UNIVERSITY OF BOLOGNA Dept. of Electrical Engineering Lab. of Applied Superconductivity

## DESIGN AND SIMULATION OF A LARGE SCALE ENERGY STORAGE AND POWER TRANSMISSION SYSTEM FOR REMOTE RENEWABLE ENERGY SOURCES EXPLOITATION

LUCA TREVISANI

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by

LUCA TREVISANI

Coordinator: Prof. FRANCESCO NEGRINI Tutors: Prof. Massimo Fabbri Prof. Francesco Negrini Prof. Pier Luigi Ribani

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to my parents

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# Chapter 1

## Introduction

Environmental concerns, limited fossil resources availability and economic-politic agreements are today greatly pushing research in the field of Renewable Energy Sources (RES). Although world resources estimations and forecasts are often subject to great uncertainties, major international organizations agree in foreseeing increasing problems of energy cost and supply security for today predominant energy sources such as petrol and natural gas in the medium (20–30 years) and long term period (end of 21<sup>st</sup> century) [1]. The fundamental role of energy availability in the modern economic and social system, associated with the complexity involved in the transition from the well established today "historical" energy system towards sustainable models demands from now a massive research effort, new proposals, and strong engagement in many possible directions.

Worldwide, many countries have already undertaken challenging programs for increasing RES generation. Hydroelectric energy has been widely exploited for many decades, and is an established technology. Still, it represents an unemployed potential in many areas of the world. Today, China is completing the world largest hydroelectric power plant, which will reach 18.2 GW total capacity in 2009, to be transported over a distance of 940 km towards energy demanding areas [2]. Wind technologies have been undergoing an extraordinary development in recent years, with rapidly increasing rotor size which led to today's multi-MW power machines. Great wind energy potential exists throughout the world, both onshore and offshore. Wind energy production is actually becoming an important resource in some energy system realities: it accounted for 20% of electricity consumption in Denmark in 2004 [3].

Solar energy production has been so far limited by high costs, however it has the potentiality to represent a leading energy source in the longer term. It was estimated that using 4% of world desert areas for solar systems installation, the annual energy production would equal world primary energy consumption [4]. A little 0.3% of Sahara desert would equal in peak power the total installed electrical power of Europe (700 GW) [5]. At present the largest solar thermal complex has a total electrical power of 354 MW [6], while the largest photovoltaic plant has a power of 10 MW [7]. The photovoltaic energy market is actually booming with 40% annual global growth rate and 1100 MW installed in 2004 [8]. The present market focus is indeed on small-scale, dispersed stand-alone power systems as well as small and medium-sized building-integrated grid-connected power systems. However, large scale photovoltaic systems may represent a future option as the fabrication of less-costly photovoltaic devices becomes a reality [4, 9].

On the other hand, a part from today costs concerns, two main technological obstacles could refrain RES power penetration in next years. RES power, with reference in particular to solar and wind, is characterized by low density per unit area, and therefore require wide low-cost lands availability. Low populated remote areas and offshore zones are often suitable for large systems installation, but efficient large power transmission systems are needed in order to transport the produced energy towards consumption sites. Besides, RES power availability is typically fluctuating and poorly predictable. If insufficient storage facilities are available (e.g. storage hydroelectric plants), whenever power generation mismatches grid demands, power-balancing in the network must be managed acting on conventional power plants. This leads to difficulties in maintaining grid stability, increased specific consumption of thermal power plants (which produce less electrical energy but with a higher fuel consumption and CO<sub>2</sub>-emissions per kWh), and could actually limit the degree of RES penetration [10]. It was estimated that wind power generation of less than 15% of total grid load may already cause a hidden increase of the specific fuel consumption in thermal-electric stations [11]. With reference for instance to the Germany's energy system, it was estimated that a sustainable electrical energy supply based on very large off-shore wind power farms and pumped water storage, would require such a storage capacity that would be impossible to realize in the country [12]. In the long term, new storage technologies should be developed for decoupling RES power availability and actual grid demand.

Superconducting cables have the ability to efficiently transport very large currents at relatively low voltage. Indeed, they are nearly loss-less conductors in DC conditions, while refrigerating power has to be accounted as power loss. DC superconducting lines have been proposed for large power transmission since the sixties [13], using those days known Low Temperature Superconducting materials (LTS). These materials, like NbTi and Nb<sub>3</sub>Sn, are relatively inexpensive and can be manufactured as long wires, but require cooling with liquid helium at 4.2 K for practical applications. In the eighties, High Temperature Superconducting materials (HTS) where discovered. These materials, like YBCO and BSCCO, have the advantage that can be more easily cooled by Liquid Nitrogen (LN<sub>2</sub>) at 77 K. HTS DC lines were recently proposed for connecting offshore wind farms to the Danish network [14]. On the other hand, HTS materials have the drawback of being very expensive and difficult to manufacture as long wires [15–17].

On March 2001, the discovery of new superconducting material MgB<sub>2</sub> was announced [18]. MgB<sub>2</sub> is a relatively inexpensive inter-metallic compound, which can be manufactured as long wires. MgB<sub>2</sub> remains superconductive up to 39 K, and shows good superconducting properties for practical applications up to about 25 K [19–21]. Only two elements can keep fluid at a conveniently wide range of temperature below 25 K: helium (with standard boiling point at about 4.2 K), and hydrogen (which can be kept in liquid form at temperatures between 13.8 K to 32.9 K) [22]. Since gaseous helium has low specific heat per unit volume, LH<sub>2</sub> seems to be the best cryogen for MgB<sub>2</sub> cables from the point of view of its physical properties. Moreover, LH<sub>2</sub> presents the important feature for use as the cryogen of a superconducting line of having extremely low viscosity for a liquid (as a reference, more than 10 times lower than LN<sub>2</sub>). This limits pressure drops along the line and, together with the feasibility of producing long MgB<sub>2</sub> wires and tapes, allows to consider the design of multi-km superconducting lines.

Hydrogen, produced by water electrolysis, is also one of the most studied clean technologies for storage and transmission of RES power [23], and is often indicated as a concrete future option for fossil fuels replacement [24–30]. From an ecological

point of view hydrogen is unequaled for environmental safety. Its primary combustion product is water vapor, allowing a near zero-emission energy cycle when produced by water electrolysis using RES. Large scale hydrogen production is a present reality: the US industry currently produces 9 Mtons/year of hydrogen for use in chemicals production, petroleum refining, metals treating, and electrical applications [25]. Hydrogen pipelines actually amount to more than 1500 km in Europe and 700 km in the US.

Hydrogen has more energy per unit mass than any other fuel (Higher Heating Value of 141.9 MJ/kg), but also has the lowest density and consequently low volumetric energy content (approximately 25% less than natural gas). For large scale storage, it can be compressed (reaching densities of  $14.5 \text{ kg/m}^3$  at 200 bar 288 K) or liquefied (70.8 kg/m<sup>3</sup> at 1 bar 20 K). Although more expensive from the energy point of view, liquefaction is often suitable when large hydrogen storage is required, and often leads to lower total storage and transport costs [31–33]. For similar reasons, growing quotes of natural gas are today transported in liquid form, with about 26% of total international trade in 2002 [34].

 $LH_2$  can therefore act at the same time as energy vector and cryogen of an  $MgB_2$  superconducting line. The general concept of a combined  $MgB_2/LH_2$  line for large power concurrent transport of electric and chemical energy was first proposed by Paul Grant, a scientist at the Electric Power Research Institute (EPRI), for connecting several remotely sited nuclear power plants in north America [35– 38]. In this thesis, for the first time the complete design and simulation of a combined MgB<sub>2</sub>/LH<sub>2</sub> system is carried out. The system, schematically represented in Fig. 1.1, is aimed at providing a solution for large scale RES power transport and supply regulation. When there is RES power excess with respect to grid demand, hydrogen is produced by water electrolysis, which can be cooled down to 20.4 K for liquid storage. The stored hydrogen can be reconverted into electric energy in periods of RES shortage, allowing the utility to increase the flexibility in responding to fluctuations in wind or solar input or consumer demand. The exceeding part can be transported through the cryogenic pipeline, acting at the same time as the cryogen of an MgB<sub>2</sub> superconducting DC line for the concurrent transport of electric energy and  $LH_2$ .

In Chapters 2, 3 and 4 the mechanical and electrical design of the combined  $MgB_2$  superconducting  $LH_2$  cryogenic pipeline is presented. It is shown how the line can be suitably designed with respect to both electrical and flow capacity



Figure 1.1 Schematic representation of the  $MgB_2/LH_2$  working principle.

specifications. The bipolar configuration with warm dielectric is considered; however, the aim is to provide general design criteria which can be easily applied also to other superconducting line configurations.

In Chapter 5 the energy aspects involved in hydrogen liquefaction and vaporization are analyzed, and advanced vaporization systems aimed at recovering part of the spent energy are proposed and compared.

Finally, in Chapters 6, 7 and 8 a complete electrical model of the system is developed and implemented in EMTP, and used to simulate the working of the system with reference to a realistic scenario of application. The use of electrochemical devices is considered both for hydrogen production and for reconversion into electrical energy (fuel-cell system). This choice was dictated by fuel-cell's good characteristics for intermittent power source compensations (fast load-response, modularity, high efficiency over a wide operating range) and by research interest reasons, but should not be considered as the only available option. Indeed, the overall electrical model and control strategy could be easily adapted for the case of conventional hydrogen fed thermal-electrical system.

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### Part I

# Mechanical and Electrical Design of the $MgB_2/LH_2$ Combined Line

# Chapter 2

## **Thermal Insulation Design**

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#### 2.1 Introduction

Superconducting cables can transmit electric power with nearly zero losses when operated in DC. However, thermal incomes through the thermal insulation have to be removed by the cable cooling system, and the required cooling power has to be accounted when estimating overall cable efficiency. Together with cable material and manufacturing costs, refrigeration costs are one of the most important aspects of the economical performance of superconducting cables, and strongly



Figure 2.1 Schematic representation of a flexible cryostat with external electrical insulation for warm dielectric superconducting cable.

depend on the quality of the thermal insulation. Indeed, thermal insulation design is a key factor in determining the performance and competitiveness of every superconducting device.

The first step for the cryogenic design of the  $MgB_2/LH_2$  line is to design the thermal insulation between 300 K and 20 K and estimate thermal incomes from the environment. The typical construction of a flexible cryostat for cable thermal insulation consists of two concentric, corrugated, stainless steel tubes. The annular region between the tubes contains a multilayer insulation (MLI) system kept under vacuum. The MLI system is composed of reflective layers separated by a low thermal conductivity spacer material. Structural spacers are used in order to keep the tubes concentric and prevent compression of the alternating reflective and spacer layers. The maintenance of high vacuum degree in the cryostat during cable operation is essential to limit thermal incomes. This can be obtained inserting special getter materials which have the property to absorb residual and outgassed material when kept at cryogenic temperature [1].

The configuration of thermal and electrical insulation depends on the chosen constructive typology of the superconducting cable. In the following, I refer to the case of warm dielectric configuration (with electrical insulation at room temperature outside the cryostat), but the same design principle can be similarly applied to cold dielectric with concentric or polycentric configurations. In Fig. 2.1 it is shown the schematic representation of the thermal and electrical insulation of a warm dielectric superconducting cable. There are no experimental data available in literature referring to measurements of thermal incomes into superconducting cables operating in the 15–25 K temperature range. However, several HTS superconducting cables working at  $LN_2$ temperatures of 65–77 K have been recently tested in Europe [2, 3], USA [4, 5], Japan [6, 7], and China [8, 9]. For these cables, measurements of thermal incomes through the cryostat per unit cable length under actual operation conditions are reported between 1 W/m to 2 W/m.

Heat leakage through a cryostat is essentially due to the contribution of several heat transfer mechanisms. However, considering the overall thermal income (sum of all the heat transfer contributions), an "effective" cryostat thermal conductivity  $\lambda$  can be introduced through the Fourier equation. With reference to an axial-symmetric geometry,  $\lambda$  is given by the following relation:

$$q_{\rm tot} = \frac{2\pi\lambda\,\Delta T}{\ln\frac{D_{\rm out}}{D_{\rm in}}}\tag{2.1}$$

where  $q_{\text{tot}}$  is the overall thermal income per unit cable length, and  $D_{\text{in}}$  and  $D_{\text{out}}$  are the inner and outer diameters of the cryostat.

MLI could in principle reach values of effective thermal conductivity lower than  $3 \times 10^{-2}$  mW/m·K under quasi ideal conditions [10]. However, it is known that non-ideal conditions such as contact between adjacent reflecting layers and low vacuum degree exist in reality, which lead to strong decrease of MLI performance. The residual gas pressure can affect the effective thermal conductivity of a cryostat for HTS cables by more than two orders of magnitude [11]. Considering an interstitial pressure of  $2 \times 10^{-2}$  Pa [12], values as low as 0.1-0.2 mW/m·K can be obtained [11, 13]. The role of getter materials is crucial in order to maintain this degree of vacuum in exercise. Higher residual pressures of  $1 \times 10^{-1}$  Pa can lead to a doubling of thermal incomes or even more [11, 14].

To a first approximation, thermal incomes into an MgB<sub>2</sub> cable working in the LH<sub>2</sub> temperature range can be estimated using Eq. (2.1), where the effective thermal insulation conductivity can be estimated based on data of similar construction HTS cables operating at 77 K. A value of  $\lambda$  between 0.2 to 0.4 mW/m·K can be considered in accordance with reported experimental data [4, 6, 7, 11, 13]. Assuming cryostat reference design with  $D_{\rm in} = 120$  mm and  $D_{\rm out} = 180$  mm, Eq. (2.1) estimates thermal incomes between 0.9 W/m to 1.7 W/m.

Another approach to predict thermal incomes through the cryostat is by us-

ing a semi-empirical relation which can predict heat flux through MLI depending on several overall parameters (typically number of radiation shields, layers density, emissivity coefficient, residual gas properties and pressure). With reference for instance to the relation provided by [14, p. 190–200], one gets similar results as those reported above when considering interstitial gas pressure in the range between  $2 \times 10^{-2}$  Pa to  $1 \times 10^{-1}$  Pa.

On the other hand, heat flux through the cryostat can be evaluated by implementing a full thermal model of the MLI system [10, 15–17]. The advantage of this approach is that it is possible to take into account the performance of each layer at the actual working temperature. Moreover, the three main contributes of heat transfer through MLI (radiation, solid conduction and gas conduction) can be separately estimated and compared, allowing to find indications for improvements of the insulation quality.

#### 2.2 Cryostat Thermal Model

#### 2.2.1 Numerical Model

Heat leakage between adjacent cryostat layers is essentially due to the sum of the following main thermal contributes:

- 1. thermal radiation between facing surfaces;
- 2. thermal conduction through the mechanical supports;
- 3. thermal conduction through the residual gas.

In steady-state condition the total heat transfer is equal for all the insulation layers, while single heat transfer contributions are different from layer to layer. Considering an axial-symmetric cryostat, the overall heat transfer is here evaluated with reference to the unit axial length as:

$$q_{\text{tot}} = q_{\text{tot}_{i,i+1}} = q_{\text{rad}_{i,i+1}} + q_{\text{s.cond}_{i,i+1}} + q_{\text{g.cond}_{i,i+1}}$$
(2.2)

where  $q_{\rm rad}$ ,  $q_{\rm s.cond}$  and  $q_{\rm g.cond}$  are respectively the heat transfer contributes per unit axial length due to radiation, solid conduction and gas conduction, while *i* is the counter variable which indicates boundary surfaces of insulating layers (whose temperature  $T_i$  is assumed to be uniform). In the following, expressions of each
thermal heat transfer contribute are derived for the specific case of concentric cylindrical surfaces with radius r.

#### Radiation

Indicating with  $\sigma$  the Stefan-Boltzmann constant, and with  $\varepsilon$  the emissivity coefficient (ratio of energy radiated to energy radiated by a black body at the same temperature), heat transfer due to radiation between adjacent surfaces is given by:

$$q_{\mathrm{rad}_{i,i+1}} = 2\pi r_i \, \frac{\sigma}{\frac{1}{\varepsilon_i} + \frac{r_i}{r_{i+1}} \left(\frac{1}{\varepsilon_{i+1}} - 1\right)} \, \left(T_{i+1}^4 - T_i^4\right) \, (1-f) \tag{2.3}$$

Factor f is introduced as the ratio of contact area and whole heat transfer area of adjacent surfaces, and takes into account the fact that radiation only occurs on fraction (1 - f) of facing surfaces.

#### Solid Conduction

Indicating with  $\lambda$  the thermal conductivity of the interposed medium, heat transfer due to solid conduction between adjacent surfaces is given by:

$$q_{\text{s.cond}_{i,i+1}} = 2\pi \, \frac{\lambda_{i,i+1}}{\ln\left(\frac{r_{i+1}}{r_i}\right)} \, \left(T_{i+1} - T_i\right) \, f \, C \tag{2.4}$$

Factor C is introduced as an empirical constant taking into account overall contact heat transfer resistance.

#### **Residual Gas Conduction**

Indicating with  $\gamma$  the ratio of specific heats of the residual gas, R the gas constant (ratio of universal gas constant over molecular weight of the residual gas), and  $p_{i,i+1}$  the interstitial pressure, heat transfer due to residual gas conduction between adjacent surfaces can be written as:

$$q_{\text{g.cond}_{i,i+1}} = 2\pi r_i \frac{1}{\frac{1}{\alpha_i} + \frac{r_i}{r_{i+1}} \left(\frac{1}{\alpha_{i+1}} - 1\right)} \frac{\gamma + 1}{\gamma - 1}$$
$$p_{i,i+1} \sqrt{\frac{R}{4\pi \left(T_{i+1} + T_i\right)}} \left(T_{i+1} - T_i\right) \left(1 - f\right) \quad (2.5)$$

Dimension	Measure	f	C
Cryostat inner tube radius	$60\mathrm{mm}$		
Cryostat inner tube thickness	$1.5\mathrm{mm}$	1.5	1
Inner vacuum layer gap thickness	$6\mathrm{mm}$	0.01	0.06
MLI total thickness	$20\mathrm{mm}$	0.01	0.03
Outer vacuum layer gap thickness	$2\mathrm{mm}$	0.01	0.06
Cryostat outer tube thickness	$1\mathrm{mm}$	1	1

 Table 2.1
 Cryostat main dimensions.

Factor  $\alpha$  is an accommodation coefficient which depends on interstitial residual gas, surface conditions and temperature [18].

### 2.2.2 Implementation

The cryostat structure considered in the implementation of the proposed MLI numerical model is composed by: internal corrugated stainless steel, inner vacuum layer gap, MLI, outervacuum layer gap, outer corrugated stainless steel. The two vacuum gaps are introduced in order to reduce the axial flow conductivity and improve evacuation in case of long line lengths. The considered MLI structure is composed by alternated double-aluminized polyester films (Mylar<sup>TM</sup>) and low conductivity spacers made of polyester fabric (Dacron<sup>TM</sup>). Main cryostat dimensions used in calculi are reported in Tab. 2.1, together with respective values of factors f and C [10, 12].

The implemented model takes into account the dependence of material properties on the temperature of the single layers. In Eq. (2.3), the emissivity coefficient for the warm and cold corrugated tubes was set to 0.5. This value is higher than for smooth surfaces because of occurring multiple reflections. The emissivity of double-aluminized Mylar<sup>TM</sup> films with respect to temperature is shown in Fig. 2.2 [14].

For Eq. (2.4), the thermal conductivity of inner and outer tubes is shown in Fig. 2.3 (304 stainless steel [14]), and the thermal conductivity of spacer materials is shown in Fig. 2.4 (fiberglass epoxy for inner and outer vacuum layer supports [14], and polyester fabric between aluminized films [19]).

In Eq. (2.5), the accommodation coefficient  $\alpha$  and the ratio of specific heats  $\gamma$  is assumed to be respectively 1 and 1.4 [18], while R = 287 J/kg-K. Because



Figure 2.2 Emissivity of double-aluminized Mylar<sup>™</sup> films.

the residual pressure p cannot be precisely predicted, at first a reference value of  $2 \times 10^{-2}$  Pa was chosen, and then a parametric study with pressure values in the range from 0 Pa to 1 Pa was carried out [11, 12, 14].

