 \cdot 10^5$</td>
<td>Pa</td>
</tr>
</tbody>
</table>

The hydrostatic water pressure load in the wetwell is applied in the axisymmetric model on the sides and the bottom of the wetwell. The hydrostatic pressure is interpolated linear in the global $z$-direction and applied on the surfaces in the wetwell. Fig. 4.17 illustrates the hydrostatic pressure in the wetwell. The total hydrostatic pressure on the wetwell bottom is equal to 10.3 meters of water column ($\approx 100$ kPa).

Prestress

Vertical prestress

The vertically prestressed tendons are not possible to model due to their non-axisymmetry. Their prestress effect is considered by applying a pressure load on the upper ring slab in the axisymmetric model. This simplification is necessarily done to describe the effect of the vertical prestress in the cylinder wall. The pressure load is applied on the reactor containment cylinder wall, divided over its uniform thickness of 1.1 m, see Fig. 4.17. The magnitude of the pressure load is calculated to 5.5 MPa and it gives a compressive stress in the cylinder wall of the same magnitude. This compressive stress represents the level of prestress achieved in the cylinder wall by the vertical tendons. A detailed hand calculation of the vertical prestress level is presented in Appendix A.3.

Horizontal prestress

The horizontally prestressed tendons in the cylinder wall are not possible to include in an axisymmetric model since degrees of freedom in the tangential $x$-direction are excluded. The prestress effect of the horizontal tendons in the cylinder wall is in a simplified manner considered when evaluating the results in the contour plots. This is done by increasing the characteristic tensile strength of the concrete (2.5 MPa) with the prestress level from the horizontal tendons (11.5 MPa). Thereby tensile stresses in the cylinder wall above 14.0 MPa are considered as cracked concrete zones. A more detailed calculation of the horizontal prestress level is presented in Appendix A.4. Fig. 4.11 illustrates the force and stress distribution in the horizontal tendons in the reactor containment cylinder wall.
The dashed lines indicate the calculated average stress and force along the tendon, from the friction loss and anchor slip. The average stress is used to calculate the compressive stress in the cylinder wall from the horizontal tendons, see Appendix A.4.

### 4.3.2 Axisymmetric model

The axisymmetric model consist of plane (two-dimensional) and axisymmetric four-node elements. The elements are defined in the $yz$-plane, with the $z$-axis being the axis of rotational symmetry for axisymmetric elements. The $y$-axis represents the radial direction ($Y \geq 0$). Furthermore, the axisymmetric element models one radian of the physical domain, i.e. one radian in the circumferential direction.

#### Geometry

The axisymmetric model consists of the axisymmetric geometries of the reactor containment around the symmetry axis. These geometries include the structural parts, which are; the bottom and ground slab, the steel liner, the cylinder wall, the upper ring slab and the biological screen, as shown in Fig. 4.12.
Mesh

The mesh consists of a global element size of approximately 5 cm. It is a very detailed mesh for an axisymmetric model and is considered necessary to describe the geometry of the horizontal tendon ducts. Fig. 4.13 illustrates different meshed sections of the cylinder wall, including the steel liner and tendon ducts.
Figure 4.13: 2D mesh of the cylinder wall: a) sectional view of the transition to the upper ring slab, b) the transition to the bottom slab and c) sectional view of the middle section with the middle slab and the cylinder wall.
4.3. FE MODELS

Non-axisymmetry

A limitation with the axisymmetric model is its requirements of axisymmetry around the symmetry axis. The accuracy of the results from the axisymmetric model greatly depends on how much the geometry and loads match with the studied part. The geometry of the cylinder wall is almost completely axisymmetric and is therefore suitable to be modelled and studied with an axisymmetric model. All the non-axisymmetric geometries, illustrated in Fig. 4.1 and Fig. 4.2, are excluded from the axisymmetric model geometry. These are replaced and included as boundary conditions or applied as pressure loads, illustrated in Fig. 4.17. Giving the cylinder wall a reasonable structural behaviour. This is done without any further considerations about their structural capacity more than validating the credibility of their influence on the cylinder wall in the numerical results.

Pool construction

Fig. 4.14 illustrates the non-axisymmetric pool construction.

![3D view of the pool construction.](image)

Figure 4.14: 3D view of the pool construction.

To capture the effect of the pool construction on the cylinder wall in an axisymmetric model. The pool construction is replaced with a pressure load, representing the deadweight of the pool construction and a boundary condition. The boundary condition represents the structural support in the radial direction which the pool construction has on the upper ring slab and the cylinder wall. The limitation this assumption gives, regarding the pressure load representing the pool construction, is that the pressure load is assumed to be evenly distributed on the upper ring slab. In the real case, the pool construction load is affecting the upper ring slab according to Fig. 4.15.
The level of compressive stress on the cylinder wall structure from this applied pressure load is assumed to be in a magnitude that is accurate enough, compared to the estimated level of compressive stress from the dead-weight of the pool construction in the cylinder wall.

**Biological screen**

Fig. 4.2 and 4.4, illustrates the biological screen. The biological screen consists of holes and different discontinuity regions as well as non-axisymmetric columns and a supporting wall. These areas in the biological screen are not possible to take into consideration in the axisymmetric model. The columns are illustrated and marked with red in Fig. 4.16. These supporting parts are applied as jointed and fixed boundary conditions in the axisymmetric model.
The influence from the supporting wall on the cylinder wall is not possible to take into consideration in the axisymmetric model, both geometric or replaced with a boundary condition. This reduces the accuracy of the defined contact condition between the middle slab and the cylinder wall. The supporting wall would increase the contact surface between the middle slab and the cylinder wall and therefore decrease the contact pressure. The most important thing is to capture the correct deformation behaviour of the biological screen. Affecting the cylinder wall with contact due to temperature displacement movements and dead-weight. High levels of stress in the cylinder wall due to the defined contact condition can indicate defects in the defined contact condition applied in the axisymmetric model. Since the cylinder wall should manage the serviceability state condition easily. Unreasonable high stresses with cracked concrete in the cylinder wall due to the defined contact condition would indicate that the influence from the biological screen is not possible to consider in the axisymmetric model.

**Boundary conditions**

The fixed boundary condition on the ground slab, with fixed degrees of freedom in all the directions, represents the boundary condition to the foundation. The fixed boundary condition on the lower central part of the biological screen, with fixed degrees of freedom in both \( y \)- and \( z \)-direction, represents the excluded lower concrete columns supporting effect. The jointed boundary condition on the lower central part of the biological screen, with a fixed degree of freedom in the \( y \)-direction, represents the excluded upper concrete columns and other excluded steel and concrete details supporting effect. The jointed boundary condition on the upper ring slab, with fixed degrees of freedom in the \( y \)-direction, represents the excluded pool constructions supporting effect.
Because of axisymmetry, only the horizontal tendon ducts are possible to be considered. The purpose with the axisymmetric model is to examine the thermal cooling effect of the ventilated tendon ducts in the cylinder wall. Therefore, it is sufficient to capture an overall structural behaviour of the cylinder wall. With the possibility of studying the global influence of the ventilated tendon ducts with reference to Forsmark F1. The thermal cooling effect of the ventilated tendon ducts is preferable to be analysed in an axisymmetric model, due to the considerably simplified geometrical modelling of the tendon ducts. A potential thermal cooling effect of the ventilated tendon ducts could lower the temperature level in the cylinder wall and therefore the level of temperature induced stress. It is of interest to find out if this potential stress reducing (thermal cooling) effect of the ventilated tendon ducts is relevant to consider in global three-dimensional models. The analysis procedure was divided into three steps:
1. A temperature analysis of the axisymmetric model. The temperature boundary conditions are defined according to Fig. 4.7. The steady state condition implies a linear temperature distribution through the cross section of the cylinder wall.

2. A stress analysis of the axisymmetric model combined with the temperature analysis. This combined analysis indicates the global behaviour of the cylinder wall. As well as an indication of the stress level in the cylinder wall at the serviceability state. The serviceability state result can indicate possible defects in the model and gives a hint of the credibility of the model.

3. An elevated temperature study. Different load combinations are studied with elevated temperatures. The temperatures are elevated to a point where a cracked concrete zone in the cylinder walls cross section reaches the tendon ducts. The worst case load combination is selected and used to study the thermal cooling effect of the ventilated tendon. This worst case scenario should indicate the maximum potential of the thermal cooling effect during a simplified and fictive accident scenario.

4.3.3 Three-dimensional model

The three-dimensional model of the upper ring slab is modelled with eight-node solid elements. The geometry of the three-dimensional model is illustrated in Fig. 4.18 and consists of a 90° conceptual and simplified section of the upper ring slab. The steel liner is excluded in this model. The mesh of the three-dimensional model consists of a global element length of approximately 20 cm.