### 2.2.3 Results

The dependence of overall thermal incomes  $q_{tot}$  on the number of insulating layers was evaluated assuming residual gas pressure of  $2 \times 10^{-2}$  Pa. For these calculations and the following ones the warm wall temperature was set to 300 K, and the cold wall temperature to 20 K. Fig. 2.5 shows that  $q_{tot}$  at first decreases rapidly with increasing number of layers, and then decreases much slower with more than 20 layers: from 0.84 W/m with 24 layers, it reaches 0.65 W/m with 48 layers and 0.55 W/m with 96 layers. An increase in the number of insulating layers for the same insulation thickness, leads both to a decrease in the temperature difference and in the distance between adjacent layers. This greatly reduces radiation (Eq. (2.3) depends on the forth power of T), reduces gas conduction (Eq. (2.5) depends linearly on T), but slightly affects the solid conduction contribute (Eq. (2.4) depends linearly on T and inversely on layer distance). Based on the results shown in Fig. 2.5, a number of insulating layers equal to 24 was considered as satisfactory.



Figure 2.3 Thermal conductivity of 304 stainless steel.



Figure 2.4 Thermal conductivity of spacer materials (fiberglass epoxy for inner and outer vacuum layer supports, polyester fabric between aluminized films).



Figure 2.5 Calculated thermal incomes with respect to number of insulating layers (assuming residual gas pressure of  $2 \times 10^{-2}$  Pa).

Fig. 2.6 shows the calculated temperature distribution through the cryostat with 24 insulating layers. For this case, Fig. 2.7 shows the relative heat transfer contributions with respect to the total heat income. The radiation contribution is preponderant in the high-temperature side of the cryostat, while solid and gas conduction contributions are preponderant in the low-temperature side. This means that, in order to improve the insulating performance, considering heat radiation one should try to lower the emission coefficient of reflecting layers which work at higher temperatures, for instance by using better materials than aluminum, such as silver. Besides, considering solid and gas pressure near the cold wall. This effect is accomplished by the inner 6 mm wide vacuum space. These results are in accordance with what indicated in [10].

A parametric study was carried out in order to highlight the influence of residual gas pressure on the cryostat thermal incomes. When varying pressure from 0 to 1 Pa, thermal incomes increase almost linearly from the lower value of 0.61 W/m with a rate of about 10 W/m·Pa, which is about 25% lower than the value predicted by the semi-empirical relation in [14, p. 197]. Fig. 2.8 shows the results of calculations. As for the cryostat effective thermal conductivity,



Figure 2.6 Temperature distribution through 24 insulating layers.



Figure 2.7 Relative heat transfer contributions with respect to the total heat income for the case with 24 insulating layers.



Figure 2.8 Calculated thermal incomes with respect to residual gas pressure (with 24 insulating layers)

with Eq. (2.1) one gets  $\lambda = 0.19 \,\mathrm{mW/m\cdot K}$  considering a residual pressure of  $2 \times 10^{-2} \,\mathrm{Pa}$ , and  $\lambda = 0.38 \,\mathrm{mW/m\cdot K}$  with a residual pressure of  $1 \times 10^{-1} \,\mathrm{Pa}$ .

However, it is known that variations in difficultly predictable parameters such as winding pressure, uniform contact pressure and interstitial pressure can strongly affect the performance of MLI [18, 20]. Therefore, MLI thermal calculations cannot predict exactly heat fluxes, and should be treated with an adequate safe coefficient. After comparison with data available in literature and provided by cryogenic pipeline manufacturers [1], the reference value of environmental thermal incomes was assumed to be 2 W/m, which corresponds to a cryostat effective thermal conductivity of  $0.46 \,\mathrm{mW/m\cdot K}$ . This means a safe coefficient of about 2.4 with respect to the value calculated with interstitial pressure of  $2 \times 10^{-2} \,\mathrm{Pa}$ , and of about 1.2 with respect to the value calculated with interstitial pressure of  $1 \times 10^{-1} \,\mathrm{Pa}$ .

# 2.3 H<sub>2</sub> Permeation through Stainless Steel

Due to its small atomic size, hydrogen can easily permeate through the crystal lattice of many materials, including steel. Therefore, it is necessary to estimate if

the amount of  $H_2$  permeation through the inner pipe is able to compromise the cryostat vacuum degree.

#### 2.3.1 Mass transfer equations

Diffusion is the macroscopic result of the many microscopic chaotic movements which molecules carry out due to thermal agitation. The phenomenon of mass diffusion of a gas phase chemical specie through a solid phase material is analogous to the phenomenon of heat conduction, and occurs whenever there is a gradient in the concentration of a species. Many physical and chemical processes can affect diffusion rate, depending on the involved chemical species. However, when considering non reacting diffusion of hydrogen through a steel wall, the first Fick's law of diffusion can be applied, which represents a linear relation between the rate of diffusion and the local concentration gradient[21]:

$$\mathbf{j} = -D \ \nabla c \tag{2.6}$$

where  $\mathbf{j} \text{ (mol/s} \cdot \text{m}^2)$  is the specific molar flux,  $c \text{ (mol/m}^3)$  the molar concentration, and  $D \text{ (m}^2/\text{s)}$  is the diffusion coefficient (or mass diffusivity). The continuity equation, which is valid for non reacting systems, is expressed by the second Fick's law of diffusion:

$$\frac{\partial c}{\partial t} = -\nabla \cdot \mathbf{j} = D \ \Delta c \tag{2.7}$$

Considering an axial-symmetric plane problem, for which all parameters depend only on the radial coordinate r and time, the two Fick's lows reduce respectively to:

$$\mathbf{j} = -D \,\frac{\partial c}{\partial r} \,\hat{\mathbf{r}} \tag{2.8}$$

$$\frac{\partial c}{\partial t} = D \frac{1}{r} \frac{\partial}{\partial r} \left( r \frac{\partial c}{\partial r} \right)$$
(2.9)

where  $\hat{\mathbf{r}}$  is the unit vector in the radial direction, and the outgoing flux across the steel wall per unit length of the pipe is given by:

$$j_{\rm out} = -2\pi r \, D \, \frac{\partial c}{\partial r} \tag{2.10}$$

22

In steady-state, Eq. (2.9) can be easily integrated two times with  $\partial c/\partial t = 0$ :

$$c(r) = A\ln r + B \tag{2.11}$$

where integration constants A and B are given by boundary conditions. Given the molar concentrations  $c_{\rm in}$  an  $c_{\rm out}$  in the inner and outer pipe wall surfaces (respectively at radius  $r_{\rm in}$  an  $r_{\rm out}$ ), one gets the expression of c(r):

$$c(r) = \frac{c_{\rm in} \ln\left(\frac{r_{\rm out}}{r}\right) + c_{\rm out} \ln\left(\frac{r}{r_{\rm in}}\right)}{\ln\left(\frac{r_{\rm out}}{r_{\rm in}}\right)}$$
(2.12)

which substituting into Eq. (2.10) gives the specific outgoing molar flux:

$$j_{\text{out}} = 2\pi D(T) \frac{c_{\text{in}} - c_{\text{out}}}{\ln\left(\frac{r_{\text{out}}}{r_{\text{in}}}\right)}$$
(2.13)

Hydrogen molecules dissociate into atoms to diffuse through a metal. Hence, c and j should be expressed in terms of moles of hydrogen atoms. However, because of the linear relation between concentrations and flux, Eq. (2.13) keeps valid also when using fictitious values of concentrations in terms of moles of hydrogen molecules, as it is often done.

In Eq. (2.13) the dependence of the diffusion coefficient on the temperature can be expressed by the Arrhenius equation:

$$D(T) = D_0 \, \exp\left(-\frac{E_D}{R_0 T}\right) \tag{2.14}$$

where  $D_0$  (m<sup>2</sup>/s) is the diffusivity reference value (or frequency factor),  $E_D$  (J/mol) the activation energy, and  $R_0$  (J/mol·K) the universal gas constant.

The hydrogen concentration in the metal lattice at the boundary interface is related to the metal temperature and to the partial pressure of hydrogen gas by the Sieverts' law [22]:

$$c = K_{\rm S}(T)\sqrt{p_{\rm H_2}} \tag{2.15}$$

where  $K_{\rm S}$  (mol/m<sup>3</sup>·Pa<sup>0.5</sup>) is the Sieverts coefficient (or equilibrium constant), and  $p_{\rm H_2}$  is the partial pressure of hydrogen in equilibrium with the metal. The dependence on the square root of partial pressure comes from the dissociation of hydrogen molecules into twice as many atoms at low concentration. The value of  $K_{\rm S}$  depends on the metal structure and composition, and follows the same Arrhenius equation with respect to temperature as in Eq. (2.14).

By substituting Eq. (2.15) into Eq. (2.13) one gets the relationship between partial pressures of hydrogen gas and outgoing molar flux:

$$j_{\text{out}} = 2\pi P(T) \frac{\sqrt{p_{\text{in}}} - \sqrt{p_{\text{out}}}}{\ln\left(\frac{r_{\text{out}}}{r_{\text{in}}}\right)}$$
(2.16)

where the permeability coefficient P is defined as the product of diffusivity and Sieverts coefficients (mol/m·s·Pa<sup>0.5</sup>), and thus also follows the Arrhenius equation with respect to the temperature:

$$P(T) = P_0 \, \exp\left(-\frac{E_P}{R_0 T}\right) \tag{2.17}$$

where  $P_0 \text{ (mol/m·s·Pa}^{0.5}\text{)}$  is the permeability reference value and  $E_P \text{ (J/mol)}$  the activation energy.

# 2.3.2 Estimation of $H_2$ permeation through the cryostat pipe wall

Measurements of hydrogen flux through metal membranes directly reported in terms of permeability are available in literature [23–26]. Hence, Eq. (2.16) and (2.17) can be used to estimate hydrogen flux through the cryostat pipe wall. However, the reported coefficients are usually derived by fitting of experimental measurements carried out above the room temperature. It is not assured by these experimental data that the reported Arrhenius relation in Eq. (2.17) would extend down to 20 K. For this reason, three precautionary augmented values of the inner pipe wall temperature (120 K, 140 K and 160 K) were chosen for evaluating the amount of H<sub>2</sub> permeation. Due to the strong dependence of permeability on temperature, these increased reference values should be enough to ensure that the estimation is approximated by excess. However, permeability experimental data actually measured at cryogenic temperatures would be needed in order to increase the trustworthy of results.

The standard ISO 13985 [27, 28], which regulates the design of  $LH_2$  tanks for land vehicles, prescribes the wall thickness of the inner vessel not to be less than

 $1.5 \,\mathrm{mm}$  and to be such that the combined static and dynamic stresses never exceed  $25\,\%$  of the minimum specified tensile strength of the material of construction. Considering the AISI 304 stainless steel, an inner cryostat pipe wall radius of 60 mm and a pressure of 25 bar, the minimum prescribed thickness of  $1.5 \,\mathrm{mm}$  can be assumed as sufficient.

Values of  $P_0 = 4.2 \times 10^{-8} \text{ mol}_{\text{H}_2}/\text{m}\cdot\text{s}\cdot\text{Pa}^{0.5}$  and  $E_P = 35.15 \text{ kJ/mol}_{\text{H}_2}$  are reported in [23]. At the reference temperature of 120 K, Eq. (2.17) gives  $P = 2.1 \times 10^{-23} \text{ mol}_{\text{H}_2}/\text{m}\cdot\text{s}\cdot\text{Pa}^{0.5}$ . Assuming  $p_{\text{in}} = 25 \text{ bar}$ ,  $p_{\text{out}} = 0 \text{ bar}$ ,  $r_{\text{in}} = 60 \text{ mm}$ ,  $r_{\text{out}} = 61.5 \text{ mm}$  (Tab. 2.1), Eq. (2.16) gives the outgoing H<sub>2</sub> molar flux per unit length of the pipe  $j_{\text{out}} = 8.5 \times 10^{-18} \text{ mol}_{\text{H}_2}/\text{s}\cdot\text{m}$ . This result strongly depend on the chosen reference temperature value: the same estimation gives  $j_{\text{out}} = 1.3 \times 10^{-15} \text{ mol}_{\text{H}_2}/\text{s}\cdot\text{m}$  assuming a reference temperature of 140 K and  $j_{\text{out}} = 5.7 \times 10^{-14} \text{ mol}_{\text{H}_2}/\text{s}\cdot\text{m}$  assuming a reference temperature of 160 K.

From these values it is possible to estimate the amount of cryostat pressure increase due to  $H_2$  permeation as well as the time of vacuum maintaining. To first approximation, the residual gas in the free cryostat space can be assumed to be at uniform average temperature  $T_f$  and to follow the ideal gas law:

$$p A_{\rm f} L = n R_0 T_{\rm f} \tag{2.18}$$

where  $A_{\rm f}$  is the free cryostat section, L the cryostat length, and n the number of moles of gas occupying the free space. The correlation between outgoing flux and cryostat pressure increment rate can be therefore written as:

$$\frac{\mathrm{d}p}{\mathrm{d}t} = \frac{R_0 T_\mathrm{f}}{A_\mathrm{f}} \cdot \frac{1}{L} \frac{\mathrm{d}n}{\mathrm{d}t} = \frac{R_0 T_\mathrm{f}}{A_\mathrm{f}} \cdot j_{\mathrm{out}}$$
(2.19)

Because of the little pressure variations in the cryostat,  $j_{out}$  can be assumed constant. The time needed to increase the cryostat pressure up to a maximum value  $p_{max}$  starting form the initial value  $p_0$  is thus given by:

$$t_{\rm vacuum} = \frac{A_{\rm f}}{R_0 T_{\rm f}} \cdot \frac{p_{\rm max} - p_0}{j_{\rm out}}$$
(2.20)

Assuming  $p_0 = 1 \times 10^{-2}$  Pa,  $p_{\text{max}} = 1 \times 10^{-1}$  Pa,  $T_{\text{f}} = 160$  K and evaluating  $A_{\text{f}}$  from Tab. 2.1 as the cryostat section which is not occupied by MLI, one gets a vacuum maintaining time of several centuries for the reference temperature of

120 K, which reduces to 6 years for the reference temperature of 140 K, and to just 5 days for the reference temperature of 160 K. It can be deduced that, with reference to the available experimental data, the phenomenon of  $H_2$  permeation, although relevant at higher temperatures, should become negligible at 20 K with respect to other possible reasons of vacuum degradation. The very low entrance rate of hydrogen into the metal lattice at low temperature should also help avoiding the compatibility problems of some construction steels to  $H_2$ .

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# Electrical Design of the Superconducting Cable

#### Contents

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# 3.1 Introduction

Since the discovery of superconductivity at 39 K in inter-metallic magnesium diboride [1] several research groups have developed techniques for the production of low-cost MgB<sub>2</sub> wires and tapes [2, 3]. The first superconducting MgB<sub>2</sub> wires were obtained by the diffusion of magnesium into boron whiskers [4], but this technique has been limited to the production of short samples. Today the most widely used process for producing industrial length of MgB<sub>2</sub> wires and tapes is the "powder in tube" (PIT) process, which can be mainly divided into the two "in-situ" and "ex-situ" techniques.

The "ex-situ" PIT technique involves direct filling of metallic tubes with MgB<sub>2</sub> powder and then drawing and rolling into tapes followed by sintering at relative high temperatures (800–1000 °C) [5–9]. The "in-situ" PIT technique is characterized by filling the metallic tubes with elemental Mg and B powders and subsequent drawing and rolling into tapes followed by heat treatment at lower temperatures, during which the elements react to form the MgB<sub>2</sub> phase [5, 10]. Beside these two main methods, a third technique was introduced, which resorts to the use of a partially reacted precursor powder induced by mechanical alloying of a Mg and B powder mixture [11]. Another proposed technique combines features of the PIT "in-situ" process with the use of a reactive liquid infiltration [12, 13]. Due to the many different shapes and production processes of MgB<sub>2</sub> wires, the reported experimental data in literature are often widely variable.

The data here used for the design of the  $MgB_2$  superconducting cable refer to wires produced with a particular "ex-situ" technique developed by "Columbus Superconductors" (Genova, Italy) [6]. Pure nickel tubes were filled with commercial powders and cold worked in several steps without intermediate annealing. As a final step, a heat treatment in an in-line oven at about 900 K was performed in argon atmosphere in order to enhance its transport properties. Monocore, multifilamentary, and copper stabilized multifilamentary PIT wires and tapes can be produced with this technique. For the copper stabilized conductors, an iron layer is used between copper and nickel to prevent diffusion of Cu towards the  $MgB_2$ phase, which would poison the superconductor. Fig. 3.1 shows the cross section of a copper stabilized multifilamentary  $MgB_2$  tape produced through this technique. When not flattened, a wire of about 1.7 mm in diameter is obtained. The developed technique allows to produce wires of several kilometers in length upon adequate sizing of the tools. Fig. 3.2 shows the measured critical current versus applied perpendicular induction field at different cooling temperatures. These experimental data were collected under a joint contract between the Department of Electrical Engineering of the University of Bologna and "Ansaldo Superconduttori" (Genova, Italy).



 $\label{eq:Figure 3.1} \begin{array}{c} {\rm Cross\ section\ of\ a\ copper\ stabilized\ multifilamentary\ MgB_2\ tape\ produced\ by\ Columbus\ Superconductors} \end{array}$ 



Figure 3.2 Critical current versus applied perpendicular induction field for the copper stabilized multifilamentary  $MgB_2$  tape (courtesy of "Ansaldo Superconductori").

# 3.2 Cable cross section

Several possibilities exist for the choice of the cable cross section geometry, whether employing superconducting tapes or wires. Here the cable is assumed to be made of twisted wires directly bathed in the LH<sub>2</sub> flow. However, because in the following the effect of the AC induction field due to the current ripple are found to be negligible, similar considerations would be valid when employing tapes. In this case, they could also be wrapped between the inner cryostat pipe and the thermal insulation layer. The nominal DC current in the cable is assumed to be 12 kA. The maximum nominal working temperature along the line is assumed to be 25 K. The cross section is first obtained through straightforward considerations using DC critical current and applied filed relations as shown in Fig. 3.2. Then, losses in the superconducting wires due to the sum of the DC and ripple components of the current are estimated. The cable is expected to be thermally stabilized thanks to the copper core included in the MgB<sub>2</sub> wires.

Assuming a safe value for the maximum current in the cable  $I_{\text{max}} = 13 \text{ kA}$ , and an attempt cable diameter  $D_{\text{cbl}} = 20 \text{ mm}$ , the maximum induction field at the cable surface is  $B_{\text{max}} = \mu_0 I_{\text{max}} / \pi D_{\text{cbl}} = 0.26 \text{ T}$ . This value is increased to the safe value of 0.4 T. As for the working temperature, it is increased to the safe value of 26 K. With these data, Fig. 3.2 gives a critical current of 188 A. The cable is then designed with a twisted  $7 \times 4 \times 4$  structure for a total of 112 wires. Assuming an empty factor of 20 %, this leads to a cable diameter of about 19.7 mm. Ultimately, in the most unfavorable conditions with current of 13 kA and temperature of 26 K, the average current per wire is 116 A and the maximum induction field at the cable surface 0.264 T.

## **3.3** Cable loss estimation

Losses in superconducting composites can be classified either by external source of the dissipated energy (magnetization loss and transfer current loss) or by nature of the loss (fluxoids motion, hysteretic and eddy currents loss) [14, 15]. AC loss in superconducting cables arises in general whenever a varying induction field and/or a varying current is applied. When the dissipated power is delivered by the power supply of the magnet which applies the field B, it is classified as magnetization loss. When the dissipated power is delivered by the power supply that enables the transport current I, it is classified as transport current loss. It has to be pointed out that this distinction is somehow arbitrary, and depends on what is considered a magnet (source of the applied induction filed) and what is considered a current conductor. In general, each conductor is at the same time a current carrier and a source of magnetic field for other conductors. The resultant dissipated power (heat produced in the superconducting composite) is the due to the sum of both contributions.

When classified by nature, losses, whether coming from a magnet or a transport current power supply, are divided with respect to the involved irreversible physical process. The flux-flow and flux-creep processes refer to the irreversible movement of current vortexes (fluxoids) in the superconductor. For a type II superconductor at finite temperatures, this kind of loss exists also with perfect DC current in self field. This behavior is usually referred to as thermally activated flux flow (TAFF), and described by an exponential I-V characteristic (power law), which leads to a nonzero resistivity also below the critical temperature  $T_{\rm c}$  [15]. Hysteretic loss is due to screening currents which appear inside a type II superconductor when exposed to a time-varying induction field (in the form of fluxons). These currents tend to persist indefinitely as long as the superconductor state is maintained, leading to a hysteretic magnetic behavior similar to that due to magnetic domains orientation in ferromagnetic materials. Eddy currents loss is due to screening currents flowing in the matrix material (normal metal) of superconducting composites when exposed to a time-varying electromagnetic field, and have ohmic nature. The eddy current path can be closed inside the matrix material only, or partially within the superconducting filaments and partially in the matrix. In the latter case, they are referred to as "coupling currents". However, the coupling currents loss is also sometimes called simply eddy currents loss, when the "normal" eddy current loss inside the metal matrix can be ignored. Coupling currents loss can be reduced by wire twisting.

DC cables have inherently low loss because the only time-varying magnetic field is due to the current ripple, which is usually produced by power electronics devices at either end of the cable. For conventional DC transmission lines the voltage distortion (ripple) is limited by ANSI/IEEE standards to a maximum of 1% for an individual frequency and 2-5% in total [16]. For DC superconducting cables, current ripple should be lowered below 1% in order to limit AC losses [17]. The magnitude of harmonic currents in electric power transmission

systems employing DC superconducting cables was estimated in [18] for several cable geometries and power electronics working conditions.

In the following, for the estimation of losses in the cable it is assumed the transport current to be described by the sum of a DC component and an AC sinusoidal component with maximum relative magnitude of 1% [18]. Under these conditions, the maximum induction field at the cable surface is the sum of a 264 mT DC component and a 2.64 mT AC component. According to experimental data reported in [19–22], the value of the time-varying component of the induction field is low enough to neglect the hysteretic loss in the MgB<sub>2</sub> superconductor. Therefore, it is expected the predominant power loss in the cable to be due to thermally activated flux flow (through the power law I-V characteristic) and, depending on the twist pitch and inter-strand contact resistance, to coupling current loss.

#### 3.3.1 Cable loss due to thermally activated flux flow

Cable loss due to thermally activated flux flow is here estimated analytically assuming uniform distribution of the cable current among the wires. The current on each wire is assumed to be the sum of a DC component  $I_{dc}$  and a sinusoidal ripple with relative magnitude  $k_{rpl}$ :

$$I(t) = I_{\rm dc} + I_{\rm ac,M} \cos(\omega t) = I_{\rm dc} \left(1 + k_{\rm rpl} \cos(\omega t)\right)$$
(3.1)

The electric field for currents below the critical current  $I_c$  is approximated by the power law:

$$E = E_{\rm c} \left(\frac{I}{I_{\rm c}}\right)^n \tag{3.2}$$

where  $E_c = 1 \,\mu V/cm$  is the reference value of the electric field when  $I = I_c$  and the exponent *n* is typically between 30 and 100 for MgB<sub>2</sub> wires depending on the cooling temperature and applied field.

Introducing the continuous component of the electric field as:

$$E_{\rm dc} = E_{\rm c} \left(\frac{I_{\rm dc}}{I_{\rm c}}\right)^n \tag{3.3}$$

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and substituting Eq. (3.1) into (3.2) one gets:

$$E = E_{\rm c} \left( \frac{I_{\rm dc}}{I_{\rm c}} \left( 1 + k_{\rm rpl} \cos(\omega t) \right) \right)^n = E_{\rm dc} \left( 1 + k_{\rm rpl} \cos(\omega t) \right)^n \tag{3.4}$$

The instantaneous dissipated power per unit length of the wire is:

$$p(t) = E(t)I(t) = E_{\rm dc}I_{\rm dc} \left(1 + k_{\rm rpl}\cos(\omega t)\right)^{n+1}$$
(3.5)

This can be rewritten according to the binomial theorem as:

$$p(t) = E_{\rm dc} I_{\rm dc} \sum_{i=0}^{n+1} \left( \binom{n+1}{i} \left( k_{\rm rpl} \cos(\omega t) \right)^{n+1-i} \right)$$
(3.6)

where the binomial coefficient is defined as:

$$\binom{n}{k} = \frac{n!}{k! (n-k)!} \tag{3.7}$$

When calculating the average value P of the dissipated power in the cable on a period, this time with reference to the unit length of the cable rather than of the single wire, all the odd terms in the sum disappear, leading to:

$$P = n_{\rm wr} k_{\rm tw} E_{\rm dc} I_{\rm dc} \sum_{\substack{i=0\\(n+1-i) \text{ even}}}^{n+1} \left( \binom{n+1}{i} (k_{\rm rpl})^{n+1-i} \right)$$
(3.8)

where  $n_{\rm wr}$  is the number of wires and  $k_{\rm tw}$  is a raising coefficient which accounts for the higher length of the wires with respect to the cable due to twisting.

Introducing  $P_{dc}$  as the dissipated power due to the DC component alone:

$$P_{\rm dc} = n_{\rm wr} k_{\rm tw} E_{\rm dc} I_{\rm dc} \tag{3.9}$$

the loss in the cable due to thermally activated flux flow is given by:

$$P = K_{\rm rpl} P_{\rm dc} \tag{3.10}$$

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where the raising coefficient  $K_{\rm rpl}$  can be written as:

$$K_{\rm rpl} = 1 + \frac{1}{2!}n(n+1)(k_{\rm rpl})^2 + \frac{1}{4!}(n-2)(n-1)n(n+1)(k_{\rm rpl})^4 + \cdots$$
 (3.11)

The order of magnitude of the *i*-th term in Eq. (3.11) is  $(n k_{\rm rpl})^i / i!$ . Thus, depending on the value of n and  $k_{\rm rpl}$ , the higher terms in Eq. (3.11) can be neglected. Considering the MgB<sub>2</sub> wires at 26 K, n = 30 and  $k_{\rm rpl} = 1 \%$  leads to  $K_{\rm rpl} \approx 1.05$ . However, for the designed cable with 112 MgB<sub>2</sub> wires, Eq. (3.10) estimates power loss due to thermal activated flux flow below the mW/m, which are certainly negligible with respect to thermal leaks. This is in accord to what is reported in [18].

#### 3.3.2 Cable loss due to coupling currents

Cable loss due to coupling currents were evaluated by use of a numerical code named THELMA which was originally developed for loss estimation in superconducting magnets for controlled thermonuclear fusion [23]. Besides thermal activated flux flow loss, the code takes into account all the inductive couplings among the cable strands and evaluates the coupling currents loss, which is often a far more relevant loss contribute in AC superconducting cables.

The here presented results refer to a  $7 \times 4 \times 4$  cable structure with twist pitches respectively of 25 mm, 38 mm and 51 mm [19]. The transport current in the cable is the sum of a 13 kA continuous component and a sinusoidal ripple with 130 A magnitude and 600 Hz frequency (main ripple frequency produced by a 12 pulse inverter connected to a 50 Hz grid). Because the contact conductance between strands is unknown, a parametric study was carried out with values starting from  $1 \times 10^1$  S/m up to  $1 \times 10^9$  S/m [19]. Fig. 3.3 shows the comparison between cable loss calculated by the THELMA code and the analytic estimation of the thermally activated flux flow loss alone. It is confirmed that the level of AC loss due to harmonic currents in the MgB<sub>2</sub> DC cable do not pose any problem for the reliable operation of the cable, and that they are negligible with respect to the thermal heat in-leak through the cryostat.



Figure 3.3 Comparison between cable loss calculated by the THELMA code and the analytic estimation of the thermally activated flux flow loss alone.

# 3.4 Heat transfer through current leads

Cable terminations are needed in order to connect the superconducting cable with conventional devices at room temperature. The goal in the design of current leads is to minimize the heat leak per unit current Q/I, while maintaining thermal stability. Conduction-cooled current leads are simple wires or rods made of normal conducting metals which span ambient and cryogenic temperatures. Better performance can be obtained by gas-cooled current leads, which are cooled by a counter flow cryogenic gas (usually boil-off gas). Hybrid leads, which use a metallic conductor for their upper stage (300 to about 77 K) and a HTS composite for their lower stage can lead to further heat leak reduction. In the following, the simple case of conduction cooled current leads is considered and approximated relations are derived for the optimal design and the estimation of thermal incomes.

The heat leak towards the superconducting cable is the sum of two contributes due respectively to thermal conductivity and ohmic dissipation along the current lead. An increase in the conductor section leads to an increase in the former term and a decrease in the latter term. An optimal cross section of the copper terminations should be chosen which minimizes the overall heat transfer towards the cable. Here, the order of magnitude of heat leakage through the copper termination stage is estimated by considering the simple case with constant conductor cross section. Moreover, the properties of copper are averaged. More accurate results can be obtained when considering changing properties and variable conductor cross section along the termination length using numerical methods [24].

Considering averaged properties of copper in the temperature range of the current lead, the total heat flux  $Q_1$  delivered at low temperature can be approximated as the sum of the two heat contributes due to thermal conductivity and ohmic dissipation:

$$Q_1 = Q_{\Delta T} + \frac{1}{2}Q_{\Omega} \approx A\bar{\lambda}\frac{\Delta T}{\Delta l} + \frac{1}{2}\frac{\bar{\rho}\Delta l}{A}I^2$$
(3.12)

where A and  $\Delta l$  are the cross section and the length of the current lead, while  $\bar{\lambda}$  and  $\bar{\rho}$  represent the average values of the copper thermal conductivity and electrical resistivity in the working temperature range. Dividing by the current I yields:

$$\frac{Q_1}{I} = \frac{\bar{\rho}}{2} \left( \frac{2\bar{\lambda}\Delta T}{\bar{\rho}} \frac{A}{I\Delta l} + \frac{I\Delta l}{A} \right)$$
(3.13)

In Eq. (3.13), the term  $\Delta l/A$  is a design parameter which can be chosen in order to minimize the heat transfer through the termination. The minimization of Eq. (3.13) gives the optimal design ratio:

$$\left(\frac{\Delta l}{A}\right)_{\rm opt} = \frac{1}{I} \sqrt{\frac{2\,\bar{\lambda}\,\Delta T}{\bar{\rho}}} \tag{3.14}$$

which substituted into Eq. (3.13) yields:

$$\frac{Q_{\rm l\,opt}}{I} = \sqrt{2\,\bar{\lambda}\bar{\rho}\,\Delta T} \tag{3.15}$$

For pure metals, the product of the thermal conductivity and electrical resistivity can be approximately expressed in function of temperature by the Wiedemann-Franz law:

$$\lambda \rho = LT \tag{3.16}$$

The proportionality number L, known as the Lorenz number, has an ideal value  $L_0 = 2.45 \times 10^{-8} \,\mathrm{W} \cdot \Omega / \mathrm{K}^2$  which slightly varies with temperature for copper [25].

With reference to the averaged properties of copper one can write:

$$2\,\bar{\lambda}\bar{\rho}\,\Delta T \approx 2\,L\bar{T}\Delta T = L\left(T_{\rm h}^2 - T_{\rm c}^2\right) \tag{3.17}$$

where  $T_{\rm h}$  and  $T_{\rm c}$  are respectively the hot and cold temperature at the terminal ends, which substituted into (3.14) and (3.15) gives respectively:

$$\left(\frac{\Delta l}{A}\right)_{\rm opt} = \frac{1}{I} \sqrt{\frac{L\left(T_{\rm h}^2 - T_{\rm c}^2\right)}{\bar{\rho}^2}} \tag{3.18}$$

$$\frac{Q_{\rm l\,opt}}{I} = \sqrt{L \left(T_{\rm h}^2 - T_{\rm c}^2\right)} \tag{3.19}$$

These relations were here obtained with simple considerations and constant averaged properties. However, it can be shown that the same expressions are obtained when solving the minimization problem of the integration of the Fourier equation using variable local properties [25]. It has to be point out that Eq. (3.19) states that thermal incomes through a current lead should actually slightly depend on the choice of the conductor (as far as the validity of Eq. (3.16) is verified). This is the direct consequence of the fact that the best electrical conductors are also the best heat conductors.

For  $T_{\rm h} = 300 \,\mathrm{K}$  and  $T_{\rm c} = 20 \,\mathrm{K}$ , Eq. (3.19) estimates a minimum thermal income per unit current of 47 W/kA. Considering the nominal current  $I = 12 \,\mathrm{kA}$ , Eq. (3.18) gives the optimum design ratio of  $(\Delta l/A)_{\rm opt} = 0.5 \,\mathrm{mm^{-1}}$ , which means a length of about 157 mm if the diameter of the current lead is 20 mm. Lower heat leakage towards the cable and cooling power demand could be obtained by adding one or more intermediate cooling points along the current leads. In this case, the optimal temperatures and positions of the intermediate cooling points could be chosen by use of Eq. (3.18) and (3.19) depending on the actual relations of the efficiency of the used cooling system with respect to temperature.

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# Chapter 4

# Thermal-Hydraulic Design

#### Contents

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# 4.1 Introduction

The design of a cryogenic line, whether addressed at the transport of a cryogenic fluid or at the cooling of a superconducting cable, represents a complex task involving the solution of a concurrent thermal and fluid-dynamic problem applied to a compressible viscous fluid, usually with properties strongly dependent on the thermodynamic state. The governing equations are derived by the balances of mass, momentum and energy of the viscous fluid. When the cross section dimensions are negligible with respect to the longitudinal dimension (for the considered phenomenon), these equations reduce to 1D equations along the axial direction. In this case, viscous terms can be removed from derivative terms and macroscopically evaluated by a friction force factor F [1].

Making use of the substantive derivative (or Lagrangian derivative), which for

a moving fluid of speed v in the axial direction l is defined as:

$$\frac{\mathrm{D}}{\mathrm{D}t} = \frac{\partial}{\partial t} + v \frac{\partial}{\partial l}$$

the 1D balances of mass, momentum, and energy for a compressible fluid flowing in a non-deformable pipeline of uniform cross section A can then be written as:

$$\frac{\mathrm{D}\rho}{\mathrm{D}t} + \rho \frac{\partial v}{\partial l} = 0 \qquad (\mathrm{mass}) \qquad (4.1a)$$

$$\frac{\mathrm{D}v}{\mathrm{D}t} + \frac{1}{\rho} \frac{\partial p}{\partial l} = -F - g \frac{\mathrm{d}z}{\mathrm{d}l} \qquad (\text{momentum}) \qquad (4.1b)$$

$$\frac{\mathrm{D}u}{\mathrm{D}t} + \frac{p}{\rho}\frac{\partial v}{\partial l} = vF + \frac{q}{\rho A} \qquad (\mathrm{energy}) \tag{4.1c}$$

where  $\rho$  and u are respectively the density and the specific internal energy of the fluid, g the gravitational acceleration (in the vertical direction z) and q the total thermal income towards LH<sub>2</sub> per unit line length. The friction force factor F is related to the Moody-Darcy friction factor f by:

$$F = \frac{f}{D_{\rm h}} \frac{v|v|}{2} \tag{4.2}$$

where  $D_{\rm h}$  is the hydraulic equivalent diameter (for a cylindrical geometry  $D_{\rm h} = D_2 - D_1$ , where  $D_1$  and  $D_2$  are the inner and outer diameter of the cross section).

The system of equations (4.1a)-(4.1c) can be solved by choosing two of the four state variables  $p, T, \rho$  and u as the independent variables, and expressing the remaining by use of the state equation. In computational fluid dynamics, usually the density  $\rho$  and the total specific energy  $e = u + v^2/2$  are chosen as independent variables. For an horizontal line, this allows to write Eq. (4.1a)-(4.1c) in the following conservative form, which is suitable for numerical computations because numerical fluxes respect the physical conservation laws [2]:

$$f(p, \rho, e) = 0 \tag{(state)} \tag{4.3a}$$

$$\frac{\partial \rho}{\partial t} + \frac{\partial (\rho v)}{\partial l} = 0 \tag{(mass)}$$

$$\frac{\partial(\rho v)}{\partial t} + \frac{\partial(\rho v^2 + p)}{\partial l} = -\rho F \qquad (\text{momentum}) \qquad (4.3c)$$

$$\frac{\partial(\rho e)}{\partial t} + \frac{\partial[(\rho e + p)v]}{\partial l} = \frac{q}{A}$$
 (energy) (4.3d)

However, for the simulation of cryogenic lines and superconducting cables, whenever the examined problem does not involve shock waves propagation phenomena, Eq. (4.1a)-(4.1c) are often reported in a non-conservative form in which temperature and pressure appear as independent variables [3, 4]. This is preferable because allows to directly derive heat exchange balances between components (which explicitly depend on temperature gradients) and because properties of cryogenic fluids are usually available as the interpolation of tabular data in function of pressure and temperature. Considering an horizontal line, this set of non-conservative equations can be written as:

$$f(p, \rho, T) = 0 \tag{(state)} \tag{4.4a}$$

$$\frac{\mathrm{D}v}{\mathrm{D}t} + \frac{1}{\rho}\frac{\partial p}{\partial l} = -F \qquad (\mathrm{momentum}) \qquad (4.4\mathrm{b})$$

$$\frac{\mathrm{D}p}{\mathrm{D}t} + \rho c^2 \frac{\partial v}{\partial l} = \phi \left(\frac{q}{A} + \rho v F\right) \qquad (\text{derived}) \qquad (4.4c)$$

$$\frac{\mathrm{D}T}{\mathrm{D}t} + \phi T \frac{\partial v}{\partial l} = \frac{q}{\rho c_v A} + \frac{v}{c_v} F \qquad (\text{derived}) \qquad (4.4d)$$

where  $c_v$  and c are respectively the specific heat at constant volume and the isentropic speed of sound in the fluid, while  $\phi$  is the Gruneisen parameter, defined as:

$$\phi = \frac{\rho}{T} \left( \frac{\partial T}{\partial \rho} \right)_S$$

The solution of the systems of equations (4.3) or (4.4) provide an accurate description of the cryogenic fluid state when integrated along the line (the boundary conditions being expressed by the mass flow and thermodynamic state of the fluid at the incoming section and the thermal incomes and friction factor along the line). On the other hand, they give no explicit information on the amount of temperature increment and pressure drop along the line and their dependence on external parameters such as thermal incomes, friction factor and cross section size.

The designer of a cryogenic line must deal with both temperature and pressure constraints. A change in the pipe cross section, thermal insulation, or pipe roughness (choosing for instance a corrugated rather than a smooth pipe) would affect both temperature increment and pressure drop. The designer could be interested in minimizing one of these two gradients, because it represents the limiting factor. The systems of equations (4.3) or (4.4) does not make clear this interdependence of parameters, and could therefore be suitably used only for the "a posteriori" evaluation of a series of attempts during line design.

A convenient system of equations which explicitly provides the temperature increment and the pressure drop along the line can be easily derived when considering the flow of an incompressible fluid in steady state. In this case, the system of equations (4.3) immediately reduces to the following:

$$\rho = \text{const.}$$
 (state) (4.5a)

$$m = \text{const.}$$
 (mass) (4.5b)

$$\frac{1}{\rho}\frac{dp}{dl} = -F \qquad (\text{momentum}) \qquad (4.5c)$$

$$c_l \frac{\mathrm{d}I}{\mathrm{d}l} = F + \frac{q}{m}$$
 (energy) (4.5d)

where m is the mass flow rate and  $c_l$  the specific heat (which is equal at constant pressure or constant volume for an incompressible fluid).

The system of equations (4.5) can greatly ease the design process a cryogenic line [5]. On the other hand, in many situations the error introduced by neglecting the compressibility of a cryogenic fluid is unacceptable. This is particularly true for LH<sub>2</sub>: with reference to the temperature and pressure range between 15-25 K and 5-15 bar, its density varies by about the 20% and the ratio of specific heats (constant pressure over constant volume) is in average equal to 1.66. This makes the system of equations (4.5) inappropriate.

# 4.2 Simplified flow equations for compressible fluid in steady state

A set of explicit relations suitable for the cryogenic design of the LH<sub>2</sub> pipeline is here derived from the compressible fluid equations under steady state condition. The fluid equations are then simplified by eliminating all the terms which become negligible when flow speed v is far from sound speed c in the fluid (which is 1150 m/s in LH<sub>2</sub> at 20 K 10 bar). The obtained system of equations allows to easily take into account the effects of fluid compressibility as well as line inclination on the fluid temperature increment and pressure drop.