Figure 4.18: 3D mesh of the upper ring slab.
Embedded steel

The purpose with the three-dimensional conceptual model of the upper ring slab is to study the influence of embedded steel on the temperature distribution in the upper ring slab during a transient temperature condition. At Forsmark F1, the upper ring slab has embedded steel beams in the bottom layer of the cross section, Kallersjö & Trepp AB (1973). A transient temperature condition is considered to generate the highest levels of temperature differences between the embedded steel and the surrounding concrete. This is due to the higher conductivity and lower specific heat capacity in steel compared to the surrounding concrete. Resulting in faster temperature changes in the embedded steel. It is of interest to examine if the embedded steel has any negative effects on the upper ring slab by increasing the temperature differences with temperature induced stresses as a result. The model is conceptual in the sense that only the concrete geometries are initially modelled, e.g. the steel liner is excluded. Some structural simplifications are made to the concrete geometry in the three-dimensional model compared to the in-situ conditions, e.g. the simplified front side geometry. These simplifications and modelling details are considered acceptable. The transient condition represents a fictive single internal pipe rupture scenario in the upper dry well. The internal pipe rupture scenario is evaluated with temperatures according to the simplified design curve, see Fig. 4.9. The temperature study consists of three models:

1. One without embedded steel, this is the initial condition (reference case) to which the results are compared

2. One with embedded steel beams in the middle of the cross section of the upper ring slab. The steel beams are positioned so that they are exposed with their smallest cross section to the environment in the upper drywell. This enables as much temperature distributing effects as possible, vertically along the embedded steel beams into the upper ring slab. The result from the temperature analysis is coupled with a separate stress analysis, making it possible to study the temperature induced stresses in the upper ring slab, from the applied temperatures only

3. One with embedded steel beams in the bottom layer of the cross section of the upper ring slab. This condition represents an exaggerated but representative condition at Forsmark F1. The condition is considered exaggerated due to the steel beams positioning in the bottom layer without any concrete cover compared to the in-situ condition at Forsmark F1. Combined with the larger cross section area of the modelled steel beams compared to the slim I-profiles in the in-situ condition. The result from the temperature analysis is coupled with a separate stress analysis, making it possible to study the temperature induced stresses in the upper ring slab (from applied temperatures only)
Chapter 5

Numerical Results

In this chapter, the numerical results from the axisymmetric and three-dimensional models are presented.

5.1 Axisymmetric model

5.1.1 Dead-weight

The influence of dead-weight study includes all boundary conditions, the mass proportional loads and the pressure loads acting on the cylinder wall. Fig. 5.1 shows the deformational behaviour, with increased deformation scale, of the axisymmetric model under the influence of dead-weight.

![Figure 5.1: Influence of dead-weight: Deformation behaviour (with increased deformation scale).](image-url)
Fig. 5.2 shows the maximum principal stress in the axisymmetric model, with focus on the cylinder wall, due to the influence of dead-weight.

![Figure 5.2: Influence of dead-weight: Maximum principal stress.](image)

The cylinder wall doesn’t indicate any problems due to the influence of dead-weight.
The compressive stress from the dead-weight in the cylinder wall is approximately -1.0 MPa. It is of interest to find the influence from the dead-weight on the contact condition between the middle slab and the cylinder wall. A fictive reaction force is calculated for the node in the left top corner of the reactor tank support base, see Fig 5.3. This makes it possible to get an indication of the magnitude, from the studied parameter, on the force due to displacement from the biological screen. The possible contact force between the middle slab and cylinder wall is also studied, with the requirement that both surfaces meet. Therefore, both these aspects are studied, guaranteeing an indication of the studied parameters impacts on the cylinder wall.

The calculated reaction force for node 94565 in the y-direction is approximately 2.37 MN. This indicates in the static analysis that the biological screen has a tendency to move towards the opposite negative (left) direction due to the dead-weight. Hence, resulting in a contact force equal to zero between the middle slab and the cylinder wall. The physical modelled distance between the middle slab and the cylinder wall is initially 1.2 cm. If the studied parameter impacts the biological screen to displacement towards the positive (right) direction, there is a possibility for contact between the middle slab and the cylinder wall.

5.1.2 Prestress

The influence of vertical prestress is combined with the dead-weight. This is done to give a total indication on the stress level in the cylinder wall from the static loads before adding the temperatures.
Fig. 5.4 shows the minimum principal stresses in the axisymmetric model, due to the vertical prestress and the dead-weight.

The compressive stress in the cylinder wall from the prestress combined with the dead-weight is approximately -7.5 MPa.
5.1.3 Design temperatures

The influence of design temperatures and the dead-weight is studied. The design temperatures represent the serviceability state and are combined with the dead-weight to obtain a more realistic deformation of the axisymmetric model. Fig. 5.5 shows the deformational behaviour, with increased deformation scale, of the axisymmetric model under the influence of the design temperatures and the dead-weight.

Figure 5.5: Influence of design temperatures and dead-weight: Deformation behaviour (with increased deformation scale).

Fig. 5.6 shows the contact force between the middle slab and the cylinder wall, due to the influence of the design temperatures and the deadweight.

Figure 5.6: Influence of design temperatures and dead-weight: Contact force.
The contact force due to the design temperatures is approximately 71 MN. The calculated fictive reaction force, accordingly to Fig. 5.7, for node 94565, see Fig. 5.3, is approximately -161.36 MN.

![Figure 5.7: Calculated fictive reaction force in the y-direction for node 94565.](image)

This indicates in the static analysis, as well as in Fig. 5.5, that the biological screen has a tendency to move towards the opposite positive (right) direction due to the design temperatures. According to Fig. 5.7, this occurs at time step 0.4 which corresponds to 40% of the applied loads. The magnitude of the reaction force due to the design temperatures and the dead-weight is approximately 70 times bigger compared to the reaction force due to the dead-weight only.

### 5.1.4 Serviceability state

The serviceability state represents a fictive normal state of the reactor containment building at service. The serviceability state is modelled with all the defined boundary conditions and loads. The loads include the dead-weight, the vertical prestress pressure load and the design temperatures. The purpose of the analysed serviceability state is to verify the overall credibility of the axisymmetric model and to define a reference case. The reference case is used to compare the results from the other analyses and it is based on the temperatures in the reference case the elevated temperature studies are being performed. The results from the serviceability state shouldn’t indicate initial structural problems since a properly modelled model of the cylinder wall should easily be able to manage the design values. An analysis with the defined contact condition shows zones with initially higher tensile stress in the cylinder wall due to the defined contact condition with the middle slab, compared to an
analysis without the contact condition. The defined contact between the middle slab and the cylinder wall is uncertain, concerning the in-situ condition at Forsmark F1. Still it can’t be ignored, due to the initial high stress levels in the results from this defined contact condition. Hence, with a greater impact from elevated temperatures on the cylinder wall in the elevated temperature study. This is the main reason to analyse both with and without the defined contact condition since results from the analysis with the defined contact condition might be misleading towards the actual in-situ condition at Forsmark F1. Fig. 5.8 shows the temperature distribution in the axisymmetric model from the design temperatures in the serviceability state.

![Figure 5.8: Serviceability state: Temperatures.](image)

Fig. 5.9 shows the deformational behaviour, with increased deformation scale, of the axisymmetric model in the serviceability state.
CHAPTER 5. NUMERICAL RESULTS

Figure 5.9: Serviceability state: Deformation behaviour (with increased deformation scale).

Fig. 5.10 shows the stress distribution in the $x$-, $y$- and $z$-direction in the cylinder wall, where the stress in the $z$-direction initially shows higher stress levels.

Figure 5.10: Serviceability state: Stress in the $x$-, $y$- and $z$-directions.
The calculated contact force, according to Fig. 5.11, between the middle slab and the cylinder wall is approximately 72.62 MN. This contact force causes higher stress zones locally, in the $z$-direction on the outer side of the cylinder wall.

5.1.5 Elevated temperatures

With elevated temperatures, the worst load combination on the cylinder wall is found and later used to study the influence of the thermal cooling effect of the ventilated tendon ducts in Sec. 5.1.6. The calculations showed that the worst load combination acting on the cylinder wall is LC15. LC15 is defined with and without the defined contact condition. This is done to ensure the two possible in-situ conditions at Forsmark F1 where the uncertainty of the defined contact condition is complemented by excluding it. Hence, if the defined contact condition between the middle slab and the cylinder wall doesn’t exist in the in-situ condition, the results of the thesis don’t have to be neglected since the second alternative ensures the credibility of the results.

LC15 - with the defined contact

LC15, illustrated in Fig. 5.12, represents a local temperature increase on the biological screen and the upper ring slab and a global temperature increase in the upper drywell, the compression area and the condensation pool of the wetwell. This scenario can be represented by a fictive first stage of the PS-principle, due to an internal pipe rupture near the reactor tank, or by a fictive long term electricity shortfall scenario. Notice that this is a steady state condition where the sequence is assumed to last for a longer time compared to a faster transient condition. The elevated critical temperature level is mainly restricted by the defined contact between the middle slab and the cylinder wall.
Figure 5.12: LC15: Variable and locked temperature loads.
5.1. AXISYMMETRIC MODEL

Fig. 5.13 shows the stress distribution in the $z$-direction for the cylinder wall at time steps 1.1, 1.2, 1.3 and 1.4.

Figure 5.13: LC15: Stress in the $z$-direction at time steps 1.1, 1.2, 1.3 and 1.4.

Figure 5.13: LC15: Stress in the $z$-direction at time steps 1.1, 1.2, 1.3 and 1.4.
The different time steps are elevated with the temperatures:

- Time step 1.1, (110%) = 110, 66, 66, 66, 55, 38.5 and 22°C
- Time step 1.2, (120%) = 120, 72, 72, 72, 60, 42 and 24°C
- Time step 1.3, (130%) = 130, 78, 78, 78, 65, 45.5 and 26°C
- Time step 1.4, (140%) = 140, 84, 84, 84, 70, 49 and 28°C

The results from the elevated temperatures, with the indicated cracked concrete zones in the negative (left) y-direction, are according to Tab 5.1.

Table 5.1: Indicated cracked concrete zones in the negative (left) y-direction

<table>
<thead>
<tr>
<th>Time step</th>
<th>Cracked zone</th>
<th>SI unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.1</td>
<td>0.13 m</td>
<td>m</td>
</tr>
<tr>
<td>1.2</td>
<td>0.27 m</td>
<td>m</td>
</tr>
<tr>
<td>1.3</td>
<td>0.33 m</td>
<td>m</td>
</tr>
<tr>
<td>1.4</td>
<td>0.40 m</td>
<td>m</td>
</tr>
</tbody>
</table>

Time step 1.3 has here been defined as the upper critical level where the cracked concrete zone in the cylinder wall is no longer considered acceptable. Since the depth of the cracked zone has reached the first tendon row. It is towards this load level the influence of the thermal cooling effect of the ventilated tendon ducts is considered.

**LC15 - without the defined contact**

Fig. 5.14 shows the stress distribution in the z-direction in the cylinder wall at time steps 1.6, 1.7, 1.8 and 1.9.
Figure 5.14: LC15: Stress in the $z$-direction at time steps 1.6, 1.7, 1.8 and 1.9.
The different time steps are elevated with the temperatures:

- Time step 1.6, (160%) = 160, 96, 96, 96, 80, 56 and 32°C
- Time step 1.7, (170%) = 170, 102, 102, 102, 85, 59.5 and 34°C
- Time step 1.8, (180%) = 180, 108, 108, 108, 90, 63 and 36°C
- Time step 1.9, (190%) = 190, 114, 114, 114, 95, 66.5 and 38°C

The results from the elevated temperatures, with the indicated cracked concrete zones in the negative (left) \( y \)-direction, are according to Tab 5.2.

<table>
<thead>
<tr>
<th>Time step</th>
<th>Cracked zone: SI unit:</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.6</td>
<td>0.05 m</td>
</tr>
<tr>
<td>1.7</td>
<td>0.11 m</td>
</tr>
<tr>
<td>1.8</td>
<td>0.35 m</td>
</tr>
<tr>
<td>1.9</td>
<td>0.50 m</td>
</tr>
</tbody>
</table>

The drastic increase in cracked concrete depth at time steps 1.8 and 1.9 is due to the local crack growth around the discontinuity in geometry representing the tendon ducts. Time step 1.8 has here been defined as the upper critical level where the cracked concrete zone in the cylinder wall is no longer considered acceptable. Since the depth of the cracked zone has reached the first tendon row. It is towards this load level the influence of the thermal cooling effect of the ventilated tendon ducts is considered.

5.1.6 Thermal cooling effect of the ventilated tendon ducts

The influence of the thermal cooling effect is studied in two analyses; with and without the thermal cooling effect. These two analyses results are then compared with one another. This comparison makes it possible to study the influence of the thermal cooling effect of the ventilated tendon ducts in the cylinder wall. This comparison study is made twice for LC15, with and without the defined contact condition between the middle slab and the cylinder wall.

LC15(with contact): Without the thermal cooling effect

LC15 is elevated without the thermal cooling effect of the ventilated tendon ducts. Fig. 5.15 shows the difference in stress, in the \( z \)-direction, with and without the thermal cooling effect of the ventilated tendon ducts at time steps 1.1, 1.2 and 1.3.
Figure 5.15: Without (left side) and with (right side) the thermal cooling effect:
Stress in the z-direction at time steps 1.1, 1.2 and 1.3.
Fig. 5.16 shows the temperature distribution in the cylinder wall, with and without the thermal cooling effect of the ventilated tendon ducts at time step 1.3.

Figure 5.16: Without (top) and with (bottom) the thermal cooling effect: Temperature distribution at time step 1.3.
Fig. 5.17 shows the stress in the $z$-direction with plotted principal stress directions at time step 1.3.

The results indicate that the thermal cooling effect of the ventilated tendon ducts decreases the cracked concrete zone, in the vertical $z$-directions along the outer side of the cylinder wall compared to the results without the thermal cooling effect, see Fig. 5.15. The vertical level of the decreased cracked concrete zone under the influence of the thermal cooling effect is according to Tab. 5.3.
Table 5.3: Decreased level of cracked concrete zone in the vertical \( z \)-directions

<table>
<thead>
<tr>
<th>Time step</th>
<th>Decreased level %</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.1</td>
<td>48.6</td>
</tr>
<tr>
<td>1.2</td>
<td>29.6</td>
</tr>
<tr>
<td>1.3</td>
<td>31.6</td>
</tr>
</tbody>
</table>

Interesting to notice, while the ventilated tendon ducts reduces the extent of cracking in the vertical directions. The thermal cooling effect of the ventilated tendon ducts marginally increases the distributed level of the cracked concrete zone in the horizontal negative \( y \)-direction, towards the tendon ducts in the cylinder wall, see Fig. 5.15. This behaviour is due to the influence of the defined contact condition. Looking at the temperature distribution without the thermal cooling effect in Fig. 5.16 you can see a remarkable smaller layer with 20\(^\circ\)C concrete compared to the one including the thermal cooling effect. With the thermal cooling effect the layer with 20\(^\circ\)C concrete almost covers half of the cross section of the cylinder wall. Fig. 5.17 shows the principal stress with plotted directions in the cylinder wall structure. This indicates the direction of the crack growth, radially directed through the cross section of the cylinder wall, perpendicular to the principal stress direction. Tab. 5.4 presents the stress in the \( z \)-direction at the integration point 1 at time steps 1.1, 1.2 and 1.3 for different, handpicked elements according to Fig. 5.18, when the thermal cooling effect of the ventilated tendon ducts is and is not considered.

Figure 5.18: Studied elements: Element numbers with height coordinates.
Table 5.4: Without and with the thermal cooling effect

<table>
<thead>
<tr>
<th>EL nr</th>
<th>Time step</th>
<th>Without</th>
<th>With</th>
<th>Unit</th>
<th>± %</th>
</tr>
</thead>
<tbody>
<tr>
<td>58962</td>
<td>1.1</td>
<td>0.90</td>
<td>0.87</td>
<td>MPa</td>
<td>-3.0%</td>
</tr>
<tr>
<td></td>
<td>1.2</td>
<td>2.79</td>
<td>2.81</td>
<td>MPa</td>
<td>+0.6%</td>
</tr>
<tr>
<td></td>
<td>1.3</td>
<td>4.68</td>
<td>4.75</td>
<td>MPa</td>
<td>+1.4%</td>
</tr>
<tr>
<td>59032</td>
<td>1.1</td>
<td>1.53</td>
<td>1.46</td>
<td>MPa</td>
<td>-5.0%</td>
</tr>
<tr>
<td></td>
<td>1.2</td>
<td>3.51</td>
<td>3.47</td>
<td>MPa</td>
<td>-1.1%</td>
</tr>
<tr>
<td></td>
<td>1.3</td>
<td>5.49</td>
<td>5.49</td>
<td>MPa</td>
<td>0.0%</td>
</tr>
<tr>
<td>58978</td>
<td>1.1</td>
<td>1.74</td>
<td>1.84</td>
<td>MPa</td>
<td>+5.5%</td>
</tr>
<tr>
<td></td>
<td>1.2</td>
<td>3.91</td>
<td>4.07</td>
<td>MPa</td>
<td>+4.3%</td>
</tr>
<tr>
<td></td>
<td>1.3</td>
<td>6.08</td>
<td>6.32</td>
<td>MPa</td>
<td>+3.9%</td>
</tr>
<tr>
<td>58992</td>
<td>1.1</td>
<td>1.64</td>
<td>1.75</td>
<td>MPa</td>
<td>+6.8%</td>
</tr>
<tr>
<td></td>
<td>1.2</td>
<td>3.80</td>
<td>3.99</td>
<td>MPa</td>
<td>+5.0%</td>
</tr>
<tr>
<td></td>
<td>1.3</td>
<td>5.96</td>
<td>6.23</td>
<td>MPa</td>
<td>+4.5%</td>
</tr>
<tr>
<td>59010</td>
<td>1.1</td>
<td>1.40</td>
<td>1.45</td>
<td>MPa</td>
<td>+3.5%</td>
</tr>
<tr>
<td></td>
<td>1.2</td>
<td>3.43</td>
<td>3.55</td>
<td>MPa</td>
<td>+3.3%</td>
</tr>
<tr>
<td></td>
<td>1.3</td>
<td>5.47</td>
<td>5.64</td>
<td>MPa</td>
<td>+3.3%</td>
</tr>
<tr>
<td>58961</td>
<td>1.1</td>
<td>1.37</td>
<td>1.25</td>
<td>MPa</td>
<td>-9.3%</td>
</tr>
<tr>
<td></td>
<td>1.2</td>
<td>3.30</td>
<td>3.20</td>
<td>MPa</td>
<td>-3.1%</td>
</tr>
<tr>
<td></td>
<td>1.3</td>
<td>5.24</td>
<td>5.16</td>
<td>MPa</td>
<td>-1.5%</td>
</tr>
<tr>
<td>58968</td>
<td>1.1</td>
<td>0.69</td>
<td>0.51</td>
<td>MPa</td>
<td>-25.8%</td>
</tr>
<tr>
<td></td>
<td>1.2</td>
<td>2.49</td>
<td>2.32</td>
<td>MPa</td>
<td>-6.8%</td>
</tr>
<tr>
<td></td>
<td>1.3</td>
<td>4.29</td>
<td>4.14</td>
<td>MPa</td>
<td>-3.7%</td>
</tr>
<tr>
<td>59005</td>
<td>1.1</td>
<td>0.42</td>
<td>0.14</td>
<td>MPa</td>
<td>-65.9%</td>
</tr>
<tr>
<td></td>
<td>1.2</td>
<td>2.18</td>
<td>1.89</td>
<td>MPa</td>
<td>-13.1%</td>
</tr>
<tr>
<td></td>
<td>1.3</td>
<td>3.94</td>
<td>3.65</td>
<td>MPa</td>
<td>-7.5%</td>
</tr>
</tbody>
</table>

The results from Tab. 5.4 are illustrated with a graph in Fig. 5.19.
Fig. 5.19 makes it possible to graphically monitoring the stress differences due to the influence of the thermal cooling effect. The results indicate marginal changes in the stress level, both increases as well as decreases, in the studied elements next to the geometries representing the tendon ducts. This behaviour is due to the defined contact condition and no direct conclusions about any stress reducing effect can be made. The overall conclusion is that the thermal cooling effect of the ventilated tendon ducts reduces the distribution of the cracked concrete zone, vertically along the cylinder wall, with a noticeable magnitude. Therefore, the thermal cooling effect of the ventilated tendon ducts is considered relevant to take into account in global three-dimensional models.