For a compressible fluid in steady state Eq. (4.1a)-(4.1c) reduce to:

$$v\frac{\mathrm{d}\rho}{\mathrm{d}l} + \rho\frac{\mathrm{d}v}{\mathrm{d}l} = 0$$
 (mass) (4.6a)

$$v\frac{\mathrm{d}v}{\mathrm{d}l} + \frac{1}{\rho}\frac{\mathrm{d}p}{\mathrm{d}l} = -F - g\frac{\mathrm{d}z}{\mathrm{d}l}$$
 (momentum) (4.6b)

$$v\frac{\mathrm{d}u}{\mathrm{d}l} + \frac{p}{\rho}\frac{\mathrm{d}v}{\mathrm{d}l} = vF + v\frac{q}{m}$$
 (energy) (4.6c)

Substitution of Eq. (4.6a) into Eq. (4.6b) and (4.6c) gives:

$$\frac{1}{\rho} \left( \frac{\mathrm{d}p}{\mathrm{d}l} - v^2 \frac{\mathrm{d}\rho}{\mathrm{d}l} \right) = -F - g \frac{\mathrm{d}z}{\mathrm{d}l} \tag{4.7a}$$

$$v\left(\frac{\mathrm{d}u}{\mathrm{d}l} - \frac{p}{\rho^2}\frac{\mathrm{d}\rho}{\mathrm{d}l}\right) = vF + v\frac{q}{m} \tag{4.7b}$$

The state equation can be expressed in differential form by use of the coefficient of isobaric thermal expansion  $\alpha$  and the coefficient of isothermal compressibility  $\beta$ , respectively defined as:

$$\alpha = -\frac{1}{\rho} \left( \frac{\partial \rho}{\partial T} \right)_p \qquad \qquad \beta = \frac{1}{\rho} \left( \frac{\partial \rho}{\partial p} \right)_T \qquad (4.8)$$

Introducing the following expressions for the total differential of density and

the total differential of internal energy:

$$\mathrm{d}\rho = -\rho\alpha\,\mathrm{d}T + \rho\beta\,\mathrm{d}p\tag{4.9a}$$

$$du = c_p \, dT + \frac{p}{\rho^2} \, d\rho - \frac{\alpha T}{\rho} \, dp \tag{4.9b}$$

where  $c_p$  is the specific heat at constant pressure, the terms in parentheses in Eq. (4.7a) and (4.7b) can be written as:

$$\frac{\mathrm{d}p}{\mathrm{d}l} - v^2 \frac{\mathrm{d}\rho}{\mathrm{d}l} = \rho \alpha v^2 \frac{\mathrm{d}T}{\mathrm{d}l} + (1 - \rho \beta v^2) \frac{\mathrm{d}p}{\mathrm{d}l}$$
(4.10a)

$$\frac{\mathrm{d}u}{\mathrm{d}l} - \frac{p}{\rho^2} \frac{\mathrm{d}\rho}{\mathrm{d}l} = c_p \frac{\mathrm{d}T}{\mathrm{d}l} - \frac{\alpha T}{\rho} \frac{\mathrm{d}p}{\mathrm{d}l}$$
(4.10b)

which substituted into Eq. (4.7a) and (4.7b) give:

$$\alpha v^2 \frac{\mathrm{d}T}{\mathrm{d}l} + (1 - \rho\beta v^2) \frac{1}{\rho} \frac{\mathrm{d}p}{\mathrm{d}l} = -F - g \frac{\mathrm{d}z}{\mathrm{d}l}$$
(4.11a)

$$c_p \frac{\mathrm{d}T}{\mathrm{d}l} - \frac{\alpha T}{\rho} \frac{\mathrm{d}p}{\mathrm{d}l} = F + \frac{q}{m} \tag{4.11b}$$

Drawing dT/dl from Eq. (4.11a) and substituting into Eq. (4.11b), one gets the following differential equation in the pressure p alone:

$$\left(1 - \rho\beta v^2 + \frac{T\alpha^2 v^2}{c_p}\right)\frac{1}{\rho}\frac{\mathrm{d}p}{\mathrm{d}l} = \\ = -\left(1 + \frac{\alpha v^2}{c_p}\right)F - \frac{\alpha v^2}{c_p}\frac{q}{m} - g\frac{\mathrm{d}z}{\mathrm{d}l}$$
(4.12)

Reversely, drawing dp/dl from Eq. (4.11a) and substituting into Eq. (4.11b), one gets the following differential equation in the temperature T alone:

$$\left(1 - \rho\beta v^2 + \frac{T\alpha^2 v^2}{c_p}\right)c_p\frac{\mathrm{d}T}{\mathrm{d}l} = \left(1 - \alpha T - \rho\beta v^2\right)F + \left(1 - \rho\beta v^2\right)\frac{q}{m} + \alpha Tg\frac{\mathrm{d}z}{\mathrm{d}l}$$
(4.13)

The terms in equations (4.12) and (4.13) can be simplified by eliminating negligible terms at low flow velocities, which can be highlighted introducing the

Parameter	Value
$\gamma$	1.66
$G_a$ $M_a^2$	$1.88 \times 10^{-5}$
$\gamma M_a^2$	$3.13 \times 10^{-5}$
$\frac{(\gamma - 1)}{(\gamma - 1)} \frac{M_a^2}{M_a^2} / G_a$	$1.25 \times 10^{-5}$ $4.10 \times 10^{-5}$

Table 4.1 Average value of dimensionless parameters for  $LH_2$  in the temperature and pressure range between 15–25 K and 5–15 bar flowing with maximum velocity of 5 m/s

following identities:

$$\rho\beta v^2 = \gamma M_a^2 \tag{4.14a}$$

$$\frac{T\alpha^2 v^2}{c_p} = (\gamma - 1) M_a^2$$
 (4.14b)

$$\frac{\alpha v^2}{c_p} = (\gamma - 1) \frac{M_a^2}{G_a} \tag{4.14c}$$

where  $\gamma$  is the ratio of specific heats,  $M_a = v/c$  the Mach number, and  $G_a = \alpha T$  the Gay-Lussac number. Tab. 4.1 shows the average values of these dimensionless parameters for LH<sub>2</sub> in the temperature and pressure range between 15–25 K and 5–15 bar flowing with a maximum velocity of 5 m/s.

By use of relations (4.14), Eq. (4.12) and (4.13) can be rewritten as:

$$(1 - M_a^2) \frac{1}{\rho} \frac{dp}{dl} = = -\left(1 + (\gamma - 1)\frac{M_a^2}{G_a}\right) F - (\gamma - 1)\frac{M_a^2}{G_a}\frac{q}{m} - g\frac{dz}{dl}$$
(4.15a)  
$$(1 - M_a^2) c_p \frac{dT}{dl} = = (1 - G_a - \gamma M_a^2) F + (1 - \gamma M_a^2) \frac{q}{m} - G_a g\frac{dz}{dl}$$
(4.15b)

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and thus simplified for low flow velocity into the following:

$$\frac{1}{\rho}\frac{\mathrm{d}p}{\mathrm{d}l} = -F - \frac{\alpha v^2}{c_p}\frac{q}{m} - g\frac{\mathrm{d}z}{\mathrm{d}l}$$
(4.16a)

$$c_p \frac{\mathrm{d}T}{\mathrm{d}l} = (1 - \alpha T) F + \frac{q}{m} - \alpha T g \frac{\mathrm{d}z}{\mathrm{d}l}$$
(4.16b)

Eq. (4.16a) and (4.16b) make clear the dependence of the fluid temperature and pressure gradients on external parameters (friction factor, heat incomes and line inclination). The fluid compressibility is introduced in these equations by the coefficient of isobaric thermal expansion  $\alpha$ . The simplified equations (4.16) are similar in form to the incompressible fluid equations (4.5). The comparison shows that, for LH<sub>2</sub> in the considered temperature and pressure interval, the term  $\alpha T \approx 0.3$  cannot be neglected with respect to unity in Eq. (4.16b). On the other hand, in Eq. (4.16a) the effect of heat incomes on the pressure gradient could be neglected with respect to viscous dissipation only after comparison of the order of magnitude of the term q/m with respect to F for the specific situation under study.

#### 4.2.1 Design of the LH<sub>2</sub> pipeline in steady state

In order to guarantee at the same time the maintaining of hydrogen at liquid state and the working of the superconducting line, the following constraints in the hydrogen temperature and pressure are here considered [6, 7]:

- maximum temperature: 25 K, due to the strong decrease of MgB<sub>2</sub> critical current density over this value;
- minimum pressure: 5 bar, in order to increase the hydrogen boiling point to  $27 \,\mathrm{K};$
- minimum temperature: 15 K, in order to avoid hydrogen solidification for pressures below 37 bar;
- maximum pressure: 15 bar, mainly dictated by cryostat mechanical resistance (considering a stainless steel cryostat with wall thickness of 1.5 mm).

Given the distance between pumping/cooling stations, the designer has to estimate the total thermal income towards  $LH_2$  and then choose a pipeline diameter and  $LH_2$  mass flow rate which respect these constraints.
Thermal incomes towards  $LH_2$  are due to heat leakage through the cryostat, AC loss in the superconducting cable, and ortho-para hydrogen conversion. As for the latter, hydrogen molecule exists in two forms depending on the spinning directions of the protons. Ortho-hydrogen makes up 75 % of hydrogen at room temperature, but is unstable at low temperatures. When liquefied, ortho-hydrogen changes to the more stable para-hydrogen over time, liberating heat which can cause hydrogen evaporation. To avoid this, an ortho-para conversion catalyst can be used during liquefaction [8]. Here it is assumed that the ortho-para hydrogen conversion is completed before  $LH_2$  enters the line. The heat leakage through the thermal insulation is estimated in the order of 2 W/m for a cryostat diameter of 12 cm. For a DC cable with limited current ripple, the AC loss in the superconductor is typically lower than 0.1 W/m, and is here neglected with respect to thermal incomes from the environment [9].

#### Preliminary design

For preliminary design, the line is supposed to be horizontal and the average gradients of temperature and pressure are estimated by introducing into Eq. (4.16a) and (4.16b) the average values of LH<sub>2</sub> properties in the temperature and pressure working range. In the following, the MgB<sub>2</sub> cable is supposed to occupy a central section of diameter  $D_1 = 2$  cm. The friction factor f for stainless steel corrugated tubes is evaluated as four times the friction factor of a smooth pipe with same diameter [10, 11]. The reference value of thermal incomes from the environment  $(q = 2 \text{ W/m for } D_2 = 12 \text{ cm})$  is linearly scaled with cryostat diameter.

Eq. (4.16a) and (4.16b) can therefore be used to draw diagrams of average pressure drop and temperature increment for different values of  $LH_2$  mass flow and pipeline cross section. Fig. 4.1 shows the average  $LH_2$  temperature increment along the line. For each diameter there is a range of allowed mass flow rate, the lower value being dictated by the thermal income and the higher by the viscosity dissipation. An increase in the cryostat diameter leads both to an increase of the higher limit (because of a lower viscosity dissipation) and of the lower limit (because of higher thermal incomes). Fig. 4.2 shows that the average pressure drop decreases with higher diameters and increases with higher flow rates.

Assuming the distance between cooling stations to be 20 km, in order to respect the LH<sub>2</sub> state constraints the temperature increment and the pressure drop have to



Figure 4.1 Average temperature increment (K/km) in function of  $LH_2$  mass flow rate and cryostat diameter.



Figure 4.2 Average pressure drop (bar/km) in function of  $LH_2$  mass flow rate and cryostat diameter.



Figure 4.3 Allowable design zone of the cryogenic pipeline for different values of LH<sub>2</sub> mass flow rate and cryostat diameter.

be kept respectively below 0.5 K/km and 0.5 bar/km. Fig. 4.3 show the allowable design zone. Chosen a cryostat diameter of 12 cm, the horizontal line represents the allowable values of LH<sub>2</sub> mass flow rate (between 0.44 kg/s to 1.34 kg/s, corresponding to flow speeds between 0.55 m/s to 1.69 m/s, and Reynolds numbers between  $2.6 \times 10^5$  to  $8.0 \times 10^5$ ).

Once the diameter  $D_2$  is chosen, it has to be considered how the system can respond to thermal incomes different from the design value. Fig. 4.4 shows the calculated average temperature increment along the line in function of LH<sub>2</sub> mass flow rate and thermal income, for a pipeline with  $D_2 = 12$  cm. Increasing the mass flow (within the allowed interval), it is possible to keep the temperature increment below the limit of 0.5 K/km with thermal incomes up to almost 6 W/m. Fig. 4.5 shows the required cooling power for the pipeline with  $D_2 = 12$  cm in function of LH<sub>2</sub> mass flow rate and thermal income.

#### Pipeline design verification

Once the line design is chosen with  $D_2 = 12$  cm, it is possible to verify the actual temperature and pressure profiles along the line by integrating Eq. (4.16a) and (4.16b) using local properties of LH<sub>2</sub>. This allows to consider the effect of



Figure 4.4 Average temperature increment (K/km) for the pipeline with  $D_2 = 12 \text{ cm}$  in function of LH<sub>2</sub> mass flow rate and thermal income.



Figure 4.5 Required cooling power  $(kW_{th}/km)$  for the pipeline with  $D_2 = 12 \text{ cm}$  in function of LH<sub>2</sub> mass flow rate and thermal income.

varying LH<sub>2</sub> properties along the line, and is particularly important when the line profile is not horizontal. In the following, the results for a bipolar line with two cryostats and warm dielectric are shown. It is assumed that the two cryostats are separately used as LH<sub>2</sub> going and return pipes, so as to free the net LH<sub>2</sub> mass flow rate transported along the line from the minimal LH<sub>2</sub> mass flow rate needed for the cooling of the line (recirculation flow). This allows the line to work also with zero net transported LH<sub>2</sub>. The two cases of horizontal line and deep bathymetric profile of the line are considered, the latter concerning the case of a 20 km submarine cable crossing the Gibraltar's strait [12].

Fig. 4.6 and 4.7 show the results for the case of horizontal line. The continuous lines represent temperature profiles along the going pipe for different LH<sub>2</sub> mass flow rates, the broken line represents the return pipe in case of minimum allowable recirculation flow. Given the incoming hydrogen temperature of 15 K, the minimum flow rate of 0.43 kg/s is imposed by the maximum temperature constraint (Fig. 4.6). As the flow rate is increased, the pressure drop increases. Hence, in order to respect the minimum pressure constraint of 5 bar, the incoming pressure has to be increased. The maximum pressure constraint leads to the maximum flow capacity of 1.34 kg/s (Fig. 4.7). Hence this design allows a net LH<sub>2</sub> mass flow rate between 0 kg/s to 0.92 kg/s. In this case, the calculated mass flow rate bounds are well in agreement with what obtained using constant averaged LH<sub>2</sub> properties (within 2.6 % error). Nevertheless, Fig. 4.6 shows that the temperature increment is not constant. Actually, the output temperature calculated for high flow rates is significantly higher than what estimated with averaged LH<sub>2</sub> values, with an error of 16.8 % for 1.34 kg/s.

Fig. 4.8 shows the bathymetric profile of a 20 km length line crossing the Gibraltar's strait. For this case, Fig. 4.9 and 4.10 show the profiles of temperature and pressure along the line. The calculated minimum mass flow rate is 0.42 kg/s (the little difference with respect to the horizontal line is caused by the influence of pressure on the LH<sub>2</sub> properties). On the other hand, the maximum mass flow rate is lowered to 1.03 kg/s due to the maximum pressure constraint. This leads to an allowable net transported LH<sub>2</sub> mass flow rate between 0 to 0.61 kg/s. As shown in Fig. 4.10, the point of maximum LH<sub>2</sub> pressure is not located at the pipe income but at the maximum depth section. This can lead to difficulties in measuring and maintaining the pressure higher bound.

A lower overall pressure drop along the bathymetric line can be observed with



Figure 4.6  $LH_2$  temperature along the horizontal line on the going pipe (for different mass flow rates) and return pipe.



Figure 4.7  $LH_2$  pressure along the horizontal line on the going pipe (for different mass flow rates) and return pipe.



Figure 4.8 Bathymetric profile of a 20 km length line crossing the Gibraltar's strait.

respect to the horizontal line. This is particularly evident at the minimum mass flow rate, where the overall pressure drop lowers from 1.25 bar to 0.78 bar. The reason could be imputed to the thermodynamic path the fluid undergoes along the line: it is compressed, absorbs heat from the environment, and then expands. The process can produce a self-pumping effect, which counteracts the flow pressure drop due to fluid viscosity. The pumping effect is increased by higher thermal incomes, especially when localized at deep line sections, which could also arise due to manufacturing imperfections or damage of the thermal insulation. For larger cryostat diameters, for which flow pressure drops are lower and thermal incomes are higher, this pumping effect could also cause the inlet pressure to be equal to the outlet pressure of the line (for instance with cryostat diameter of 18 cm, thermal incomes of 3 W/m, and  $\text{LH}_2$  mass flow rate of 0.68 kg/s). The described self-pumping effect cannot be outlined when using incompressible flow equations for cryogenic pipeline design, and could lead to difficulties in mass flow and pressure regulation if not adequately taken into account during plant design.



Figure 4.9 LH<sub>2</sub> temperature along the bathymetric line on the going pipe (for different mass flow rates) and return pipe.



Figure 4.10  $LH_2$  pressure along the bathymetric line on the going pipe (for different mass flow rates) and return pipe.

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### Part II

### Advanced Energy Recovery Systems from LH<sub>2</sub>

# Chapter 5

### Advanced Energy Recovery Systems from LH<sub>2</sub>

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#### 5.1 Introduction

Hydrogen can be stored and transported as a compressed gas, liquid, or metalhydride solid compound. Liquefaction allows high storage density, and can represent the appropriate solution when large scale hydrogen storage and long range transport is needed. On the other hand, the most disadvantaging aspect of liquefaction is the energy amount required by the process. This applies in similar way to the natural gas, which could be considered as a reference for future hydrogen market forecasts in the energy field. Actually, as a confirm that in many cases benefits of liquefaction can counterbalance the energy drawback, in 2002 about 26% of total natural gas international trade was transported in liquid form [1], with increasing trend. When compared to natural gas, hydrogen make the liquefaction process at the same time more demanded (due to its lower density) and less appealing (due to its higher liquefaction work).

As a reference case, one can consider a solar power plant of 6 GWh daily capacity, which represents the energy content of about 150 metric tons of hydrogen (in terms of HHV). A typical pack of 40 pressurized cylinders used for GH<sub>2</sub> delivering measures about  $12 \text{ m} \times 2.5 \text{ m} \times 2 \text{ m}$  and can store a useful mass of hydrogen of roughly 250 kg at 200 bar [2]. This means that considering the typical bottle size and spatial disposition, and calculating the ratio of total hydrogen mass storage over total occupied space, the actual plant storage density is just  $4.2 \text{ kg/m}^3$ . In order to store the whole daily energy generated by the plant, about 600 of these pressurized cylinder packs would be necessary (for a total of 24000 bottles), with an occupied volume of roughly  $36\,000 \text{ m}^3$ . The same hydrogen quantity occupies  $2100 \text{ m}^3$  in liquid form and can be stored in a single cryogenic vessel [3]. Moreover, the total cost for storage and transportation of LH<sub>2</sub> can be lower than for GH<sub>2</sub> [4].

For the today market, hydrogen is often stored and transported by trucks in liquid form. The best liquefaction plants require 36 MJ/kg, which is about 25 % of hydrogen HHV. Advanced liquefying systems have been proposed with energy requirements as low as 18-25 MJ/kg employing both mechanical or magnetic liquefaction systems [4–6] (in comparison with the minimum liquefaction work of 11.45 MJ/kg for an ideal reversible machine working at room temperature).

At the end of the storage and transport chain, gasification is always necessary for final use. Today vaporization systems directly provide heat absorbing it from the environment, loosing all the work spent for liquefaction, or even burning part of the gasified fuel. Attempts to recover part of the energy spent for LNG production were proposed, involving two different approaches: cooling power supply and power generation. For cooling power supply, LNG can be used for freezing food, cryogenic air separation, production of dry ice, air conditioning etc. For power generation, some recovery plants have been operating in Japan since 1970s, mainly using natural gas direct expansion in turbine or closed Rankine cycles with organic working fluid, achieving thermal efficiency in the range of 13-23% [7]. Advanced systems have been recently proposed for LNG cold energy recovery [7–9]. Some of them are aimed at CO<sub>2</sub> sequestration while producing electricity and natural gas [10, 11]. Direct thermoelectric conversion has also been considered, showing efficiencies up to 9% [12].