LC15(without contact): Without the thermal cooling effect

Fig. 5.20 shows the difference in stresses in the $z$-direction, with and without the thermal cooling effect of the ventilated tendon ducts at time steps 1.7, 1.8 and 1.9.
Figure 5.20: Without (left side) and with (right side) the thermal cooling effect: stress in the $z$-direction at time steps 1.7, 1.8 and 1.9.
Fig. 5.21 shows the temperature distribution in the cylinder wall, with and without the thermal cooling effect of the ventilated tendon ducts at time step 1.9.

The results indicate that the thermal cooling effect of the ventilated tendon ducts markedly decreases the distribution level of the cracked concrete zone, in the vertical \( z \)-directions along the outer side of the cylinder wall compared to the results without the thermal cooling effect, see Fig. 5.20. The vertical level of the decreased cracked concrete zone under the influence of the thermal cooling effect is according to Tab. 5.5.

Table 5.5: Decreased level of cracked concrete zone in the vertical \( z \)-directions

<table>
<thead>
<tr>
<th>Time step</th>
<th>Decreased level %</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.7</td>
<td>81.1</td>
</tr>
<tr>
<td>1.8</td>
<td>67.6</td>
</tr>
<tr>
<td>1.9</td>
<td>49.1</td>
</tr>
</tbody>
</table>

The thermal cooling effect of the ventilated tendon ducts noticeable decreases, see
5.1. AXISYMMETRIC MODEL

Fig. 5.20, the horizontally cracked concrete zone distribution at time step 1.7 (170%). For time steps 1.8 and 1.9 this positive effect is decreased due to locally cracked concrete around the discontinuity regions, representing the tendon duct geometries, markedly increasing the distributed zone inwards the cross section of the cylinder wall. Overall, the horizontally distribution of the cracked concrete zone is decreased due to the influence of the thermal cooling effect. Looking at the temperature distribution without the thermal cooling effect in Fig. 5.21 you can see a remarkable smaller layer with 20°C concrete compared to the one including the thermal cooling effect. With the thermal cooling effect the layer with 20°C concrete almost covers half of the cross section of the cylinder wall. Tab. 5.6 presents the stress in the \( z \)-direction at the integration point 1 at time steps 1.7, 1.8 and 1.9 for different (handpicked) elements according to Fig. 5.18, when the thermal cooling effect of the ventilated tendon ducts is and is not considered.

Table 5.6: Without and with the thermal cooling effect

<table>
<thead>
<tr>
<th>EL nr</th>
<th>Time step</th>
<th>Without:</th>
<th>With:</th>
<th>Unit:</th>
<th>± %</th>
</tr>
</thead>
<tbody>
<tr>
<td>58962</td>
<td>1.7</td>
<td>3.68</td>
<td>2.03</td>
<td>MPa</td>
<td>( \approx -45.0 % )</td>
</tr>
<tr>
<td></td>
<td>1.8</td>
<td>4.58</td>
<td>2.81</td>
<td>MPa</td>
<td>( \approx -38.6 % )</td>
</tr>
<tr>
<td></td>
<td>1.9</td>
<td>5.48</td>
<td>3.61</td>
<td>MPa</td>
<td>( \approx -34.2 % )</td>
</tr>
<tr>
<td>59032</td>
<td>1.7</td>
<td>3.86</td>
<td>1.89</td>
<td>MPa</td>
<td>( \approx -50.9 % )</td>
</tr>
<tr>
<td></td>
<td>1.8</td>
<td>4.73</td>
<td>2.62</td>
<td>MPa</td>
<td>( \approx -44.5 % )</td>
</tr>
<tr>
<td></td>
<td>1.9</td>
<td>5.60</td>
<td>3.36</td>
<td>MPa</td>
<td>( \approx -40.0 % )</td>
</tr>
<tr>
<td>58978</td>
<td>1.7</td>
<td>3.87</td>
<td>1.99</td>
<td>MPa</td>
<td>( \approx -48.7 % )</td>
</tr>
<tr>
<td></td>
<td>1.8</td>
<td>4.77</td>
<td>2.75</td>
<td>MPa</td>
<td>( \approx -42.3 % )</td>
</tr>
<tr>
<td></td>
<td>1.9</td>
<td>5.68</td>
<td>3.53</td>
<td>MPa</td>
<td>( \approx -37.9 % )</td>
</tr>
<tr>
<td>58992</td>
<td>1.7</td>
<td>3.78</td>
<td>1.98</td>
<td>MPa</td>
<td>( \approx -47.6 % )</td>
</tr>
<tr>
<td></td>
<td>1.8</td>
<td>4.67</td>
<td>2.75</td>
<td>MPa</td>
<td>( \approx -41.2 % )</td>
</tr>
<tr>
<td></td>
<td>1.9</td>
<td>5.58</td>
<td>3.52</td>
<td>MPa</td>
<td>( \approx -36.8 % )</td>
</tr>
<tr>
<td>59010</td>
<td>1.7</td>
<td>3.70</td>
<td>1.96</td>
<td>MPa</td>
<td>( \approx -47.0 % )</td>
</tr>
<tr>
<td></td>
<td>1.8</td>
<td>4.58</td>
<td>2.71</td>
<td>MPa</td>
<td>( \approx -40.8 % )</td>
</tr>
<tr>
<td></td>
<td>1.9</td>
<td>5.46</td>
<td>3.47</td>
<td>MPa</td>
<td>( \approx -36.5 % )</td>
</tr>
<tr>
<td>58961</td>
<td>1.7</td>
<td>3.97</td>
<td>2.01</td>
<td>MPa</td>
<td>( \approx -49.3 % )</td>
</tr>
<tr>
<td></td>
<td>1.8</td>
<td>4.85</td>
<td>2.75</td>
<td>MPa</td>
<td>( \approx -43.3 % )</td>
</tr>
<tr>
<td></td>
<td>1.9</td>
<td>5.74</td>
<td>3.49</td>
<td>MPa</td>
<td>( \approx -39.2 % )</td>
</tr>
<tr>
<td>58968</td>
<td>1.7</td>
<td>3.52</td>
<td>1.66</td>
<td>MPa</td>
<td>( \approx -53.0 % )</td>
</tr>
<tr>
<td></td>
<td>1.8</td>
<td>4.40</td>
<td>2.39</td>
<td>MPa</td>
<td>( \approx -45.7 % )</td>
</tr>
<tr>
<td></td>
<td>1.9</td>
<td>5.27</td>
<td>3.12</td>
<td>MPa</td>
<td>( \approx -40.8 % )</td>
</tr>
<tr>
<td>59005</td>
<td>1.7</td>
<td>3.55</td>
<td>1.59</td>
<td>MPa</td>
<td>( \approx -55.4 % )</td>
</tr>
<tr>
<td></td>
<td>1.8</td>
<td>4.45</td>
<td>2.33</td>
<td>MPa</td>
<td>( \approx -47.6 % )</td>
</tr>
<tr>
<td></td>
<td>1.9</td>
<td>5.35</td>
<td>3.09</td>
<td>MPa</td>
<td>( \approx -42.4 % )</td>
</tr>
</tbody>
</table>

The results from Tab. 5.6 are illustrated with a graph in Fig. 5.19.
The results, illustrated in the graph in Fig. 5.19, indicate greatly reduced stress levels in the studied elements due to the influence of the thermal cooling effect. At time step 1.7 this reduction is approximately 50%, which indicates a positive stress reducing effect in the concrete, due to the influence of the thermal cooling effect of the ventilated tendon ducts. The conclusion is that the thermal cooling effect of the ventilated tendon ducts should be taken into account in global three-dimensional models.

5.2 Three-dimensional model

5.2.1 Temperature analyses

Without embedded steel - reference case

The concrete without embedded steel study is evaluated with the transient temperature condition accordingly to the graph in Fig. 4.9, at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s. The initial time steps from 0 - to $9.1 \cdot 10^7$ s represent the time
needed to guarantee a steady state temperature condition in the upper ring slab. This time span represents an approximated period of three years. Fig. 5.23 shows a vertical sectional view of the upper ring slab, dividing it in half with temperature and heat flow distribution at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s.

Figure 5.23: 3D model: Temperature and heat flow distribution in the upper ring slab at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s.
Fig. 5.24 shows a horizontal sectional view of the upper ring slab, dividing it in half with temperature distribution at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s.

Figure 5.24: 3D model: Temperature distribution in the upper ring slab at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s.
Fig. 5.25 shows a horizontal sectional view of the upper ring slab, dividing it in half at the bottom element layer with temperature and heat flow distribution at time steps $0.1 \times 10^4$, $0.4 \times 10^4$, $1 \times 10^4$ and $7 \times 10^4$ s.

Figure 5.25: 3D model: Temperature and heat flow distribution in the upper ring slab at time steps $0.1 \times 10^4$, $0.4 \times 10^4$, $1 \times 10^4$ and $7 \times 10^4$ s.
The result from the reference case indicates only higher temperatures on the surfaces facing the upper drywell. This can be expected, due to the modelled condition without any embedded steel.

**With embedded steel - in the middle section**

The temperature distribution in the upper ring slab is studied with embedded steel beams placed in the middle of the cross section of the upper ring slab. Fig. 5.26 shows the modelled distribution of the embedded steel beams, through a horizontal sectional view dividing the upper ring slab in half.

Figure 5.26: 3D model: Horizontal sectional view of the modelled steel beams in the upper ring slab.

Fig. 5.27 shows the temperature and heat flow distribution in the steel beams at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s.
Figure 5.27: 3D model: Temperature and heat flow distribution in the steel beams at time steps $0.1 \times 10^4$, $0.4 \times 10^4$, $1 \times 10^4$ and $7 \times 10^4$ s.
Fig. 5.28 shows a vertical sectional view of the upper ring slab, dividing it in half with temperature and heat flow distribution at time steps $0.1 \times 10^4$, $0.4 \times 10^4$, $1 \times 10^4$ and $7 \times 10^4$ s. The steel beams are placed according to Fig. 5.26.