In this chapter, advanced LH<sub>2</sub> vaporizing systems aimed at the partial recovery of the energy spent for liquefaction are introduced and compared [13]. Differently from the LNG vaporization process, while heating from 20 K up to room temperature hydrogen absorbs heat mainly at variable temperature (sensible heat) rather than during constant temperature vaporization (latent heat). Hence, Brayton closed cycles with helium working gas are considered (helium is the only fluid that does not liquefy at 20 K 1 bar). This choice allows to obtain satisfying thermal match at the hydrogen heat exchanger. First, closed cycle Gas Turbines (GT) are considered, with hot sink from room temperature up to 1800 K. Then, in order to explore the full range of temperatures provided by  $H_2$ -air combustion, it is considered the use of combined cycles with topper MHD closed cycle generator in the range 2200-1300 K [14–16], and bottomer GT in the range 1300–20 K. All the considered systems have a double output (mass flow rate of  $GH_2$  at room temperature, and electric power). They are compared at various output ratios with conventional systems (reference thermal-electric combined system and fuel-cell system upon hydrogen vaporization) and with ideal systems. The recovered energy is evaluated as the higher electric energy produced with respect to conventional systems for the same burned  $GH_2$ . The recovery efficiency is evaluated as the ratio between the actual produced energy and the maximum energy produced by an ideal machine working under same conditions.

#### 5.2 Reference systems

#### 5.2.1 Conventional systems

The conversion of  $LH_2$  into  $GH_2$  with conventional systems can be carried out with one of the two processes today used for LNG vaporization [7]:

- Open Rack Vaporization (ORV): consists in the direct heat exchange with water withdrawn from sea or river. However, exchanging heat with the environment can be problematic when dealing with cryogenic fluids. This solution can be employed only if sufficiently hot water is available on site, hence usually at relatively low latitudes or when external heat sources are available [17].
- Submerged Combustion Vaporization (SCV): involves the combustion of part of the fuel itself to heat a water bath which is used for vaporization. The water is recycled and reheated in a closed loop system. Today, 75 % of the world LNG gasification facilities employ this technology [18]. While the SCV process burns about 1.5–2 % of LNG to provide the heat of vaporization, in the case of LH<sub>2</sub> a quote between 3–4 % can be estimated.

After vaporization, part of the hydrogen can be reconverted into electric energy either in a conventional thermal-electric or fuel-cell system. For reference, we consider respectively a thermal-electric GT-steam combined plant with total conversion efficiency of 55 % over LHV (which is 47 % over HHV in the case of hydrogen) and a SOFC-GT (Solid Oxide Fuel Cell combined plant) with efficiency of 60 % over HHV [19–23].

#### 5.2.2 Ideal systems

Energy balances of ideal energy recovery systems are shown in Fig. 5.1. On the left, it is shown the ORV-like recovery system, which works as the reverse machine of the ideal liquefier, returning the liquefaction work. On the right, it is shown the SCV-like recovery system, which burns a fraction  $X_{\rm id}$  of output GH<sub>2</sub>, and delivers the remaining  $(1 - X_{\rm id})$ .

The recovery systems produce work while releasing heat to  $LH_2$ . The higher energy recovery limit can be estimated by considering an ideal thermodynamic machine working between a hot sink at temperature  $T_h$  and  $LH_2$  as cold sink.



Figure 5.1 Global energy balance of the ideal energy recovery systems.

Taking into account the actual variable temperature at which hydrogen absorbs heat from 20 K (liquid) up to 288 K (gas), the higher recoverable energy is then:

$$E_{\mathrm{id}\,T_{\mathrm{h}}} = T_{\mathrm{h}}\,\Delta s_{\mathrm{H}_2} - \Delta h_{\mathrm{H}_2} \tag{5.1}$$

where  $\Delta s_{\rm H_2} = 52.9 \,\text{kJ/kgK}$  and  $\Delta h_{\rm H_2} = 3785 \,\text{kJ/kg}$  are the hydrogen specific entropy and enthalpy variations during vaporization. In Eq. (5.1) the term  $T_{\rm h} \Delta s_{\rm H_2}$  represents the heat absorbed from the hot sink, and  $\Delta h_{\rm H_2}$  represents the heat released to the cold sink (hydrogen).

For the ORV-like process,  $T_{\rm h} = 288 \,\mathrm{K}$  (room temperature) gives  $E_{\rm id \, ORV} = 11.45 \,\mathrm{MJ/kg_{LH_2}}$ . This is also the minimum energy required by an ideal liquefier. Hence using ideal machines both for liquefaction and vaporization exchanging heat with the environment at the same temperature would bring to complete energy recovery, as a result of their reversibility. Given an energy recovery ORV system which produces  $E_{\rm ORV}$  while vaporizing  $1 \,\mathrm{kg_{LH_2}}$ , the system efficiency with reference to the ideal ORV system can be defined as:

$$\eta_{\rm ORV} = \frac{E_{\rm ORV}}{E_{\rm id\,ORV}} \tag{5.2}$$

For the case of SCV-like process,  $T_{\rm h}$  is the temperature of the combustion

chamber, fueled with part of the exiting  $GH_2$ . Assuming ideal combustion and heat exchange processes, the hydrogen burned fraction is given by:

$$X_{\rm id\,SCV} = \frac{T_{\rm h}\,\Delta s_{\rm H_2}}{\rm HHV_{\rm H_2}} \tag{5.3}$$

where  $HHV_{H_2} = 141.9 \text{ MJ/kg}$  is the higher heating value of hydrogen. Eq. (5.1) can then be written as:

$$E_{\rm id\,SCV} = X_{\rm id\,SCV}\,\rm HHV_{\rm H_2} - \Delta h_{\rm H_2} \tag{5.4}$$

For  $T_{\rm h} = 288 \,\mathrm{K}$  the SCV system produces the same work of ORV while burning 10.7% of incoming LH<sub>2</sub>. Higher combustion temperatures lead to higher extracted work and burning fractions. The higher limit ( $X_{\rm id} = 1$ ) is given by  $T_{\rm h} = 2683 \,\mathrm{K}$ . This temperature can be reached with H<sub>2</sub>–O<sub>2</sub> combustion and leads to  $E_{\rm id\,SCVmax} = \mathrm{HHV}_{\mathrm{H}_2} - \Delta h_{\mathrm{H}_2} = 138.1 \,\mathrm{MJ/kg}_{\mathrm{LH}_2}$ . Given an SCV energy recovery system which burns a fraction  $X_{\rm SCV}$  of hydrogen producing  $E_{\rm SCV}$  per unit mass of incoming hydrogen, the system efficiency with reference to the ideal SCV system which burns the same hydrogen fraction can be defined as:

$$\eta_{\rm SCV} = \frac{E_{\rm SCV}}{X_{\rm SCV} \,\rm HHV_{\rm H_2} - \Delta h_{\rm H_2}} \tag{5.5}$$

#### 5.3 Model description

The efficiencies of the proposed systems are evaluated using real properties of fluids [24]. The analysis is here focused on the vaporization process, and the recovered energy is evaluated with reference to the energy spent for liquefaction. The energy consumption which is needed to maintain the low temperature during  $LH_2$  storage and transport is neglected, because depends on a series of unknown parameters such as size of the storage tank, cryogenic insulation design, storage and transportation time.

In order to ease the comparison of systems working at different hot sink temperatures, common assumption for pressure ratios and component's efficiencies are chosen. However, the optimization of some selected systems among those here introduced could be done through parametric analysis starting from proposed values. Main assumption through all evaluations are discussed in the following, and

GT systems		
	Combustion efficiency	99%
	Turbine polytropic efficiency	86%
	Compressor polytropic efficiency	86%
	Pressure ratio	18
	Electro-mechanical conversion efficiency	90%
MHD systems		
	Combustion efficiency	99%
	MHD isentropic efficiency	84%
	Compressor polytropic efficiency	86%
	Pressure ratio	6
	DC/AC power conversion efficiency	97%
Heat exchangers		
	Efficiency	93-94%
	$\Delta T$ at pitch point (20 K)	$2\mathrm{K}$
	$\Delta T$ at pitch point (80 K)	$3 - 4 \mathrm{K}$
	$\Delta T$ at pitch point (288 K)	$1520\mathrm{K}$
	$\Delta T$ at pitch point (1200 K)	$5060\mathrm{K}$

 Table 5.1
 Main assumptions through all evaluations.

summarized in Tab. 5.1.

- For GT systems, the pressure ratio is selected as 18 (with lower pressure of 1 bar). This value showed to be in average close to optimum upon preliminary design estimation for most of the compared systems, and is consistent with typical values of commercially available GT systems [20, 25]. The polytropic efficiency of compressors and turbines is set to 86 % [10, 25–28]. Using known relations between polytropic and isentropic efficiencies, this leads to turbine and compressor isentropic efficiencies respectively of 77 % and 92 % in case of helium working gas (while they would be respectively 80 % and 90 % in case of air working gas). The combustion efficiency is 99 %, and the overall mechanic, organic and electric generator efficiency is 90 % [25]. The hot sink temperature ranges from 288 K (room temperature) to 1800 K [29].
- For MHD systems, data are obtained from [15], referring to the potassium seeded helium closed cycle. Inlet and outlet stagnant pressures are respectively 3.6 and 0.6 bar, while inlet temperature is 2200 K. The isentropic

expansion efficiency is 84%. For the compressor, the same polytropic efficiency as before is chosen. This leads to an isentropic efficiency of 80% for helium working gas with pressure ratio equal to 6. The combustion efficiency is 99%, and the DC/AC power conversion efficiency is 97% [23].

- The heat exchangers efficiency is assumed as  $\epsilon = \Delta h / \Delta h_{\rm max} = 93-94 \%$  [28, 30]. Temperature difference at pinch point is selected depending on heat exchanger working temperature as: 2 K at 20 K, 3-4 K at 80 K, 15-20 K at 288 K, and 50-60 K at 1200 K [10, 30]. The temperature of incoming LH<sub>2</sub> is 20 K (boiling point at 1 bar), and the available room temperature is 288 K. Subcritical hydrogen evaporation is considered. Supercritical evaporation could be considered if high pressure output GH<sub>2</sub> is required. He-LH<sub>2</sub> thermal match in the heat exchanger would be slightly improved, being LH<sub>2</sub> absorbed latent heat much smaller than sensible heat.

For each component, output states are computed by imposing the following energy balances:

- Compression:  $\eta_{\rm isc} = \Delta h_{\rm is} / \Delta h$ , where  $\eta_{\rm isc}$  is the compression isentropic efficiency and  $\Delta h_{\rm is}$  is the enthalpy jump for isentropic compression.
- Expansion:  $\eta_{\rm ise} = \Delta h / \Delta h_{\rm is}$ , where  $\eta_{\rm ise}$  is the expansion isentropic efficiency and  $\Delta h_{\rm is}$  is the enthalpy jump for isentropic expansion.
- Heat exchange:  $\epsilon = \Delta h / \Delta h_{\text{max}}$ , where  $\epsilon$  is the heat exchanger efficiency and  $\Delta h_{\text{max}}$  is the maximum available enthalpy jump. Temperature difference at pitch point is selected depending on temperature working range.

For the MHD systems,  $H_2$ -air combustion is considered, because it reaches temperatures of 2380 K which are above the 2200 K required by the considered MHD generator [15]. Anyway, because hydrogen production by water electrolysis also provides oxygen at stoichiometric ratio, higher electric conversion efficiency could be reached when considering  $H_2$ -O<sub>2</sub> combustion, which has flame temperature of 3080 K. This could make MHD adoption more convenient and provide higher energy recovery factors. Further system optimizations include the possibility of using LH<sub>2</sub> as coolant for the thermal shield of the MHD superconducting magnet.



Figure 5.2 Scheme and T-s diagram of the ORV-like GT system.

	T (K)	p (bar)	$\rho~(\rm kg/m^3)$	$u \; (kJ/kg)$	$h \; (kJ/kg)$	s~(kJ/kgK)
1	22.0	1.0	2.19	-1480.74	-1435.01	18.02
2	84.2	18.0	10.29	-1286.76	-1111.72	18.99
3	271.0	18.0	3.20	-704.36	-141.08	25.06
4	100.1	1.0	0.48	-1237.08	-1028.93	25.90

Table 5.2Thermodynamic states of the ORV-like GT cycle.

#### 5.4 Recovery systems

#### 5.4.1 Closed Cycle Gas Turbine at 288 K

This system, shown in Fig. 5.2, is a ORV-like recovery system which absorbs heat from the environment at 288 K through heat exchange with sea water. Thermodynamic states are reported in Tab. 5.2. The temperature of He after turbine expansion is  $T_4 = 100$  K, and the output hydrogen temperature is 95 K. Hence, in order to reach 288 K, another heat exchange with sea water would be necessary (not shown in Fig. 5.2). Higher output temperature and extracted work could be reached using multistage reheated expansion, leading to slight improvements at the expense of system design simplicity. Compared to the conventional ORV process, this system leads to an energy recovery of  $1.6 \text{ MJ/kg}_{LH_2}$ .



**Figure 5.3** Scheme and T-s diagram of the GT system with TIT at 820 K.

	T (K)	p (bar)	$ ho~({\rm kg/m^3})$	$u \; (kJ/kg)$	$h \; (kJ/kg)$	$s~(\rm kJ/kgK)$
1	22.0	1.0	2.19	-1480.74	-1435.01	18.02
2	84.2	18.0	10.29	-1286.76	-1111.72	18.99
3	820.0	18.0	1.06	1007.43	2711.80	30.82
4	303.0	1.0	0.16	-604.52	25.31	31.65

Table 5.3 Thermodynamic states of the GT cycle with TIT at 820 K.

#### 5.4.2 Closed Cycle Gas Turbine at 820 K

This system and all the following ones avoid heat exchange with the environment, and provide heat for hydrogen vaporization by burning part of the output GH<sub>2</sub>. For this reason these systems are not comparable with ORV-like systems, but only among SCV-like systems. The system is shown in Fig. 5.3, and thermodynamic states are reported in Tab. 5.3. The Turbine Inlet Temperature (TIT) of 820 K is chosen because it leads to a helium temperature after turbine expansion  $T_4 = 303$  K. This allows the output GH<sub>2</sub> to reach 288 K. In accordance with Eq. (5.1), the higher hot sink temperature leads to higher extracted energy per kg of vaporized LH<sub>2</sub>: this system produces  $6.1 \text{ MJ/kg}_{LH_2}$  of net electric energy while burning 9.4% of output GH<sub>2</sub>.

#### 5.4.3 Closed Cycle Gas Turbine at 1200–1800 K

Four increasing TITs are here considered: 1200 K, 1400 K, 1600 K and 1800 K [29]. The last case, which has the best efficiency, is shown in Fig. 5.4, and its thermo-



Figure 5.4 Scheme and T-s diagram of the GT with TIT at 1800 K.

	T (K)	p (bar)	$\rho~(\rm kg/m^3)$	$u \ (kJ/kg)$	$h \ (kJ/kg)$	$s~(\rm kJ/kgK)$
1	22.0	1.0	2.19	-1480.74	-1435.01	18.02
2	84.2	18.0	10.29	-1286.76	-1111.72	18.99
3	444.4	18.0	1.95	-163.75	759.89	27.63
4	1800.0	18.0	0.48	4063.08	7804.38	34.90
5	665.2	1.0	0.07	524.66	1907.21	35.74
6	305.0	1.0	0.16	-598.34	35.60	31.68

Table 5.4 Thermodynamic states of the GT cycle with TIT at 1800 K.

dynamic states are reported in Tab. 5.4. Because helium temperature at turbine outlet is higher than needed for hydrogen heating, a regenerator can be inserted in the cycle. In this case, a low efficiency of the regenerator in the range of 50–60 % is enough for lowering helium temperature to about 300 K. This allows output GH<sub>2</sub> to be heated up to 288 K. The use of higher regenerator efficiency would save fuel consumption at the combustion chamber, but would lower  $T_6$  too much, hence requiring a similar amount of fuel for heating output GH<sub>2</sub> up to 288 K. For the four considered TITs, the system burns respectively 12.5, 14.1, 15.7, and 17.3 % of hydrogen producing 9.4, 11.1, 12.8 and 14.5 MJ/kg<sub>LH<sub>2</sub></sub> of net electric energy.

In order to evaluate the relevance of assumptions made in Tab. 5.1 on the results, a parametric analysis of this system with fixed TIT at 1800 K and varying



**Figure 5.5** Scheme and T-s diagram of the single closed cycle MHD.

	T (K)	p (bar)	$\rho~(\rm kg/m^3)$	u~(kJ/kg)	$h~(\rm kJ/kg)$	$s~(\rm kJ/kgK)$
1	22.0	0.6	1.33	-1480.7	-1435.0	19.1
2	50.8	3.6	3.45	-1390.9	-1285.3	19.7
3	1000.3	3.6	0.18	1569.7	3648.9	35.2
4	2200.0	3.6	0.08	5310.3	9883.0	39.3
5	1254.5	0.6	0.02	2362.3	4969.8	40.1
6	305.0	0.6	0.10	-598.3	35.6	32.7

Table 5.5Thermodynamic states of the single closed cycle MHD.

pressure ratio is carried out. The efficiencies of compressor and turbine are varied accordingly to pressure ratio. Results show that the chosen value of  $\beta = 18$  can be considered as satisfying. Increasing the pressure ratio over 18 leads to little improvements, up to the value of  $\beta = 26$  for which the net produced electric energy is 14.8 MJ/kg<sub>LH2</sub> with 17.5% of burned hydrogen.

#### 5.4.4 Single Closed Cycle MHD

In order to evaluate the achievable improvements with higher hot sink temperatures, the use of an MHD generator is here considered. This system, shown in Fig. 5.5, has inlet temperature of 2200 K [15], which is close to the highest temperature achievable with H<sub>2</sub>-air combustion. Thermodynamic states are reported in Tab. 5.5. The output  $GH_2$  leaves the system at 288 K. This system burns 15.3% of hydrogen producing  $13.7 \text{ MJ/kg}_{LH_2}$  of net electric energy.

#### 5.4.5 Combined MHD-GT

This combined system uses the closed cycle MHD as topper, and the helium GT as bottomer (with TIT at 1200 K). Two schemes are considered. The former, shown in Fig. 5.6, uses the regenerator inside the GT cycle. This configuration, marked with label (1), leads to the combustion of 20.3% of hydrogen producing  $18.5 \text{ MJ/kg}_{LH_2}$  of net electric energy. The latter, shown in Fig. 5.7, uses the regenerator to heat helium downstream the compressor of the MHD cycle. This configuration, marked with label (2), leads to the combustion of 22.6% of hydrogen producing  $21.2 \text{ MJ/kg}_{LH_2}$  of net electric energy. The thermodynamic states of the two systems are reported respectively in Tab. 5.6 and 5.7.

#### 5.5 Comparison

The efficiencies of the proposed systems are evaluated by comparison with ideal reversible systems (higher end) and with conventional reference systems (lower end). All the proposed energy recovery systems have a double output: a mass flow rate of GH<sub>2</sub> at room temperature, and a net electric power. The comparison is done for the same output GH<sub>2</sub> per incoming kg of LH<sub>2</sub>. With reference to ideal systems, the efficiency is evaluated respectively with Eq. (5.2) or Eq. (5.5) for ORV or SCV-like recovery systems. With reference to conventional systems, the higher extracted electric energy for the same burned fraction is expressed as the difference  $\Delta E = E_{\rm el\,rec.\,sys.} - E_{\rm el\,conv.}$ . The GT@288K system is compared with the plain ORV process, while the other systems are compared with the conventional SCV process with following partial conversion into electric energy. Two values are derived by considering respectively the reference thermal-electric and fuel-cell systems.