Figure 5.28: 3D model: Temperature and heat flow distribution in the upper ring slab at time steps $0.1 \times 10^4$, $0.4 \times 10^4$, $1 \times 10^4$ and $7 \times 10^4$ s.
Fig. 5.29 shows a horizontal sectional view of the upper ring slab, dividing it in half at the embedded steel beams with temperature and heat flow distribution at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s. The steel beams are placed according to Fig. 5.26.

Figure 5.29: 3D model: Temperature distribution in the upper ring slab at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s.
CHAPTER 5. NUMERICAL RESULTS

The results from the embedded steel beams in the middle of the cross section of the upper ring slab, see Sec. 5.2.1, is compared with the reference case at time step $7 \times 10^4$ s. The temperature contour plots shown in Fig. 5.23 and Fig. 5.28 indicates the differences in temperatures in the vertical directions as well as for surface temperatures. Fig. 5.24 and Fig. 5.29 indicates the differences in temperatures in the horizontal directions as well as for surface temperatures. The comparisons towards the reference case indicate changes in horizontal as well as vertical directions due to the influence of embedded steel beams. The vertical sectional views of the upper ring slab, see Fig. 5.28, marginally indicate increased temperatures in the concrete, due to the influence of embedded steel beams, horizontally inwards the structure from the bottom side. The sectional view represents a cut through the concrete cross section only (without any visible steel). The vertical sectional view indicates decreased surface temperature zones at the front side, due to the influence of embedded steel beams. The surface temperatures for the steel and for the concrete surfaces close to the embedded steel beams are lower than for the rest of the surrounding concrete surfaces. The steel beams work as thermal bridges where the highly conductive steel allows the heat from the drywell to be transferred easier into the upper ring slab through the embedded beams. The heat flows through the material with the lowest resistance towards conduction, resulting in lower surface temperatures near the steel compared to the concrete with higher resistance. According to Fig. 5.27, the main thermal heat flow occurs vertically through the embedded steel beams, due to their position in the upper ring slab. This indicates a dominating vertical thermal heat flow inwards through the embedded steel beams. A comparison towards the reference case indicates an increased thermal heat flow of up to 18.5% at time step $7 \times 10^4$ s. At time step $0.4 \times 10^4$, the increase in thermal heat flow vertically inwards is up to 34.4%. The horizontal sectional view of the upper ring slab, see Fig. 5.29, indicates increased temperature levels through the embedded steel beams. This is noticeable at the temperature spikes in the temperature contour plot along the steel beams, approximately 1.5 m further into the structure compared to the reference case. The surrounding concrete between the embedded steel beams is not sensitive to the temperature changes in steel due to their larger specific heat capacity combined with the earlier applied steady state condition. This leads to small changes in the surrounding concrete temperatures compared to the temperature changes in the embedded steel beams, see detailed nodal point data in Fig. 5.30. Fig. 5.30 shows the nodal temperatures for selected nodes. The highest temperature difference in the concrete layer next to the embedded steel beam is approximately $4.5 \degree C$ for the selected nodes, i.e. at a distance of approximately 20 cm (excluding the surface nodal temperatures). The main temperature increase occurs in the embedded steel beams, increasing the temperature difference towards the surrounding concrete.
The approximated level of stress difference between the concrete and the steel, due to the temperature difference, is calculated by using Eq. 2.11 and 2.10. The calculated results are according to Eq. 5.1 and Eq. 5.2.

\[ \varepsilon_0 = \varepsilon_t = \alpha \Delta T = \alpha (T_t - T_0) \approx 1 \cdot 10^{-5} \times 4.5 \approx 4.5 \cdot 10^{-5} \quad (5.1) \]

\[ \sigma = E(\varepsilon - \varepsilon_0) \approx 35 \cdot 10^9 \times 4.5 \cdot 10^{-5} \approx 1575000 \text{ Pa} \approx 1.6 \text{ MPa} \quad (5.2) \]

The embedded steel beams are generally warmer than the surrounding concrete and will therefore expand more, compared to the colder concrete. The colder concrete will therefore be exposed to tensile stresses. According to the result in Eq. 5.2, the local stress difference between the concrete and the steel beams corresponds to 64\% of the concrete characteristic tensile strength. This returns a remaining over capacity of 36\% or 0.9 MPa until cracking.
With embedded steel - in the bottom section

The temperature distribution in the upper ring slab is considered with embedded steel beams, modelled in the bottom of the cross section of the upper ring slab. This scenario represents an exaggerated condition, with reference to similar conditions as Forsmark F1. This condition is considered exaggerated due to the modelled cross section area of the embedded steel beams with the dimension of $200 \times 230$ mm compared to the slim in-situ I-profiles. As well as to the simplified positioning of the modelled steel beams, see Fig. 5.31.

The modelled position is due to the detail of the mesh where the elements are assigned as steel. Including the concrete cover, representing the in-situ condition, in the model would lead to a concrete cover corresponding to the element height of 230 mm. The modelled condition is considered conservative, due to the modelled steel area and positioning. The positioning of the modelled steel beams excludes the
60 mm concrete cover, which leads to higher levels of horizontal thermal heat flow inwards the structure. Hence, with a modelled condition worse than the actual insitu condition (and therefore considered a conservative assumption) in a temperature distributive perspective. The cross sectional steel area in the modelled condition is approximately seven times bigger than for the in-situ case, 46000 mm$^2$ compared to 6600 mm$^2$. Fig. 5.32 shows the modelled distribution of the embedded steel beams, through a horizontal sectional view dividing the upper ring slab in half at the bottom element layer.

Figure 5.32: 3D model: Horizontal sectional view of the modelled steel beams in the upper ring slab.

Fig. 5.33 shows the temperature and heat flow distribution in the steel beams at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s.
Figure 5.33: 3D model: Steel beams with temperature and heat flow distribution at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s.
Fig. 5.34 shows a vertical sectional view of the upper ring slab, dividing it in half with the temperature and heat flow distribution at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s. The steel beams are placed according to Fig. 5.32.

Figure 5.34: 3D model: Temperature and heat flow distribution in the upper ring slab at time steps $0.1 \cdot 10^4$, $0.4 \cdot 10^4$, $1 \cdot 10^4$ and $7 \cdot 10^4$ s.
Fig. 5.35 shows a horizontal sectional view of the upper ring slab, dividing it in half at the bottom element layer with temperature and heat flow distribution at time steps $0.1 \times 10^4$, $0.4 \times 10^4$, $1 \times 10^4$ and $7 \times 10^4$ s. The steel beams are placed according to Fig. 5.32.

Figure 5.35: 3D model: Temperature and heat flow distribution in the upper ring slab at time steps $0.1 \times 10^4$, $0.4 \times 10^4$, $1 \times 10^4$ and $7 \times 10^4$ s.
The results from the embedded steel beams in the bottom of the cross section of the upper ring slab, see Sec. 5.2.1, is compared with the reference case at time step 7·10^4 s. The temperature contour plots shown in Fig. 5.23 and Fig. 5.34 indicates the differences in temperatures in the vertical directions as well as surface temperatures. Fig. 5.24 and Fig. 5.35 indicates the differences in temperatures in the horizontal directions as well as for surface temperatures. The results indicate increased temperatures in the horizontal direction and reduced temperatures in the vertical direction due to the influence of embedded steel beams. The vertical sectional view of the upper ring slab, see Fig. 5.34, indicate reduced temperatures in the bottom. This is due to the influence of embedded steel beams, with a horizontal temperature decrease inwards the concrete structure. The sectional view represents a cut through the concrete cross section, with visible steel. The overall temperature level is marginally increased horizontally inwards the concrete structure. According to Fig. 5.33, the main thermal heat flow occurs vertically across the embedded steel beams, due to their position in the upper ring slab. This indicates a dominating vertical thermal heat flow inwards the upper ring slab due to the presence of the embedded steel beams. A comparison with the reference case indicates an increased thermal heat flow of up to 22.8% at time step 7·10^4 s. At time step 0.4·10^4, the increase in horizontal thermal heat flow horizontally inwards is about 56.1%. The horizontal sectional view of the upper ring slab, see Fig. 5.35, indicates increased temperature levels further into the upper ring slab structure, through the embedded steel beams. Higher levels of temperature are distributed into the upper ring slab along the embedded steel beams. This is noticeable due to the increased temperatures along the steel beams. The indicated temperature differences between the embedded steel beams and the surrounding concrete are approximately 8.9°C. The approximated level of stress difference between the concrete and the steel, due to the temperature difference, is calculated by using Eq. 2.11 and 2.10. The calculated results are according to Eq. 5.3 and Eq. 5.4.

\[
\varepsilon_0 = \varepsilon_t = \alpha \Delta T = \alpha (T_t - T_0) \approx 1 \cdot 10^{-5} \times 8.9 \approx 8.9 \cdot 10^{-5} \quad (5.3)
\]

\[
\sigma = E(\varepsilon - \varepsilon_0) \approx 35 \cdot 10^9 \times 8.9 \cdot 10^{-5} \approx 3115000 \text{ Pa} \approx 3.1 \text{ MPa} \quad (5.4)
\]

The embedded steel beams are generally warmer than the surrounding concrete and will therefore expand more, compared to the colder concrete. The colder concrete will therefore be exposed to tensile stresses. According to the result in Eq. 5.4, the local stress difference between the concrete and the steel beams corresponds to a stress level higher than the characteristic tensile strength. The result exceeds the characteristic tensile strength of the concrete with 24% or 0.6 MPa.