The calculated performance of the considered systems are reported in Tab. 5.8 and shown in the diagram of Fig. 5.8, where the continuous lines represent the electric energy extracted by an ideal SCV-like recovery system (high end) and by reference thermal-electric and fuel-cell systems (low end). The global energy conversion efficiency of proposed systems is always higher than for reference con-



**Figure 5.6** Scheme and T-s diagram of the combined MHD-GT (1).

	T (K)	p (bar)	$\rho~(\rm kg/m^3)$	$u \; (kJ/kg)$	$h \ (kJ/kg)$	$s~(\rm kJ/kgK)$
1	22.0	1.0	2.22	-1480.7	-1435.0	18.0
2	84.2	18.0	10.42	-1286.8	-1111.7	19.0
3	222.7	18.0	3.94	-855.1	-392.3	24.0
4	1200.0	18.0	0.73	2192.3	4686.5	32.8
5	443.4	1.0	0.11	-166.7	755.0	33.6
6	305.0	1.0	0.16	-598.3	35.6	31.7
7	234.7	0.6	0.12	-817.5	-329.7	31.4
8	542.0	3.6	0.32	140.8	1267.4	32.0
9	2200.0	3.6	0.08	5310.3	9883.0	39.3
10	1254.5	0.6	0.02	2362.3	4969.8	40.1

Table 5.6Thermodynamic states of the combined MHD-GT (1).



**Figure 5.7** Scheme and T-s diagram of the combined MHD-GT (2).

	T (K)	p (bar)	$ ho~({\rm kg/m^3})$	$u \; (kJ/kg)$	$h ~({\rm kJ/kg})$	s~(kJ/kgK)
1	22.0	1.0	2.22	-1480.7	-1435.0	18.0
2	84.2	18.0	10.42	-1286.8	-1111.7	19.0
3	1200.0	18.0	0.73	2192.3	4686.5	32.8
4	443.4	1.0	0.11	-166.7	755.0	33.6
5	305.0	1.0	0.16	-598.3	35.6	31.7
6	88.0	0.6	0.33	-1275.0	-1092.0	26.3
7	203.2	3.6	0.86	-915.6	-493.2	26.9
8	348.0	3.6	0.50	-464.4	258.9	29.7
9	2200.0	3.6	0.08	5310.3	9883.0	39.3
10	1254.5	0.6	0.02	2362.3	4969.8	40.1

Table 5.7Thermodynamic states of the combined MHD-GT (2).



Figure 5.8 Comparison of output electric energy and  $GH_2$  per unit mass of input  $LH_2$ .

Table 5.8	Comparison of	proposed	systems	with	$\operatorname{respect}$	$\operatorname{to}$	ideal	and	conven-
	tional systems.								

System	X (%)	$E_{\rm el}~({\rm MJ/kg_{LH_2}})$	$\eta~(\%)$	$\Delta E \ (\mathrm{MJ/kg_{LH_2}})$
Conv. ORV	0	0		
GT@288K	0	1.6	13.9	1.6
Conv. SCV	3.3	0		
GT@820K	9.4	6.1	64.5	1.2 - 2.3
GT@1200K	12.5	9.4	67.4	1.8 - 3.5
GT@1400K	14.1	11.1	68.4	2.2 - 4.2
GT@1600K	15.7	12.8	69.2	2.6 - 4.9
GT@1800K	17.3	14.5	69.8	3.0 - 5.6
MHD (single)	15.3	13.7	76.7	3.9 - 6.1
MHD-GT $(1)$	20.3	18.5	74.2	4.6 - 7.7
MHD-GT $(2)$	22.6	21.2	75.3	5.4 - 9.0

ventional thermal-electric or SOFC-GT combined systems. The efficiency to ideal is in the range of 64–77 % for all the SCV systems, with higher values for higher hot sink temperature. The use of the "MHD-GT (2)" energy recovery system leads to an electric energy output which is up to  $9 \text{ MJ/kg}_{\text{LH}_2}$  higher than the current conventional combined GT-vapor thermal-electric system for the same GH<sub>2</sub> output. The higher electric output can be considered as the energy recovery in the amount of 25 % for the reference value of 36 MJ/kg spent for hydrogen liquefaction, and up to 36-50 % for a value of 18-25 MJ/kg in case of proposed high efficiency liquefying systems [4, 5].

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### Part III

## Electrical Simulation of an MgB<sub>2</sub>/LH<sub>2</sub> Combined System for RES Power Regulation and Transport

# Chapter 6

## Dynamic Circuital Model for Electrochemical PEM Devices

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#### 6.1 Introduction

For the simulation of the electrical system the commercial code "ElectroMagnetic Transients Program" (EMTP) was chosen. In this chapter, the dynamic circuital model which was developed for the simulation of the fuel-cell and electrolyzer systems is described. The choice of considering Proton Exchange Membrane (PEM) devices was mainly dictated by research interest reasons and by good characteristics of efficiency and rapid response over a wide range of working power. The use of pure electrolytic hydrogen do not pose problems of electrodes poisoning. PEM devices are today object of strong research aimed at increasing the efficiency, reliability, and duration, reducing the cost per unit power, and developing large power devices [1–3].

Several models for PEM fuel cell and electrolyzer devices can be found in the literature. They can be roughly divided in two categories: theoretical models (also named mechanistic models) and empirical models. Theoretical model equations are directly derived by analyzing the physics of the occurring electrochemical processes. Some of them, in which all variables are treated with local description [4], are too complex and computation demanding to be suitably implemented in environments such as EMTP for overall electrical system simulations. Others, which are suitable for circuital modeling, resort to lumped parameters, whose average value can be empirically estimated. This is the case of the Amphlett model [5, 6], which is largely quoted in the literature and has been later improved to take into account the effects of the actual working condition of the PEM membrane [7, 8] and its degradation over time [9]. To a certain extent, theoretical models can predict the behavior of devices which are not actually available for experimental measurements, by appropriately scaling the known parameters referring to existing similar devices.

On the other hand, empirical models directly introduce semi-empirical equations, whose parameters are calculated by experimental data fitting. In these models, some of the equations may be derived by theoretical ones (such as Nernst, Tafel and Ohm laws), while others are absolutely arbitrary. An example of this approach is the Kim model [10], later extended by Lee [11]. Empirical models allow to easily characterize an existing model of fuel cell, but can hardly be extended
to other models without a new experimental identification of parameters.

Dynamic circuital models which allow to take into account the double-layer charging effect (due to charge accumulation at the membrane boundaries), thermal transients, as well as the effect of overall delay between the change in the load current and in the gas flows are available [8, 12–14]. However, dynamic circuital models of fuel cells and electrolyzers are usually weak in the low current zone. This is a consequence of using the explicit Tafel equation for calculating the activation potential drop. Numerical problems may arise for instance when considering the connection of a fuel cell with power electronics devices or interruption switches. A zeroing in the membrane current of a dynamic circuital model could also temporarily arise when simulating fast transients.

The Tafel equation was empirically developed in 1905, and estimates the activation overpotential  $V_{\text{act}}$  in an electrochemical reaction occurring with current density rate J as [15]:

$$V_{\rm act} = b \cdot (\ln J_0 \pm \ln J) \tag{6.1}$$

where  $J_0$  is the exchange current density at equilibrium (which is the value of equal current in opposite directions with continuous reaction at equilibrium), and b is a parameter called Tafel slope. Due to the logarithmic term, the Tafel equation diverges at zero current.

Actually, Eq. (6.1) is an approximation of the more general Butler-Volmer equation [15, 16], which can be theoretically derived, and represents an implicit expression in the activation potential:

$$J = J_0 \left( \exp \frac{\alpha n F V_{\text{act}}}{R_0 T} - \exp \frac{-(1-\alpha) n F V_{\text{act}}}{R_0 T} \right)$$
(6.2)

where n is the charge number of the particular ion species transferred in the process, F the Faraday constant,  $R_0$  the universal gas constant, T the reaction temperature, and  $\alpha$  the chemical activity parameter (also called symmetry factor). Usually, when  $|V_{\text{act}}| > 0.1 \text{ V}$  one of the two exponential can be neglected. In this case, the inversion of Butler-Volmer gives the Tafel equation, which is hence valid only as a large-current approximation.

Some electrical models of fuel cells use Eq. (6.2) for accurate activation potential estimation at low current densities [16]. However, in dynamic circuital models the Tafel equation is preferred because of its explicit form in the activation potential, which allows to implement the activation potential loss using a controlled voltage source. As a result, they cannot generally work at open-circuit.

Some authors have dealt with this problem, and have proposed ways to dodge the divergence in the model equations while keeping the explicit form. In [14] the logarithmic term in Eq. (6.1) is series expanded, and the polynomial coefficients are then found by experimental data fitting. In theory, it may be also possible to calculate the coefficients known the theoretical equations. However, any little change in a parameter would require a new identifications of all the coefficients. Moreover, the series expansion of logarithmic terms lead to relevant errors in some working conditions. Another approach is proposed in [17] where a steady-state electrical model is introduced for fuel cell, electrolyzer or regenerative fuel cell whose equations do not divergence at zero current. However, these equations are arbitrarily introduced by data-fitting of experimental data, thus making the model unable to predict the behavior of unavailable devices.

The here presented dynamic circuital model belongs to the category of lumped parameters theoretical models. The equations are kept as close as possible to the relations which describe the occurring electrochemical processes. The model is easily scalable with number of cells in the stack, and with membrane area (under uniform current density assumption). The divergence at zero current is avoided by directly implementing in EMTP the implicit Butler-Volmer equation for activation potential calculation. Moreover, the model is able to take into account relevant effects at low currents which are due to internal currents and fuel crossover through the membrane. The obtained equations keep valid also when inverting the current sign, thus allowing to simulate a fuel cell, electrolyzer or regenerative fuel cell throughout its complete working behavior.

## 6.2 Description of the model

The dynamic model is obtained under the following common assumptions [5, 7, 8, 14]:

- one-dimensional treatment;
- constant pressures in the fuel-cell gas flow channels;
- the fuel cell works under 100 °C and the reaction product is in liquid phase;
- thermodynamic properties are evaluated at the average stack temperature,



Figure 6.1 Typical fuel cell characteristic (cell voltage versus membrane current density).

temperature variations across the stack are neglected;

- parameters for individual cells connected in series and/or in parallel can be lumped together to represent a fuel-cell stack system.

Fig. 6.1 shows a typical steady-state electrical characteristic of a fuel cell, which is usually divided in three regions. In region 1, the voltage decreases drastically due to the oxygen electrochemical activation reactions. In region 2, the curve is roughly linear, and is due to the sum of activation and resistive loss. The region 3 corresponds to the diffusion loss, which becomes predominant at high current densities due to slow transportation of reactants (products) to (from) the reaction sites.

The ideal potential related to the occurring electrochemical reaction is given by the Nernst equation. However, even with zero stack current, the measured open-circuit voltage  $E_0$  is lower than ideal. In the presented circuital model, part of this parasitic loss is attributed to internal currents and fuel crossover [16]. In fact, while the PEM membrane should be permeable only to protons, some electrons still migrate through it to the cathode directly, and also some fuel can cross over the membrane, to be directly oxidized at the cathode. This means that, even if the cell is in open-circuit, actually the electrochemical reaction is activated by internal currents. Due to the exponential term in the Butler-Volmer equation, parasitic currents of few mA/cm<sup>2</sup> lead to significant activation polarization loss.

# 6.3 Model equations and EMTP implementation

The scheme of the dynamic circuital model as implemented in EMTP is shown in Fig. 6.2. The internal potential, activation, concentration and ohmic loss (due to the membrane resistance to proton crossing) are separately calculated by the respective blocks. The resistance to electron conduction on collectors and electrodes is assumed to be constant, and is represented by the resistor  $R_{\rm oc}$ . The capacitor C introduces the dynamic effect due to the double-layer charging effect [18]. The overall effect of internal currents and fuel crossover are introduced through the current source  $I_{\rm int}$ . The on/off switch is added in order to simulate the stand-by mode, for which fuel loss due to parasitic loss is avoided. The model equations of each of the components in Fig. 6.2 are described in the following.

#### 6.3.1 Internal potential

With reference to a single cell, the Nernst equation can be written as [19]:

$$E_{\text{Nernst}} = e_0 + e_1 \cdot (T - T_{\text{ref}}) + e_2 \cdot \ln\left(p_{\text{H}_2} \cdot p_{\text{O}_2}^{0.5}\right)$$
(6.3a)

$$e_0 = \frac{\Delta G}{2F}, \qquad e_1 = \frac{\Delta S}{2F}, \qquad e_2 = \frac{R_0 T}{2F}$$
 (6.3b)

where  $\Delta G$  and  $\Delta S$  are respectively the reaction change in the free Gibbs energy and entropy, and p indicates the effective partial pressure in the anode and cathode channel. The temperature T denotes the cell operation temperature while  $T_{\text{ref}}$  is the reference temperature. Typical values of these chemical parameters can be found in [7].

The overall effect of delays between the change in the load current and the flow of fuel and oxidant is introduced by subtracting the following corrective potential in the Laplace domain:

$$E_{\rm d}(s) = k_{\rm d} \cdot \frac{s}{s+1/\tau_{\rm d}} \cdot J(s) \tag{6.4}$$

where parameters  $k_{\rm d}$  and  $\tau_{\rm d}$  were obtained from [14].

The cell internal potential  $E_{\text{cell}} = E_{\text{Nernst}} - E_{\text{d}}$  is then multiplied by the number of cells connected in series  $N_{\text{s}}$  to obtain the stack internal potential  $E_{\text{stk}}$ . Fig. 6.3 shows the EMTP implementation of the internal potential block.



Figure 6.2 Scheme of the dynamic circuital model as implemented in EMTP.



Figure 6.3 EMTP implementation of the internal potential block.

## 6.3.2 Activation voltage drop

Activation polarization arises both at the anode and the cathode of a PEM device. However, because the hydrogen reaction is much faster than the oxygen reaction, the polarization behavior of the cell is only slightly influenced by the hydrogen kinetic. Thus, the activation polarization at the anode can be neglected with respect to the activation polarization at the cathode.

Multiplying Eq. (6.2) by the membrane active area A and dividing by the total activation potential of the series of the cells  $N_{\rm s} V_{\rm act}$  one gets the expression of a non linear conductance:

$$Y_{\rm a \, stk} = \frac{AJ_0 \left( \exp \frac{\alpha F V_{\rm act}}{R_0 T} - \exp \frac{-(1-\alpha)F V_{\rm act}}{R_0 T} \right)}{N_{\rm s} V_{\rm act}}$$
(6.5)

which can be directly implemented in EMTP. According to [7], in Eq. (6.5) the value of n is set to unity, referring to the main rate-controlling reaction of OH<sup>-</sup> and H<sup>+</sup> occurring on the active sites of the platinum cathode catalyst. For zero current density through the membrane, Eq. (6.5) converge to the finite value:

$$(Y_{\rm a\,stk})_{J=0} = \frac{AJ_0}{N_s} \frac{F}{R_0 T}$$
(6.6)

The exchange current density  $J_0$  can be calculated as a function of the acti-



Figure 6.4 EMTP implementation of the activation potential block.

vation free energy [15]:

$$J_0 = k \cdot \exp \frac{-\Delta G}{R_0 T} \tag{6.7}$$

where k is a constant depending on the process and on the ion activity. Eq. (6.7) is then linearized about  $T_{\rm ref}$  as:

$$J_0 = J_{0 \text{ ref}} \left( 1 + \frac{\Delta G(T - T_{\text{ref}})}{R_0 T_{\text{ref}}^2} \right) = J_{0 \text{ ref}} \left( 1 + k_{J_0} (T - T_{\text{ref}}) \right)$$
(6.8)

Typical values of  $J_0$  and  $\alpha$  for the oxidation reaction at the cathode of a PEM fuel cell can be found in [16]. Fig. 6.4 shows the EMTP implementation of the activation potential block.

## 6.3.3 Concentration voltage drop

The potential drop produced by the difference between the concentration of chemicals at the electrode surface and in the electrode bulk is called the concentration polarization, and can be written as [19]:

$$V_{\rm c} = -\frac{R_0 T}{nF} \ln \frac{C_{\rm S}}{C_{\rm B}} \tag{6.9}$$



Figure 6.5 EMTP implementation of the concentration potential block.

were  $C_{\rm S}$  is the surface concentration and  $C_{\rm B}$  the bulk concentration. By use of the Fick's first law of diffusion, the above equation can be rewritten as [14, 19]:

$$V_{\rm c} = -\frac{R_0 T}{F} \ln\left(1 - \frac{J}{J_{\rm max}}\right) \tag{6.10}$$

in which the value of n was set to unity, and  $J_{\text{max}}$  is the maximum theoretical current density (which is never reached in reality). In order to keep Eq. (6.10) valid also with negative current, the module of J is used and the sign of  $V_c$  is set in order to make the concentration potential opposing to the current direction:

$$V_{\rm c} = -\operatorname{sign}(J)\frac{R_0T}{F}\ln\left(1 - \frac{|J|}{J_{\rm max}}\right)$$
(6.11)

It has to be noted that, if the model is used for the simulation of a regenerative fuel cell, it could be necessary to use two different values of  $J_{\text{max}}$  depending whether the device is working in direct (fuel cell) or reverse (electrolyzer) mode.

Fig. 6.5 shows the EMTP implementation of the concentration potential block. The measured value of J is limited to a value which is slightly lower than the maximum theoretical value  $J_{\text{max}}$  in order to avoid numerical problems. However, under normal operation, the limiting condition should never arise.



Figure 6.6 EMTP implementation of the membrane proton transfer potential block.

### 6.3.4 Ohmic voltage drop

The ohmic voltage drop in a PEM device is predominantly due to the resistance of the membrane to proton transfer, plus other contributes due to charge-transfer resistance of the hydrogen-oxygen reaction, conducting resistance between the membrane and electrodes, and resistance of electrodes [7, 14]. Because the internal currents only flow through the membrane, these contributes were separated in two components as shown in Fig. 6.2.

An expression of the resistance of Nafion<sup>TM</sup> PEM membrane to proton transfer is here reported from [7]:

$$R_{\rm m} = \frac{r_{\rm m}s_{\rm m}}{A}, \qquad r_{\rm m} = \frac{181.6\left(1 + 0.03|J| + 0.062(T/303)^2|J|^{2.5}\right)}{(\lambda - 0.634 - 3|J|)\exp\left(4.18\left(\frac{T-303}{T}\right)\right)} \tag{6.12}$$

where  $r_{\rm m}$  ( $\Omega$ -cm) is the membrane resistivity,  $s_{\rm m}$  (cm) the membrane thickness, A (cm<sup>2</sup>) the membrane area, while  $\lambda$  is an dimensionless parameter which mainly depends on the membrane characteristics and humidity conditions [7, 9]. The module of the current density was added in Eq. (6.12) in order to keep it valid also when considering negative currents. Fig. 6.6 shows the EMTP implementation of the membrane proton transfer potential block.

According to [7], the resistance to electron conduction on collectors and electrodes can be assumed to be constant over the relatively narrow temperature range of PEM fuel cell operation, and is therefore represented by the constant resistor  $R_{\rm oc}$  in Fig. 6.2.

### 6.3.5 Double-Layer Charging Effect

In a PEM fuel cell, the two electrodes are separated by a solid membrane which only allows the  $H^+$  ions to pass [19]. The electrons will flow from the anode through the external load and gather at the surface of the cathode, to which the protons of hydrogen will be attracted at the same time. Thus, two charged layers of opposite polarity are formed across the boundary between the porous cathode and the membrane. The layers, known as electrochemical double layer, can store electrical energy and behave like a capacitor [13, 14]. Actually, the same occurs at the anode, but its effect can be neglected because the cathode polarization is predominant. Since the electrodes of a PEM fuel cell are porous, the capacitance is very large and can be in the order of several Farads for a single cell [14, 16, 18, 20].

If the load voltage changes, there will be some time for the charge (and the associated current) to vanish (if the voltage increases) or to increase (if the voltage decreases). Such a delay affects the activation and concentration potentials. It is important to point out that the ohmic overpotential is not affected, since it is directly related to the cell current through the Ohm's law. Thus, a change in the current causes an immediate change in the ohmic voltage drop [13]. In this way, it can be considered that a first-order delay exists in the activation and concentration voltages. The time constant associated with this delay is approximately given by the product:

$$\tau_{\rm c} = (R_{\rm act} + R_{\rm conc}) \cdot C \tag{6.13}$$

where  $R_{\text{act}}$  and  $R_{\text{conc}}$  are respectively the average equivalent resistance due to the activation and the concentration potentials. Values of capacity per unit membrane surface are given in [13, 14, 16].