5.2.2 Temperature induced stress analyses

Due to the complexity with temperature gradients towards the surrounding environment, a stress analysis is considered necessary. The stress analysis is performed
to study the impact on the upper ring slab regarding stresses due to temperatures. The hand calculated stress levels made earlier in Sec. 5.2.1 and Sec. 5.2.1 assumes a fixed structure and will therefore only give an approximated estimation. Fig. 5.36 shows the boundary conditions, applied in the stress analysis of the upper ring slab. A fixed boundary condition is applied in the bottom, representing the connection with the cylinder wall, with fixed degrees of freedom in all directions. Two fixed boundary conditions are applied on the left and right short sides, representing the structural impact from the surrounding structure (symmetry condition), one with the fixed degree of freedom in the \( x \)-direction and one in the \( y \)-direction.

![Figure 5.36: 3D model: Boundary conditions.](image)

**Without embedded steel - reference case**

This stress analysis is coupled with the calculated temperatures in the temperature analysis, see Sec. 5.2.1, without embedded steel (reference case). The reference case enables a comparison between the scenarios with and without embedded steel and their influence on the temperature induced stresses in the upper ring slab. Fig. 5.37, shows a vertical sectional view of the upper ring slab with plotted maximum principal stress at time step \( 7 \cdot 10^4 \) s.
5.2. THREE-DIMENSIONAL MODEL

Figure 5.37: 3D model: Vertical sectional view with plotted maximum principal stress at time step $7 \times 10^4$ s.

Fig. 5.38, shows a horizontal sectional view of the upper ring slab with plotted maximum principal stress at time step $7 \times 10^4$ s.

Figure 5.38: 3D model: Horizontal sectional view with plotted maximum principal stress at time step $7 \times 10^4$ s.

Fig. 5.39, shows a horizontal sectional view of the bottom (element layer) of the upper ring slab with plotted maximum principal stress at time step $7 \times 10^4$ s.
CHAPTER 5. NUMERICAL RESULTS

Figure 5.39: 3D model: Horizontal sectional view with plotted maximum principal stress at time step $7 \times 10^4$ s.

The cracked concrete zone at the lower left side is due to local effects from the fixed boundary condition and can therefore be neglected. The results in Fig. 5.37 and Fig. 5.38 indicate a cracked concrete zone near the front side and middle of the cross section of the upper ring slab. As well as a cracked concrete zone at the right side of the top surface. The results in Fig. 5.39 indicate no cracked concrete.

**With embedded steel - in the middle section**

This stress analysis is coupled with the calculated temperatures in the temperature analysis, see Sec. 5.2.1, with the embedded steel in the middle of the cross section of the upper ring slab. Fig. 5.40, shows a vertical sectional view of the upper ring slab with plotted maximum principal stress at time step $7 \times 10^4$ s. The steel beams are placed according to Fig. 5.26.
Figure 5.40: 3D model: Vertical sectional view with plotted maximum principal stress at time step $7 \cdot 10^4$ s.

Fig. 5.41, shows a horizontal sectional view of the upper ring slab with plotted maximum principal stress at time step $7 \cdot 10^4$ s. The steel beams are placed according to Fig. 5.26.

Figure 5.41: 3D model: Horizontal sectional view with plotted maximum principal stress at time step $7 \cdot 10^4$ s.
The results in Fig. 5.40 indicate no cracked concrete zones next to the embedded steel beams. This corresponds to the approximated hand calculation in Sec. 5.2.1, indicating a low risk for stresses higher than the concrete tensile strength. Fig. 5.40 indicates an increased cracked concrete zone at the right side of the top surface. The results in Fig. 5.41 indicate cracked concrete zones between the embedded steel beams.

**With embedded steel - in the bottom section**

This stress analysis is coupled with the calculated temperatures in the temperature analysis, see Sec. 5.2.1, with the embedded steel in the bottom of the cross section of the upper ring slab. Fig. 5.42, shows a vertical sectional view of the upper ring slab with plotted maximum principal stress at time step $7 \times 10^4$ s. The steel beams are placed according to Fig. 5.32.

![Figure 5.42: 3D model: Vertical sectional view with plotted maximum principal stress at time step $7 \times 10^4$ s.](image)

Fig. 5.43, shows a horizontal sectional view of the upper ring slab with plotted maximum principal stress at time step $7 \times 10^4$ s. The steel beams are placed according to Fig. 5.32.
The results in Fig. 5.42 indicate cracked concrete zones along the embedded steel beams. This corresponds to the approximated hand calculation in Sec. 5.2.1, indicating a high risk for stresses higher than the concrete tensile strength. Fig. 5.42 indicates an increased cracked concrete zone near the front side and middle of the cross section of the upper ring slab. As well as an increased cracked concrete zone at the right side of the top surface. The results in Fig. 5.43 indicate cracked concrete zones more than 23 cm horizontally into the upper ring slab structure, in the concrete zones between the embedded steel beams.
Chapter 6

Discussion, Conclusions and Future Research

In this chapter, the results from Chap. 5 are discussed and conclusions are being drawn. Future research and development are discussed at the end of this chapter.

6.0.3 Thermal cooling effect of the ventilated tendon ducts

The results in Sec. 5.1.6 indicate decreased levels of cracked concrete zones in the vertical directions of the cylinder wall by the influence of the thermal cooling effect of the ventilated tendon ducts. This behaviour occurs due to decreased temperature levels in the cylinder wall when influenced by the thermal cooling effect, see Fig. 5.16 and Fig. 5.21. The decreased temperature reduces the level of stress, due to a more flattened temperature gradient through the cylinder wall. This induces lower levels of stress due to reduced temperature differences. The difference between the percentage of decreased levels of cracked concrete zones in the vertical directions and the difference at critical levels of elevated temperatures for the two analyses in Sec. 5.1.6 and Sec. 5.1.6, are due to the influence of the defined contact condition. The defined contact induces high levels of tensile stress to the cylinder wall. This results in higher levels of cracked concrete zones in the cylinder wall at lower elevated temperatures. It also prohibits thermal expansion from taking place as freely as without the defined contact, with higher levels of stress as a result. The stress levels in the studied elements next to the tendon ducts are marginally changed with the defined contact, see Fig. 5.19, compared to the huge changes in stress levels without the defined contact, see Fig. 5.19. The proportion of the stress level, due to the defined contact condition, dominates the stress distribution in the cylinder wall. Hence, with small changes in stress due to the influence of the thermal cooling effect. Without the contact condition, the stress levels are greatly reduced due to the influence of the thermal cooling effect. Hence, with smaller zones of cracked concrete as well as higher evaluated temperatures as a result. The horizontal distribution of cracked concrete zones in the cylinder wall is marginally increased with the defined contact, due to the influence of the thermal cooling effect. This increase is so small
that it can be considered negligible. Without the defined contact, the horizontal
distribution is noticeably decreased by the thermal cooling effect at time step 1.7
(170%). At time steps 1.8 and 1.9 this positive effect is decreased due to cracked
cement around the discontinuity regions representing the tendon duct geometries.
Notice that the studied stress is in the $z$-direction, with radial crack growth direction
through the cross section of the cylinder wall. The influence of the thermal cooling
effect is considered positive, with a reduced cracked concrete zone in the cylinder
wall. This mainly occurs in the vertical direction but also to small content in the
horizontal direction. The thermal cooling effect of the ventilated tendon ducts is
considered to have a positive global effect which is relevant to consider in global
three-dimensional models.

6.0.4 Embedded steel

The influence of embedded steel affects the temperature distribution in the two studied scenarios. The main differences between the results from the two scenarios are
due to the positioning of the embedded steel beams. By positioning the embedded
steel beams with their steel surface towards the exposed environment, higher levels
of temperature will be distributed due to increased levels of thermal heat flow by
conduction through the embedded steel beams. During a transient condition the
highest temperature changes occur in the embedded steel, due to its higher thermal
conductivity and lower specific heat capacity compared to the surrounding concrete.
The intention of the scenario with the embedded steel beams in the middle of the
cross section of the upper ring slab was to maximise the temperature distributing
effect of the embedded steel. Therefore, the embedded steel beams were positioned
without any concrete cover, towards the upper dry well environment. The results
indicated an increase in thermal heat flow with up to 34% horizontally inwards
the concrete structure compared to the reference case without embedded steel at
time step $0.4 \cdot 10^4$ s. The distributive temperature level inwards the structure was
increased with approximate 1.5 m. All these factors contribute to increase the risk
of cracking. The stress analysis part, including only the temperatures, indicates
cracked concrete zones between the embedded steel beams and an increased zone
with cracked concrete at the right side of the top surface. These zones occurred
near the front side of the upper ring slab, i.e. the surface closest to the reactor tank.
The intention of the scenario with the embedded steel beams in the bottom of the
cross section of the upper ring slab was to study a similar condition as Forsmark
F1. The modelled scenario was considered conservative due to the increased cross
section area of the modelled steel beams. As well as the excluded concrete cover, against the environment in the upper drywell, along the embedded steel beams. The
results indicate high temperatures in the embedded steel beams due to their positioning. This leads to larger temperature differences between the embedded steel beams and the surrounding concrete. The stress analysis indicates cracked concrete zones along the embedded steel beams. As well as cracked concrete zones between the embedded steel beams and an increased cracked concrete zone at the right side of the top surface. Both occurred at regions near the front side of the upper ring
slab. The influence of embedded steel is considered as an aspect that needs to be taken into consideration in order to obtain accurate temperature distributions in transient analyses.

6.0.5 Future research and development

In this section, thoughts and future ideas are discussed. Future developments concerning more detailed and extensive FE models are discussed.