### 6.3.6 Internal Currents and Fuel Crossover Effect

Internal currents and fuel crossover effect could be introduced by a controlled current source or non linear resistance. Since temperature-dependent data are not available in literature, an independent current source was used ( $I_{int}$  in Fig. 6.2) with value proportional to the membrane area:

$$I_{\rm int} = J_{\rm int} \cdot A \tag{6.14}$$

Values of internal current per unit membrane area can be obtained from [13, 16].

## 6.4 Validation of the model

The simulation results of the model were validated by comparison with the experimental data provided in [14], referring to an Avista Labs SR-12 PEM fuel-cell stack (48 cells in series, 500 W nominal power). Fig. 6.7 and 6.8 show the V-I and the P-I static characteristics of the stack as calculated by the model.

The fast transient response of the model due to fast step load current change is shown in Fig. 6.9. It is noted that when the load current steps up, the voltage drops simultaneously to some value due to the ohmic voltage drop (which directly depends on the stack current), and then it decays exponentially due to the capacitance of the double-layer charging effect.

On the other hand, Fig. 6.10 shows the transient response to steps in the load current in a long time scale. Here, the effect of the capacitor ends too fast to be visible. The shown voltage transients are introduced in the model by Eq. (6.4), and reproduce the effect of delays between the change in the load current and the flow of fuel and oxidant, which can be in the range from several tens of seconds to several hundreds of seconds [14].



Figure 6.7 V–I static characteristics of the SR-12 stack calculated by the model.



Figure 6.8 P-I static characteristics of the SR-12 stack calculated by the model.



Figure 6.9 Transient response of the model to steps in the load current in the short time range.



Figure 6.10 Transient response of the model to steps in the load current in the long time range.

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# Chapter

# Average Circuital Model for Power Electronic Devices

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# 7.1 Introduction

In the  $MgB_2/LH_2$  electrical system, power electronics devices are used to adjust the output voltage of generators and electrochemical devices to the voltage of the DC superconducting line (DC/DC), and for the connection of the DC line to the electrical grid (DC/AC). The direct implementation of switching devices in the EMTP code would imply to set the maximum simulation step time well below the millisecond in order to resolve the actual switching frequency (which can be in the order of 1 kHz or more). This would make the electrical simulation much slower than the "real time" working with today available common calculators, making it unsuitable for simulating the system in a long time scale (hours or days). When considering long simulation periods, the interest is more in the average behavior (relations between input and output currents and voltages) and in the average efficiency of the devices in the switching period.

The assumption for the validity of averaging models is that the natural time constants of the converter are much longer than the switching period, that is that the natural frequencies of the converter (corresponding to the poles of the system) are much lower than the switching frequency. In this case, the substitution of the discontinuous system with an averaged continuous time system introduce very small errors at frequencies below one half the switching frequency.

A classic method for deriving the continuous average characteristics of power electronic devices is the "state space averaging" method [1]. The formulation of this technique begins by writing down the state space differential equations that describe the linear circuit during the "on" interval and the "off" interval of the switching period. Then, the matrix of coefficients of the obtained systems are averaged (weighed on the relative durations), in order to find the state space system of equations which describe the average behavior. A drawback of this method is that it involves a considerable amount of matrix manipulations when considering non trivial cases. The complexity of the problem steps up when trying to take into account the non-ideal behavior of the components during commutations, which however can largely affect the efficiency and operation of power electronics devices. Recently, a simpler method was proposed for the analysis of Pulse Width Modulation (PWM) converters. With this method, the nonlinear part of the converter which can be identified as a particular three-terminal device (active switch, passive switch and short circuit) is substituted by its averaged circuital model (called the "PWM switch model") [2–5]. In other words, rather than averaging and linearizing the converter state equations, the averaging and linearization operations are performed directly on the converter circuit.

On the other hand, in this chapter a simple method is developed to obtain the average DC equivalent circuit of a PWM converter which only involves considerations on the average power absorbed, supplied and dissipated by the converter. This method is valid under assumption that variations in the state variables



Figure 7.1 Scheme of a first-quadrant chopper.

(currents through inductors, and voltage across capacitors) during the switching period can be neglected with respect to their average value. As shown below, non-ideal characteristics such as conduction loss, finite time of commutation, and finite storage time of electronic components can be introduced in the model in a very straightforward way. As an example, in the following the identification method is applied to a simple first-quadrant chopper (buck regulator).

# 7.2 DC equivalent circuit of the first-quadrant chopper

Fig. 7.1 shows the scheme of a first-quadrant chopper. In general, here S is meant to be an active component (also compound by more than one element) that can be controlled for current conduction or interruption, and that prevents the flow of a negative  $i_S$  current, while D is meant to be a passive component (also compound by more than one element) which prevents the flow of a negative  $i_D$  current.

In the following, the instantaneous values of voltage and current are denoted with lower case letters v and i, while their average values on the switching period T are denoted with upper case letters V and I. It is assumed the switching frequency to be high enough that the ripple in the voltage across the capacitor Cand in the current through the inductor L in Fig. 7.1 can be neglected, so that their instantaneous values can be approximated to the average values  $V_C$  and  $V_L$ . For the Kirchhoff's laws it is at any time:

$$V_C = v_S + v_D \tag{7.1a}$$

$$I_L = i_S + i_D \tag{7.1b}$$

thus, averaging on the switching period:

$$V_C = V_S + V_D \tag{7.2a}$$

$$I_L = I_S + I_D \tag{7.2b}$$

The average power absorbed and supplied by the chopper are given by:

$$P_{\rm in} = V_C I_S = V_C (I_L - I_D) \tag{7.3a}$$

$$P_{\text{out}} = V_D I_L = (V_C - V_S) I_L \tag{7.3b}$$

The difference gives the average power absorbed by the two components S and D, which represents the average dissipated power in the chopper if the variation in the stored energy on a period is neglected (it is rigorously null in steady state). The expression of the dissipated power can be written in four equivalent forms, depending on which ones of the expressions in Eq. (7.3a) and (7.3b) are used. In particular:

$$P_{\rm diss} = V_C I_S - V_D I_L = V_S I_L - V_C I_D \tag{7.4}$$

which states that the dissipated power increases when  $I_S$  rises and  $V_D$  falls, or similarly when  $V_S$  rises and  $I_D$  falls. Finally, the efficiency can be written as:

$$\eta = \frac{P_{\text{out}}}{P_{\text{in}}} = 1 - \frac{P_{\text{diss}}}{P_{\text{in}}} = \frac{V_D I_L}{V_C I_S}$$
(7.5)

Known an average value of voltage  $V_S$  or  $V_D$ , and an average value of current  $I_S$  or  $I_D$ , it is possible to substitute the discontinuous device in Fig. 7.1 with a continuous equivalent circuit which absorbs, supplies and dissipates the same average powers. Four equivalent circuits can be obtained, corresponding to the four forms in which the dissipated power can be written. In particular, Fig. 7.2 and 7.3 show the two equivalent circuits which correspond to the two forms in Eq. (7.4). In the former, the current source absorbs  $V_C I_S$  and the voltage source supplies  $V_D I_L$ . In the latter, the current source supplies  $V_C I_D$  and the voltage

source absorbs  $V_S I_L$ . Of course, it is also possible to transform each in the other with simple circuital operations. By substituting in the circuit of Fig. 7.2 the voltage source  $V_D$  with the series of two voltage sources according to Eq. (7.2a), and the current source  $I_S$  with the parallel of two current sources according to Eq. (7.2b), the circuit of Fig. 7.4 is obtained. As for the behavior of the circuit at the two ports, this can be reduced into the circuit of Fig. 7.3 noticing that the nodes A and B are always kept at the same potential and that the current source  $I_L$  and the voltage source  $V_C$  as a whole always exchange zero power.

It is important to point out that the identification of the circuital model (whichever is the chosen form) can be done once it is known one average value of voltage ( $V_S$  or  $V_D$ ) and one average value of current ( $I_S$  or  $I_D$ ) with respect to the actual operating condition which is defined by  $V_C$ ,  $I_L$  and the converter duty cycle. This could be done through experimental characterization of an existing device, by measuring for instance the average current and voltage across the D branch or across the S branch.

On the other hand, it is possible to proceed deductively by introducing some hypothesis on the expected characteristics of the two power electronics components. In general, the time trend of the current and voltage across S and D are unknown. However, they are constrained in respecting at any time the Kirchhoff's laws (7.1a) and (7.1b), and by the condition that, neglected the stored energy, the net energies dissipated in a period separately by the components S and D have to be greater than or equal to zero:

$$E_{S \operatorname{diss}} = \int_0^T (v_S i_S) \,\mathrm{d}t \ge 0 \tag{7.6a}$$

$$E_{D \text{ diss}} = \int_0^T (-v_D i_D) \,\mathrm{d}t \ge 0$$
 (7.6b)

The mere introduction for instance of an expected trend in the voltage  $v_S$  and in the current  $i_D$  might be tricky, because could likely lead to a forbidden trend in the couple v-i of one of the two components with respect to conditions (7.6) once the Kirchhoff's laws are applied.

Thanks to Eq. (7.2a) or (7.2b), the deductive process can be simplified by considering only one of the two components S or D, and providing an expected trend in the current through it (or in the voltage across it). When the component is in conduction mode, the corresponding voltage (or current) is found by use of



Figure 7.2 Equivalent circuit of the first-quadrant chopper (form 1).



Figure 7.3 Equivalent circuit of the first-quadrant chopper (form 2).



Figure 7.4 Equivalent circuit of the first-quadrant chopper (intermediate transformation between form 1 and 2).

its electric characteristic. In particular, it is preferable to consider the component D because it is a passive component, and its expected electrical characteristic is easier to introduce.

In the following, it is assumed that when the component D is in conduction state the voltage  $v_D$  is expressible as the sum of a resistive component (given by the static characteristic as a function of  $i_D$ ) and an inductive component (dynamic effect proportional to the time derivative of  $i_D$ ):

$$v_D = v_{D \text{ static}} - L_D \frac{\mathrm{d}i_D}{\mathrm{d}t} \tag{7.7}$$

When the component D is in interruption state, the current through the component S is constant, thus the voltage  $v_D$  is found as  $v_D = V_C - v_{S \text{ static}}$ . Under the same previous hypothesis of neglecting the variation in the stored energy on a period in the component D (rigorously null in steady state), the average value of  $v_D$  on the switching period T is given by:

$$V_D = \frac{1}{T} \int_0^T v_{D \text{ static}} \,\mathrm{d}t - \frac{L_D}{T} \int_0^T \mathrm{d}i_D = V_{D \text{ static}}$$
(7.8)

Hence, because the identification of the averaged equivalent circuit in Fig. 7.2 or 7.3 requires the only knowledge of the average values, the only voltage components given by the static characteristics can be considered. Four reference cases are separately analyzed (ideal components, conduction loss, conduction and commutation loss, reverse recovery time) in order to highlight their effect on the predicted average behavior of the chopper.

#### 7.2.1 Ideal components

With ideal components there is no conduction loss and commutations are instantaneous. The time trend of  $i_D$  and  $v_D$  are shown in Fig. 7.5. During the "on" interval  $(t_{on}) v_D = V_C$ , during the "off" interval  $(t_{off}) v_D = 0$ . Denoting the duty cycle with  $d = t_{on}/T$ , and its complement with  $d' = t_{off}/T$ , it is immediately:

$$I_{D \,\mathrm{id}} = d' \cdot I_L \tag{7.9a}$$

$$V_{D \text{ id}} = d \cdot V_C \tag{7.9b}$$



**Figure 7.5** Time trend of  $i_D$  and  $v_D$  with ideal components.



Figure 7.6 Equivalent circuit of the first-quadrant chopper with ideal components.

and, according to Eq. (7.2a) and (7.2b):

$$I_{S \,\mathrm{id}} = d \cdot I_L \tag{7.9c}$$

$$V_{S \, \text{id}} = d' \cdot V_C \tag{7.9d}$$

whereas Eq. (7.3)-(7.5) reduce to the trivial form:

$$P_{\rm in\,id} = d \cdot V_C I_L = P_{\rm out\,id} \tag{7.10a}$$

$$P_{\rm diss\,id} = 0 \tag{7.10b}$$

$$\eta_{\rm id} = 1 \tag{7.10c}$$

Fig. 7.6 shows the equivalent circuit of the ideal chopper (form 1).



Figure 7.7 Linearized static electrical characteristic of the component D.

### 7.2.2 Conduction loss and instantaneous commutations

Conduction voltage loss are approximated as the sum of a constant component  $V_0$  and a component proportional to the conduction current (with coefficient R). In particular, Fig. 7.7 shows the linearized static electrical characteristic of the component D.

The time trend of  $i_D$  and  $v_D$  are shown in Fig. 7.8, where  $v_{D \text{ on}}$  and  $v_{D \text{ off}}$  are obtained by considering the two states of the chopper:

- a) current  $I_L$  flows through S:  $v_{D \text{ on}} = V_C V_{S0} R_S I_L;$
- b) current  $I_L$  flows through  $D: v_{D \text{ off}} = -V_{D0} R_D I_L$ .

Conduction loss leads to a reduction in the average value  $V_D$  (or equivalently an increase in the average value  $V_S$ ), which translates into power loss according to Eq. (7.4). With simple geometric considerations:

$$I_{D \text{ cond}} = d' \cdot I_L = I_{D \text{ id}} \tag{7.11a}$$

$$V_{D \text{ cond}} = d \cdot v_{D \text{ on}} + d' \cdot v_{D \text{ off}}$$
  
=  $V_{D \text{ id}} - V_{\text{cond}} - R_{\text{cond}} I_L$  (7.11b)

where:

$$V_{\text{cond}} = d \cdot V_{S0} + d' \cdot V_{D0} \tag{7.12a}$$

$$R_{\rm cond} = d \cdot R_S + d' \cdot R_D \tag{7.12b}$$



**Figure 7.8** Time trend of  $i_D$  and  $v_D$  with conduction loss.



Figure 7.9 Equivalent circuit of the first-quadrant chopper with conduction loss.



**Figure 7.10** Equivalent circuit of the first-quadrant chopper with conduction loss (case of  $V_{D0} = V_{S0} = V_0$  and  $R_D = R_S = R$ ).

According to Eq. (7.2a) and (7.2b):

$$I_{S \text{ cond}} = I_{S \text{ id}} \tag{7.13a}$$

$$V_{S \text{ cond}} = V_{S \text{ id}} + V_{\text{cond}} + R_{\text{cond}} I_L$$
(7.13b)

and, by use of Eq. (7.3)-(7.5):

$$P_{\rm in\ cond} = d \cdot V_C I_L = P_{\rm in\ id} \tag{7.14a}$$

$$P_{\rm diss\ cond} = V_{\rm cond} I_L + R_{\rm cond} I_L^2 \tag{7.14b}$$

$$\eta_{\text{cond}} = 1 - \frac{V_{\text{cond}} + R_{\text{cond}} I_L}{d \cdot V_C}$$
(7.14c)

Conduction loss can therefore be introduced in the equivalent circuit by use of a controlled voltage source and an adjustable resistance on the branch with current  $I_L$ , as shown in Fig. 7.9. If the conduction loss in D and S can be assumed as equal, so that  $V_{D0} = V_{S0} = V_0$  and  $R_D = R_S = R$ , the voltage source turns independent ( $V_{\text{cond}} = V_0$ ) and the resistance turns constant ( $R_{\text{cond}} = R$ ), as shown in Fig. 7.10.

# 7.2.3 Conduction loss and commutation loss with current ramps

In addition to conduction loss, commutation loss are here evaluated by considering finite intervals  $\tau_{\rm on}$  and  $\tau_{\rm off}$  needed to switch respectively to the "on" state or to the "off" state of the chopper. During these intervals, the currents in the *S* and *D* branches are assumed to follow linear ramps respectively with slopes  $s_{\rm on} = I_L/\tau_{\rm on}$ and  $s_{\rm off} = I_L/\tau_{\rm off}$  (absolute value). In the following, the relative durations of the current ramps with respect to switching period are denoted with:

$$\delta_{\rm on} = \frac{\tau_{\rm on}}{T} \qquad \qquad \delta_{\rm off} = \frac{\tau_{\rm off}}{T} \tag{7.15}$$

With reference to the static voltage component alone, the time trend of  $v_D$  during the switching period can be estimated as follows:

- a) current  $I_L$  flows through S:  $v_{D \text{ on}} = V_C V_{S0} R_S I_L;$
- b) current  $I_L$  flows through D:  $v_{D \text{ off}} = -V_{D0} R_D I_L$ ;
- c) concurrent conduction of S and D during ramps:  $v_D = -V_{D0} R_D i_D$ .



**Figure 7.11** Time trend of  $i_D$  and  $v_D$  with conduction loss and commutation loss.

The resulting time trends of  $i_D$  and  $v_D$  are shown in Fig. 7.11, where it is assumed  $\tau_{\rm on} \leq t_{\rm on} \leq (T - \tau_{\rm off})$ . Under this condition, with simple geometric considerations:

$$I_{D \text{ cond}+\text{comm}} = \frac{1}{2} (\delta_{\text{on}} + \delta_{\text{off}}) \cdot I_L + (d' - \delta_{\text{off}}) \cdot I_L$$

$$= I_{D \text{ id}} - I_{\text{comm}}$$

$$V_{D \text{ cond}+\text{comm}} = \frac{1}{2} (\delta_{\text{on}} + \delta_{\text{off}}) \cdot (v_{D \text{ off}} - V_{D0})$$

$$+ (d - \delta_{\text{on}}) \cdot v_{D \text{ on}} + (d' - \delta_{\text{off}}) \cdot v_{D \text{ off}}$$

$$= V_{D \text{ id}} - V_{\text{cond}+\text{comm}} - R_{\text{cond}+\text{comm}} I_L$$
(7.16a)
(7.16b)

where:

$$I_{\rm comm} = \frac{1}{2} (\delta_{\rm off} - \delta_{\rm on}) \cdot I_L$$
(7.17a)

$$V_{\text{cond}+\text{comm}} = \delta_{\text{on}} \cdot V_C + (d - \delta_{\text{on}}) \cdot V_{S0} + (d' + \delta_{\text{on}}) \cdot V_{D0}$$
(7.17b)

$$R_{\rm cond+comm} = (d - \delta_{\rm on}) \cdot R_S + \left(d' - \frac{1}{2}(\delta_{\rm off} - \delta_{\rm on})\right) \cdot R_D \tag{7.17c}$$

and, according to Eq. (7.2a) and (7.2b):

$$I_{S \text{ cond}+\text{comm}} = I_{S \text{ id}} + I_{\text{comm}} \tag{7.18a}$$

$$V_{S \text{ cond+comm}} = V_{S \text{ id}} + V_{\text{cond+comm}} + R_{\text{cond+comm}} I_L$$
(7.18b)

When  $\delta_{on} = \delta_{off} = \delta$ , Eq.(7.17a)–(7.17c) simplify into:

$$I_{\rm comm} = 0 \tag{7.19a}$$

 $V_{\text{cond}+\text{comm}} = \delta \cdot V_C + (d-\delta) \cdot V_{S0} + (d'+\delta) \cdot V_{D0}$ (7.19b)

$$R_{\text{cond}+\text{comm}} = (d - \delta) \cdot R_S + d' \cdot R_D \tag{7.19c}$$

The equivalent circuit is analogous to that one in Fig. 7.9.

#### 7.2.4 Effect of a finite reverse recovery time

In some cases, for the estimation of the performance of a chopper it is important to take into account the reverse recovery time of the component D, during which it can conduct reverse current due to the depletion of the charge accumulated in the junction during conduction. The total reverse recovery charge  $Q_D$  is effectively independent of the forward current and can be usually found from manufacturers specifications.

In order to roughly estimate the effect of the recovery time, a simplified approach is here used. The current is assumed to extend the descending ramp below zero with same constant slope  $s_{\rm on}$  until the reverse recovery charge  $Q_D$  is completely depleted, and then abruptly zeroed. Hence, the reverse conduction time  $\tau_{\rm rev}$  is easily found as:

$$\tau_{\rm rev} = \sqrt{\frac{2\,Q_D}{s_{\rm on}}} = \sqrt{2\,Q_D\frac{\tau_{\rm on}}{I_L}} \tag{7.20}$$

and the corresponding parameter of relative duration is introduced:

$$\delta_{\rm rev} = \frac{\tau_{\rm rev}}{T} \tag{7.21}$$

During  $\tau_{rev}$ , the constant value  $v_D = -V_{D0}$  is used for the purpose of calculating the average potential  $V_D$  on the switching period. The resulting time trends of  $i_D$ and  $v_D$  are shown in Fig. 7.12, where it is assumed  $\tau_{on} + \tau_{rev} \leq t_{on} \leq (T - \tau_{off})$ .