Axisymmetric modelling

It would have been desirable to obtain more detailed information about actual temperature conditions at Forsmark F1 with the possibility to run an analysis with reference to the actual serviceability condition at Forsmark F1. This analysis would deliver more relevant and useful results for the Forsmark Power Group. It is uncertain, how much thermal cooling effect the ventilated tendon ducts actually transmit to the cylinder wall concrete in practice. The numerical analyses are calculated with the assumption that the tendon ducts are ventilated with the 20°C reactor building air temperature and applied as a 20 °C nodal temperature. In practice, this thermal heat transfer is transmitted with forced convection in the tendon ducts and the thermal heat transfer would work as a heat exchanger were the surrounding concrete temperature increases the air flow temperature. It would be of interest to measure the airflow on the outgoing air in the tendon ducts to verify an actual air flow with the possibility of cooling as well as air temperatures. The numerical difference is considered small by replacing the convection and radiation boundary condition inside the tendon ducts with nodal temperatures, due to the steady state condition. Even though the results indicate a stress reducing effect, due to the influence of the thermal cooling effect. It would be of great interest to calculate more accurate results concerning this potential (extra) capacity increasing effect on the cylinder wall. This would require a more extensive and detailed FE model since the axisymmetric model is used for indicating a global behaviour only.

Three-dimensional modelling

A more detailed FE model of the upper ring slab with the actual embedded steel condition is considered necessary. It is considered uncertain how much the 60 mm concrete cover and the lower in-situ area of the embedded steel beams affects the temperature distribution into the upper ring slab at the transient temperature condition. The FE model should be combined with a more extensive stress analysis, including all the other load conditions affecting the structure. This would give more reliable results concerning the actual condition at Forsmark F1.
Bibliography


Appendix A

Hand Calculations

A.1 Pool construction and reactor tank pressure loads

In the axisymmetric model, the pressure load from the pool construction is approximated to an overlaying water volume. The pressure load from the water volume doesn’t include the overlaying mass of the pool construction. The marked zones, see Fig. A.1 and A.2, are zones in the pool construction that will be taken into consideration due to the applied boundary condition in the axisymmetric model illustrated in Fig. A.2. The pool construction zones 1, 5 and 6 lies outside the boundary condition in the axisymmetric model and will therefore not be included in the total pressure load from the pool construction. The pressure load from the pool construction is supported by the reactor containment building structure and will work as a counterweight. The pool construction prevents the reactor containment to move in the $y$-direction and is defined as a boundary condition on the side of the upper ring slab in the axisymmetric model.
Figure A.1: Situation plan of the pool construction with zones.

Figure A.2: Section view of the pool construction with zones, water volumes and boundary condition.

The approximated water load from the pool construction zones 2, 3 and 4 is according to Eq. A.1

$$F \approx (660 + 845 + 660) \times 1000 \times 10.0 \approx 21.65 \text{ MN} \quad (A.1)$$

There are extra pressure loads on the pool bottom from different devices illustrated in Fig. A.3. The marked devices, 4, 5, 6 and 7, are the ones taken in consideration due to the boundary condition in Fig. A.2.
The total load from the pool construction devices 4, 5, 6 and 7 are according to Eq. A.2.

\[ F \approx (50000 + 24000 + 30000 + 50000) \times 10 \approx 1.54 \text{ MN} \quad (A.2) \]

The total load, Eq. A.1, from the pool construction and the devices, Eq. A.2, are divided over the top area, Eq. A.5, of the upper ring slab illustrated in Fig. A.4.

\[ A \approx 12.1^2 \times \pi - 4.4^2 \times \pi \approx 400.0 \text{ m}^2 \quad (A.3) \]

The total pressure load from the pool construction and the devices on the top geometry is according to Eq. A.4.
The total pressure load from the reactor tank is in the serviceability state equal to 18 MN and is divided over the whole reactor tank support base accordingly to Eq. A.5.

\[ A \approx 5.0^2 \times \pi - 3.8^2 \times \pi \approx 33.2 \text{ m}^2 \]  
(A.5)

The total pressure load from the reactor tank on the reactor tank support base is according to Eq. A.6.

\[ P \approx \frac{18000000}{33.2} \approx 540 \text{ kPa} \]  
(A.6)

The pressure load from the reactor tank is supported by the biological screen and works as a counterweight against displacement in the positive (right) \( y \)-direction.

### A.2 Convective heat transfer coefficients
Approximated hand calculation:

The surface heat transfer coefficients for convection are approximately calculated for turbulent air flow inside the reactor containment and for laminar water flow in the reactor containment pools.

Material parameters taken from:


**Turbulent air flow:**

\[ L_c := 1.0 \text{ m} \]  
Characteristic length

\[ \lambda_{air} := 0.025 \frac{W}{m \cdot K} \]  
Thermal conductivity

\[ g = 9.807 \frac{m}{s^2} \]  
Acceleration of gravity

\[ \beta_{air} := 3.43 \times 10^{-3} \frac{1}{K} \]  
Volume expansion coefficient

\[ \Delta T_{air} := 25 \text{ K} \]  
Temperature difference between wall and surface

\[ \nu_{air} := 15.11 \times 10^{-6} \frac{m^2}{s} \]  
Kinematic viscosity

\[ \rho_{air} := 1.23 \frac{kg}{m^3} \]  
Density

\[ c_{air} := 1008 \frac{J}{kg \cdot K} \]  
Specific heat capacity

**Dimensionless numbers:**

**Prandtl's number**  
\[ Pr_{air} := \left( \frac{\nu_{air} \rho_{air} c_{air}}{\lambda_{air}} \right) = 0.749 \]

**Grashof's number**  
\[ Gr_{air} := \frac{\left( g \beta_{air} L_c^3 \Delta T_{air} \right)}{\nu_{air}^2} = 3.683 \times 10^9 \]

Natural convection on room surfaces:
\[
\text{Gr}_{\text{air}} \cdot \text{Pr}_{\text{air}} = 2.76 \times 10^9 : \quad \text{Gr}_{\text{air}} \cdot \text{Pr}_{\text{air}} > 10^9 \quad \text{Turbulent flow}
\]

\[
A_{\text{turb}} := 0.13
\]

\[
B_{\text{turb}} := \frac{1}{3}
\]

**Nusselt's number for natural flow on room surfaces:**

\[
\text{Nusselt's number } \quad \text{Nu}_{\text{air}} := A_{\text{turb}} \left( \text{Gr}_{\text{air}} \cdot \text{Pr}_{\text{air}} \right)^B_{\text{turb}} = 182.354
\]

**Coefficient of surface heat transfer due to convection (and conduction):**

\[
h_{c, \text{air}} := \frac{\left( \text{Nu}_{\text{air}} \cdot \lambda_{\text{air}} \right)}{\text{L}_c} = 4.559 \frac{W}{m^2 \cdot K} \approx 5.0 \frac{W}{m^2 \cdot K}
\]

**Laminar water flow:**

\[
\lambda_{\text{water}} := 0.6 \frac{W}{m \cdot K} \quad \text{Thermal conductivity}
\]

\[
\beta_{\text{water}} := 207 \times 10^{-6} \frac{1}{K} \quad \text{Volume expansion coefficient}
\]

\[
\Delta T_{\text{water}} := 0.001 \text{ K} \quad \text{Temperature difference between wall and surface} \quad (\Delta T \approx 0)
\]

\[
\nu_{\text{water}} := 1.004 \times 10^{-6} \frac{m^2}{s} \quad \text{Kinematic viscosity}
\]

\[
\rho_{\text{water}} := 1000 \frac{kg}{m^3} \quad \text{Density}
\]

\[
\epsilon_{\text{water}} := 4190 \frac{J}{kg \cdot K} \quad \text{Specific heat capacity}
\]

**Dimensionless numbers:**

\[
\text{Prandtl's number } \quad \text{Pr}_{\text{water}} := \frac{\left( \nu_{\text{water}} \rho_{\text{water}} \epsilon_{\text{water}} \right)}{\lambda_{\text{water}}} = 7.011
\]
Grashof's number \[ \text{Gr}_{\text{water}} = \frac{g \beta_{\text{water}} L_c^3 \Delta T_{\text{water}}}{\nu_{\text{water}}^2} = 2.014 \times 10^6 \]

Natural convection on room surfaces:

\[ \text{Gr}_{\text{water}} \cdot \text{Pr}_{\text{water}} = 1.412 \times 10^7 \quad \text{Gr}_{\text{water}} \cdot \text{Pr}_{\text{water}} < 10^9 \quad \text{Laminar flow} \]

\[ A_{\text{lam}} := 0.59 \]

\[ B_{\text{lam}} := \frac{1}{4} \]

Nusselt's number for natural flow on room surfaces:

\[ \text{Nusselt's number} \quad \text{Nu}_{\text{water}} := A_{\text{lam}} \left( \text{Gr}_{\text{water}} \cdot \text{Pr}_{\text{water}} \right)^{B_{\text{lam}}} = 36.167 \]

Coefficient of surface heat transfer due to convection (and conduction):

\[ h_{c,\text{water}} := \frac{\left( \text{Nu}_{\text{water}} \lambda_{\text{water}} \right)}{L_c} = 21.7 \frac{\text{W}}{\text{m}^2 \cdot \text{K}} \approx 20.0 \frac{\text{W}}{\text{m}^2 \cdot \text{K}} \]
A.3 Vertical prestress
**Approximated hand calculation:**

The vertical prestress level is calculated approximately without any frictional- or time dependent losses. The calculated tendon is assumed to be initially straight. The measured initial prestress force corresponds to a geometrical straight tendon.

**Area of one tendon unit:**

\[
A_{\text{sp}} := 0.0019 \text{m}^2
\]

**Average cc-distance between tendon units:**

\[
cc := 0.5181 \text{m}
\]

**Concrete section thickness:**

\[
d := 0.834 \text{m}
\]

**Measured initial prestress force:**

\[
F_0 := 2.4 \text{MN}
\]

**Ratio between tendon and concrete area:**

\[
P_{\text{hsp}} := \frac{A_{\text{sp}}}{cc \cdot d} = 0.0044
\]

**Calculated average stress in tendon:**

\[
P_a := \frac{F_0}{A_{\text{sp}}} = 1.263 \times 10^3 \cdot \text{MPa}
\]

**Compressive stress in the cylinder wall from the vertical tendons:**

\[
P_{\text{cyl}} := P_a \cdot P_{\text{hsp}} = 5.554 \text{ MPa}
\]

The influence of the vertical tendons, is considered as **-5.5 MPa** in the vertical direction of the cylinder wall.
A.4 Horizontal prestress
Approximated hand calculation:

The horizontal prestress level is calculated approximately, with frictional losses. Time dependent losses, e.g. relaxation, are neglected. The measured initial prestressing force corresponds to a geometrical straight tendon.

Radius of the inner tendon: \[ r := 11.825 \text{m} \]

Initially straight tendon part at the active end: \[ x_1 := 4.8 \text{m} \]

Cylinder wall thickness, concrete section: \[ t := 0.834 \text{m} \]

Double wrapped part of the tendon: \[ x_2 := 2.7 \text{m} \]

Approximated tendon length (half): \[ d := x_1 + x_2 + \pi \cdot r = 44.649 \text{m} \]

Young’s modulus (Tendon steel): \[ E_s := 200 \text{GPa} \]

Area of one tendon unit: \[ a := 0.0019 \text{m}^2 \]

Average cc-distance between tendon units: \[ c_c := 0.38 \text{m} \]

Number of rows: \[ n := 1 \]

Initial prestress force at active end: \[ F_0 := 2.5 \cdot \text{MN} \]

Frictional coefficient between tendon and duct: \[ \mu := 0.18 \]

Wobbling effect: \[ k := 0 \text{m}^{-1} \]

Measured prestress force at active end: \[ F_1 := 1.89 \cdot \text{MN} \]

Total height: \[ h := 30 \text{m} \]

Number of tendons: \[ n_{st} := 156 \]

Vertical CC-distance: \[ c_{c_{vert}} := \frac{h}{\left(\frac{n_{st} - 2}{2}\right)} + 2 = 0.38 \text{m} \]
The tendon length, $x$, is defined and divided into twelve points of interest.

The accumulated change of angle, $\alpha$, is calculated for the studied points. The expression for a constant radius is: $\alpha = x/r$ where $x$ is the distance from the tendon end.

$$x = \begin{bmatrix} 0m \\ x_1 \\ x_1 + \frac{(d - x_1)}{10} \\ x_1 + 2\cdot \frac{(d - x_1)}{10} \\ x_1 + 3\cdot \frac{(d - x_1)}{10} \\ x_1 + 4\cdot \frac{(d - x_1)}{10} \\ x_1 + 5\cdot \frac{(d - x_1)}{10} \\ x_1 + 6\cdot \frac{(d - x_1)}{10} \\ x_1 + 7\cdot \frac{(d - x_1)}{10} \\ x_1 + 8\cdot \frac{(d - x_1)}{10} \\ x_1 + 9\cdot \frac{(d - x_1)}{10} \\ d \end{bmatrix}$$

$$\alpha = \begin{bmatrix} 0 \\ 0 \\ x_1 + \frac{(d - x_1)}{10} - x_1 \\ x_1 + 2\cdot \frac{(d - x_1)}{10} - x_1 \\ x_1 + 3\cdot \frac{(d - x_1)}{10} - x_1 \\ x_1 + 4\cdot \frac{(d - x_1)}{10} - x_1 \\ x_1 + 5\cdot \frac{(d - x_1)}{10} - x_1 \\ x_1 + 6\cdot \frac{(d - x_1)}{10} - x_1 \\ x_1 + 7\cdot \frac{(d - x_1)}{10} - x_1 \\ x_1 + 8\cdot \frac{(d - x_1)}{10} - x_1 \\ x_1 + 9\cdot \frac{(d - x_1)}{10} - x_1 \\ x_1 + 10\cdot \frac{(d - x_1)}{10} - x_1 \end{bmatrix}$$

$$= \begin{array}{c|c} \text{m} & \text{0} \\ \hline 0 & 0 \\ 1 & 4.8 \\ 2 & 8.7849 \\ 3 & 12.7699 \\ 4 & 16.7548 \\ 5 & 20.7397 \\ 6 & 24.7247 \\ 7 & 28.7096 \\ 8 & 32.6945 \\ 9 & 36.6795 \\ 10 & 40.6644 \\ 11 & 44.6493 \end{array}$$
The frictional loss is calculated by the force balance:

\[ f(\alpha) := F_0 e^{-\mu(\alpha + k \cdot x)} ; \quad f(\alpha) = \\cdot \cdot \cdot = 23.181 \text{ MN} \]

**Calculated frictional loss:**

\[ F_{\text{friction}} := f(\alpha) \]

**Summation of the frictional loss:**

\[ \sum f(\alpha) = 23.181 \text{ MN} ; \quad \Delta F_{\text{friction}} := \sum f(\alpha) \]

**Calculated average force in tendon:**

\[ F_a := \frac{\sum f(\alpha)}{12} = 1.932 \text{ MN} \]

**Calculated average stress in tendon:**

\[ P_a := \frac{F_a}{a} = 1016.716 \text{ MPa} \]

The initial force is known, the new force in the tendon is calculated due to the slip.

**Calculated force due to tendon slip:**

\[ f_{\text{slip}}(\alpha) := F_1 e^{\mu(\alpha)} ; \quad f_{\text{slip}}(\alpha) = \text{ MN} \]

\[ F_{\text{slip}} := f_{\text{slip}}(\alpha) \]

\[ \begin{array}{c|c}
0 & 2.5 \\
1 & 2.5 \\
2 & 2.353 \\
3 & 2.214 \\
4 & 2.084 \\
5 & 1.961 \\
6 & 1.846 \\
7 & 1.737 \\
8 & 1.635 \\
9 & 1.539 \\
10 & 1.448 \\
11 & 1.363 \\
\end{array} \]
Difference in force between friction and slip:

\[ \Delta F := F_{\text{friction}} - F_{\text{slip}} = MN \]

<table>
<thead>
<tr>
<th></th>
<th>0</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
<th>8</th>
<th>9</th>
<th>10</th>
<th>11</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>0.61</td>
<td>0.61</td>
<td>0.3447</td>
<td>0.0806</td>
<td>-0.1832</td>
<td>-0.4476</td>
<td>-0.7137</td>
<td>-0.9824</td>
<td>-1.2547</td>
<td>-1.5317</td>
<td>-1.8143</td>
<td>-2.1035</td>
</tr>
</tbody>
</table>

Sorting the calculated difference into ascending order:

\[ \Delta F_{\text{sorted}} := \begin{bmatrix} -2.104 \times 10^6 \text{N} \\ -1.814 \times 10^6 \text{N} \\ -1.532 \times 10^6 \text{N} \\ -1.255 \times 10^6 \text{N} \\ -9.824 \times 10^5 \text{N} \\ -7.137 \times 10^5 \text{N} \\ -4.476 \times 10^5 \text{N} \\ -1.832 \times 10^5 \text{N} \\ 8.059 \times 10^4 \text{N} \\ 3.447 \times 10^5 \text{N} \\ 6.1 \times 10^5 \text{N} \end{bmatrix} \]

\[ x_{\text{sorted}} := \begin{bmatrix} 44.6493 \text{m} \\ 40.6644 \text{m} \\ 36.6795 \text{m} \\ 32.6945 \text{m} \\ 28.7096 \text{m} \\ 24.7247 \text{m} \\ 20.7397 \text{m} \\ 16.7548 \text{m} \\ 12.7699 \text{m} \\ 8.7849 \text{m} \\ 4.8 \text{m} \end{bmatrix} \]

Interpolating the distance:

\[ x_f := \text{linterp} (\Delta F_{\text{sorted}}, x_{\text{sorted}}, 0) \]

\[ x_f = 13.9873 \text{ m} \]

Interpolating the force:

\[ F_f := \text{linterp} (x - x_f, F_{\text{slip}}, 0) \]

\[ F_f = 2.1745 \text{ MN} \]
Tendon curve, after loss at tendon slip:

\[
X_1 := \begin{pmatrix}
44.6493m \\
40.6644m \\
36.6795m \\
32.6945m \\
28.7096m \\
24.7247m \\
20.7397m \\
16.7548m \\
13.9876m \\
12.7699m \\
8.7849m \\
4.8m \\
0m
\end{pmatrix},
F_X := \begin{pmatrix}
1.3630 \times 10^6 N \\
1.4483 \times 10^6 N \\
1.5388 \times 10^6 N \\
1.6351 \times 10^6 N \\
1.7373 \times 10^6 N \\
1.8460 \times 10^6 N \\
1.9614 \times 10^6 N \\
2.0841 \times 10^6 N \\
2.1746 \times 10^6 N \\
2.1338 \times 10^6 N \\
2.0082 \times 10^6 N \\
1.8900 \times 10^6 N \\
1.8900 \times 10^6 N
\end{pmatrix}
\]

Summation of the frictional loss, due to tendon slip:

\[
\sum F_X = 23.711 \cdot MN
\]

Calculated average force in tendon, due to tendon slip:

\[
F_{aslip} := \frac{\sum F_X}{13} = 1.824 \cdot MN
\]

Calculated average stress in tendon, due to tendon slip:

\[
P_{aslip} := \frac{F_{aslip}}{a} = 959.943 \cdot MPa
\]

Ratio between the tendon and concrete area:

\[
\frac{a}{(t \cdot cc_{vert})} = 0.012
\]

Compressive stress in the cylinder wall from the horizontal tendons:

\[
P_{cyl} := -P_{hs} \cdot P_{aslip} = -11.518 \cdot MPa
\]

The influence of the horizontal tendons, is considered as \textbf{-11.5 MPa} in the tangential direction of the cylinder wall.