Under this condition, with simple geometric considerations:

$$I_{D \text{ cond}+\text{comm}+\text{rev}} = \frac{1}{2} (\delta_{\text{on}} + \delta_{\text{off}}) \cdot I_{L} - \frac{1}{2} \frac{\delta_{\text{rev}}^{2}}{\delta_{\text{on}}} \cdot I_{L} + (d' - \delta_{\text{off}}) \cdot I_{L}$$
(7.22a)  
$$= I_{D \text{ id}} - I_{\text{comm}} - I_{\text{rev}}$$
$$V_{D \text{ cond}+\text{comm}+\text{rev}} = \frac{1}{2} (\delta_{\text{on}} + \delta_{\text{off}}) \cdot (v_{D \text{ off}} - V_{D0}) + \delta_{\text{rev}} \cdot (-V_{D0})$$
$$+ (d - \delta_{\text{on}} - \delta_{\text{rev}}) \cdot v_{D \text{ on}} + (d' - \delta_{\text{off}}) \cdot v_{D \text{ off}}$$
(7.22b)  
$$= V_{D \text{ id}} - V_{\text{cond}+\text{comm}+\text{rev}} - R_{\text{cond}+\text{comm}+\text{rev}} I_{L}$$

where:

$$I_{\rm rev} = \frac{I_L}{2} \frac{\delta_{\rm rev}^2}{\delta_{\rm on}} = \frac{Q_D}{T}$$
(7.23a)

$$I_{\rm comm} = \frac{1}{2} (\delta_{\rm off} - \delta_{\rm on}) \cdot I_L \tag{7.23b}$$

 $V_{\text{cond}+\text{comm}+\text{rev}} = (\delta_{\text{on}} + \delta_{\text{rev}}) \cdot V_C + (d - (\delta_{\text{on}} + \delta_{\text{rev}})) \cdot V_{S0}$ (7.23c)

$$+ (d' + (\delta_{\rm on} + \delta_{\rm rev})) \cdot V_{D0}$$
(7.23c)

$$R_{\rm cond+comm+rev} = \left(d - \left(\delta_{\rm on} + \delta_{\rm rev}\right)\right) \cdot R_S + \left(d' - \frac{1}{2}\left(\delta_{\rm off} - \delta_{\rm on}\right)\right) \cdot R_D \quad (7.23d)$$

and, according to Eq. (7.2a) and (7.2b):

$$I_{S \text{ cond+comm+rev}} = I_{S \text{ id}} + I_{\text{comm}} + I_{\text{rev}}$$
(7.24a)

$$V_{S \text{ cond+comm+rev}} = V_{S \text{ id}} + V_{\text{cond+comm+rev}} + R_{\text{cond+comm+rev}} I_L \qquad (7.24b)$$

When  $\delta_{on} = \delta_{off} = \delta$ , Eq.(7.23a)–(7.23d) simplify into:

$$I_{\rm rev} = \frac{Q_D}{T} \tag{7.25a}$$

$$I_{\rm comm} = 0 \tag{7.25b}$$

$$V_{\text{cond}+\text{comm}+\text{rev}} = (\delta + \delta_{\text{rev}}) \cdot V_C + (d - (\delta + \delta_{\text{rev}})) \cdot V_{S0} + (d' + (\delta + \delta_{\text{rev}})) \cdot V_{D0}$$

$$(7.25c)$$

$$R_{\text{cond}+\text{comm}+\text{rev}} = (d - (\delta + \delta_{\text{rev}})) \cdot R_S + d' \cdot R_D$$
(7.25d)

By substituting the current source with two current sources in parallel, the equivalent circuit can be implemented as shown in Fig. 7.13, where the average reverse



Figure 7.12 Time trend of  $i_D$  and  $v_D$  with conduction loss, commutation loss, and finite reverse recovery time.



Figure 7.13 Equivalent circuit of the first-quadrant chopper with conduction loss, commutation loss, and finite reverse recovery time.

current  $I_{\rm rev}$  through the component D is clearly distinguished.

# 7.3 DC equivalent circuit of the two quadrants chopper

The previous relations can be easily extended to the choppers operating in other quadrants. For instance, the equivalent circuit of a two quadrants chopper can be found by considering it as the parallel of a first-quadrant chopper and a second-quadrant chopper, as shown in Fig. 7.14. When the current  $I_L$  is positive, the chopper works as a first-quadrant chopper through components  $S_1$  and  $D_1$ , with duty cycle  $d_1 = d$ . When the current  $I_L$  becomes negative, the chopper works



Figure 7.14 Scheme of a two quadrants chopper, as the parallel of a firstquadrant chopper  $(S_1 \text{ and } D_1)$  and a second-quadrant chopper  $(S_2 \text{ and } D_2)$ .



Figure 7.15 Equivalent circuit of the two quadrants chopper operating with reverse current  $I_L$  (only conduction loss and same dissipation in the components D and S).

as a second-quadrant chopper through components  $S_2$  and  $D_2$ , with duty cycle  $d_2 = 1 - d = d'$ . By repeating the same previous considerations for a secondquadrant chopper working with duty cycle d', analogous relations can be found. As an example, in the case of only conduction loss and same dissipation in the components D and S, the equivalent circuit of the two quadrants chopper operating with reverse current  $I_L$  is shown in Fig. 7.15. The only difference with respect to the equivalent circuit of the first-quadrant chopper in Fig. 7.10 (considering negative  $I_L$ ), is the reversal of the voltage source  $V_0$  (which has always to be opposed to  $I_L$ ).

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# Chapter 8

# Case Study: DC Link through the Gibraltar's Strait

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# 8.1 Introduction

The design methods and the simulation models introduced in the previous chapters are here applied to the design and simulation of a combined  $MgB_2/LH_2$ 



Figure 8.1 Map of the Gibraltar's strait with the two considered crossing routes (line 1 with minimum depth, line 2 with minimum length).

system with reference to a realistic scenario of application, considering the realization of a large power link for electric energy and  $LH_2$  transport through the Gibraltar's strait [1]. The electric energy is considered to be produced by large scale RES plants sited in North Africa. When RES power exceeds grid demand, the system produces hydrogen by water electrolysis, which can be cooled down to 20.4 K for liquid storage. Part of the stored  $LH_2$  is reconverted into electric energy in periods of RES shortage. The exceeding part can be transported through the combined MgB<sub>2</sub>/LH<sub>2</sub> pipeline.

The choice of the plant location is based on the following considerations:

- limited length of the tract;
- expected increase of energy transport demand in the area, assuring grids stability;
- great RES availability in North Africa.

Moreover, the location of the tract is chosen for expository reasons: the bathymetric profile of the submarine line leads to peculiar technical difficulties, which are later highlighted.

The geographic map of the area is shown in Fig. 8.1. The Gibraltar's strait is characterized by a minimal length crossing route (the Tarifa Narrows, with length of about 14 km and maximum depth of 800 m), and by a minimal depth crossing route (the Camarinal Sill, with length of 27 km and maximum depth of 300 m). Both these crossing routes are considered for the design of the MgB<sub>2</sub>/LH<sub>2</sub> line.
It is shown that the  $MgB_2/LH_2$  technology would allow the realization of the superconducting cable in a single tract.

## 8.2 Design specifications of the $MgB_2/LH_2$ line

The line is here designed with reference to a nominal electrical power of 96 MW, which is transported in the DC cable with 12 kA current and  $\pm 4$  kV voltage. This size is considered with reference to the realization of an initial demonstrative pilot plant. However, both the current in the superconducting cable and the line voltage could be easily increased, allowing to reach multi-GW electric power [2]. In particular, today available XLPE insulated cables used in HVDC plants can work at  $\pm 150$  kV [3], while improvements in order to reach  $\pm 500$  kV were recently reported in literature [4]. This would lead respectively to 3.6 GW and 12 GW rated power for the same 12 kA superconducting cable design.

Among the constructive typologies of superconducting lines, the bipolar configuration with two cryostats and warm dielectric is chosen. This would allow to reach the high voltage range by use of conventional room temperature dielectrics. Moreover, the two cryostats can be separately used as  $LH_2$  going and return lines, so as to free the net  $LH_2$  mass flow rate transported along the line from the minimal  $LH_2$  mass flow rate needed for line cooling (recirculation flow). The maximum transportable  $LH_2$  mass flow rate is set to 0.7 kg/s, which corresponds to about 100 MW of chemical power in terms of HHV. The principle scheme of the MgB<sub>2</sub>/LH<sub>2</sub> plant is represented in Fig. 8.2. The main design specifications are summarized in Tab. 8.1.

The submarine tract of the line involves the problem of cryostat resistance to external water pressure. For this purpose the two cryostats can be inserted in an outer protection pipe, which is at room temperature and can therefore be realized with conventional technologies such as those used for submarine methane pipelines. Besides, it is found that the ferromagnetic material of the protection pipe can effectively shield the induction field generated by the cable current. Fig. 8.3 shows the map of the induction field module near the line: the values are widely below the European limits for DC fields [5], and rapidly drop under the earth natural field value (40–50  $\mu$ T).



Figure 8.2 Scheme of the  $MgB_2/LH_2$  plant.

Table 8.1 Line design specifications for the  $MgB_2/LH_2$  pilot plant.

Parameter	Reference value
Nominal current	$12 \mathrm{kA}$
Nominal voltage	$\pm 4\mathrm{kV}$
Nominal electrical power	$96\mathrm{MW}$
Net transported $LH_2$ mass flow rate	$00.7\mathrm{kg/s}$
Maximum transported $LH_2$ chemical power (HHV)	$100\mathrm{MW}$



Figure 8.3 Induction field module around the transmission line (earth natural field typical value  $40-50\,\mu\mathrm{T}$ ).

.

### 8.3 Electrical and cryogenic design of the line

The evaluation of thermal leakage through the cryostat is carried out as presented in Chapter 2 (reference value of 2 W/m for a cryostat diameter of 12 cm). The electrical design of the 12 kA MgB<sub>2</sub> cable is carried out as presented in Chapter 3 (112 wires, twisted  $7 \times 4 \times 4$  structure, cable diameter of about 20 mm). As it was shown, AC electrical loss in the superconducting cable can be neglected with respect to thermal leakage through the cryostat. The design of the cryogenic line is carried out as presented in Chapter 4, using LH<sub>2</sub> temperature and pressure bounds of 15–25 K and 5–17 bar. The choice of the cryostat diameter is done by imposing the same maximum transportable LH<sub>2</sub> mass flow rate (difference between the going and the return mass flow rate) of 0.7 kg/s for the two considered line routes (Fig. 8.1).

For the line 1 path, a cryostat diameters of 120 mm is chosen. Fig. 8.4, 8.5 and 8.6 show respectively the bathymetric profile, and the calculated temperature and pressure trends for different going mass flow rates. The minimum mass flow rate (given by the maximum temperature constraint) is 0.58 kg/s; the maximum mass flow rate (given by the maximum pressure constraint) is 1.32 kg/s. Thus with this line design the net transported LH<sub>2</sub> mass flow rate can vary between 0 kg/s to 0.74 kg/s.

Similarly, Fig. 8.7, 8.8 and 8.9 show the bathymetric profile of the line 2, and the respective temperature and pressure diagrams. Because of the lower line length, the same transportable  $LH_2$  mass flow rate is obtained with a smaller cryostat diameter of 100 mm. This line design determines a minimum flow of refrigeration of 0.31 kg/s, and a maximum flow capacity of 0.97 kg/s. However, for this crossing route the higher water pressure could lead to serious problems for the resistance of the outer protection pipe (which could be separately done for the two cryostats). Moreover, in this case the point of maximum  $LH_2$  pressure is not located at the pipe income but at the maximum depth point. This could lead to difficulties in measuring and maintaining the pressure higher bound.



Figure 8.4 Bathymetric profile of line 1.



Figure 8.5 LH<sub>2</sub> temperature along line 1 on the going pipe (for different mass flow rates) and return pipe.



Figure 8.6 LH<sub>2</sub> pressure along line 1 on the going pipe (for different mass flow rates) and return pipe.



Figure 8.7 Bathymetric profile of line 2.



Figure 8.8 LH<sub>2</sub> temperature along line 2 on the going pipe (for different mass flow rates) and return pipe.



Figure 8.9 LH<sub>2</sub> pressure along line 2 on the going pipe (for different mass flow rates) and return pipe.

### 8.4 Electrical model of the system

The electrical simulation of the system is carried out by implementing in EMTP the models proposed for electrochemical devices in Chapter 6 and for power electronics devices in Chapter 7. The use of the advanced energy recovery systems presented in Chapter 5 for the vaporization of  $LH_2$  is not considered. It has to be pointed out that the choice of considering a fuel-cell system for the reconversion of hydrogen into electric energy was mainly dictated by research interest reasons and because of good characteristic of high efficiency and rapid response to power variations. The electric conversion could be also carried out with a conventional thermal-electric systems. However, in this case it might be necessary to compensate for the slow transient response by use of an adding battery pack or, again, an auxiliary fuel cell system. The developed simulation model could therefore be useful also for the study of the system when considering a substantially steady part of the electric power coming from thermal-electric systems and the fast power transient response supplied by an auxiliary fuel-cell system.

Fig. 8.10 shows the full electrical model of the system implemented in EMTP. The DC side of the current source inverter is represented by the controlled voltage source  $V_{\rm inv DC}$ . The inverter can control the ratio between active and reactive power fed to the AC grid by changing the firing angle. Doing this it modifies its average voltage on the DC side. For this reason, it is considered that  $V_{\rm inv DC}$  can be arbitrarily varied. The limits are set in principle between 6 kV to 10 kV. Known the actual value of  $V_{\rm inv DC}$ , the system is controlled in order to always supply the reference power  $P_{\rm ref}$  to the inverter, depending on the active power required by the grid. Thus, the reference value of the DC current  $I_{\rm DC ref}$  is calculated as:

$$I_{\rm DC ref} = \min\left(\frac{P_{\rm ref}}{V_{\rm inv \, DC}} , \ 12 \,\rm kA\right) \tag{8.1}$$

Clearly, because the maximum cable current is limited to 12 kA, the nominal plant power of 96 MW can be supplied to the grid only when  $V_{\text{inv DC}}$  is greater than or equal to 8 kV. Depending on the active power required by the grid, this sets a limit on the maximum reactive power which the inverter can supply.

The hydrogen block includes the electrolyzer, fuel-cell and liquefying systems, and is requested to regulate the line DC current in order to follow the reference value  $I_{\rm DC ref}$  regardless of the actual RES power availability. The 0.5 H inductor

in Fig. 8.10 acts as a smoothing reactor to reduce the line current ripple due to power electronics switching, and helps the control system by slowing down current variations. The smoothing reactor, which is always found also in conventional HVDC systems [6], is here assumed to be manufactured with superconducting windings (SMES), but could also by made with conventional copper windings. The resistor R in Fig. 8.10 introduces the electrical resistance due to the two current leads at the cable ends. This value can be easily estimated on base of relations given in Chapter 3 for a copper lead with optimum size ratio. The distributed representation of the superconducting line was not inserted in the model, because the propagation delay time was estimated to be in the order of 0.1 ms. The line inductance can be considered as lumped together with the smooth reactor. In the following, the EMTP implementations of the RES and H<sub>2</sub> sub-blocks are described.

#### 8.4.1 **RES** power generation block

The overall behavior of RES generators is simply implemented as a controlled voltage source which always delivers an arbitrarily value of power, depending on the actual RES power availability. This is for instance the case of a PhotoVoltaic (PV) element with Maximum Power Point Tracker (MPPT): for each actual value of the incoming radiant solar power the tracker will accommodate the PV voltage to the DC bus voltage in order to supply the converted electrical power. The control signal  $P_{\text{RES}}$  of the EMTP RES block has to be intended as the actual value of generated electrical power, which is the product of the incoming gross RES power and the conversion efficiency. Fig. 8.11 shows the EMTP implementation of the RES block. The generator voltage is calculated as the ratio of the arbitrarily variable control signal  $P_{\rm RES}$  and the flowing current, and then limited below  $12\,{\rm kV}$ (which is assumed as the maximum output voltage of the RES generators). The  $V_{\rm loss}$  term represents the loss of the static converter. Because here the duty cycle of the converter is unknown,  $V_{\rm loss}$  is implemented as an independent voltage source and a constant resistance (which is the case of same voltage loss in the active and passive switch, as seen in Chapter 7). The little 1 mH reactor was added in order to avoid EMTP numerical instabilities by smoothing fast current oscillations.



Figure 8.10 EMTP electrical model of the system.



Figure 8.11 EMTP implementation of the RES generators block.

#### 8.4.2 LH<sub>2</sub> energy storage and power conditioning system

Fig. 8.12 shows the EMTP implementation of the  $LH_2$  energy storage and power conditioning system block. The fuel-cell and the electrolyzer blocks are implemented through the dynamic circuital model presented in Chapter 6. Here, the fuel-cell system is sized in order to deliver the full nominal power of the plant in case of no RES power availability, and the electrolyzer system is sized in order to absorb the maximum power supplied by RES generators in case of no power demand by the grid. Depending on the actual RES power availability with respect to the active power required by the grid, the fuel-cell or the electrolyzer is activated. When there is no need for power compensation, both the power systems can be put in stand-by mode in order to avoid parasitic loss. The power conditioning is carried out by a two quadrants chopper (DC/DC regulator), but could also be done with two separated single quadrant choppers. The chopper is implemented in EMTP through the averaged equivalent circuit presented in Chapter 7, as shown in Fig. 8.13. The control system varies the duty cycle of the chopper on the basis of the measured error in the line DC current through a Proportional-Integral-Derivative (PID) regulator. For simplicity, power electronics loss are implemented as an independent voltage source (always opposite to the



Figure 8.12 EMTP implementation of the  $LH_2$  system block.



Figure 8.13 EMTP implementation of the averaged chopper equivalent circuit block.



Figure 8.14 EMTP implementation of the cryogenic system block.

current direction) and a constant resistance. Finally, the power needed for line cooling and hydrogen liquefaction is here estimated as a fixed 3 MW quote (with cryocooler efficiency of 2.9% and pumping efficiency of 70% [2]) plus 36 MJ for each kg of liquefied H<sub>2</sub>. This power is absorbed by the cryogenic block (Fig. 8.10) through a controlled current source, as shown in Fig. 8.14.

## 8.5 Simulation results

The developed electrical model of the system is complete enough to be used for investigating both the slow trend of the system in the long time range and the dynamic evolution in response to fast variations in the available RES power or grid demands. In the following, first the start-up of the system and its response to slow variations in the available RES power and in the required grid power is analyzed, and then a simulation using realistic data of fast fluctuating RES power for the case of a wind farm is presented.

#### 8.5.1 Start-up of the system

From the initial stand-by state with zero RES power and zero current in the DC line, the available RES power is supposed to rise and follow the arbitrary time trend shown in Fig. 8.15. At time t = 6 s, the reference value of the DC power which has to be delivered to the inverter is raised from zero to the nominal value of 96 MW following a ramp of 10 s length. During this simulation, the DC voltage of the inverter is kept constant at 8 kV, hence assuming it works with constant firing angle and constant grid voltage.

Fig. 8.16 shows the time trend of the actual DC power delivered to the inverter, which closely follows the control signal. The power supplied by the  $LH_2$  energy storage system is shown in Fig. 8.17. At first, all the available RES power is absorbed by the electrolyzer and by the cryogenic system for the production of LH<sub>2</sub>. When the power required by the grid is raised, the control system acts in order to make the DC current follow the reference value  $I_{\rm ref}$  as evaluated by Eq. (8.1). Hence, first the power absorbed by the electrolyzer is lowered. Then, when the sum of the DC power required by the inverter and absorbed by the inductor (with rising current) and by all other devices overcomes the available RES power, the fuel-cell system is activated. Finally, the working is reversed again when the available RES power becomes in excess. Fig. 8.18 shows the DC voltage respectively at the  $RES/LH_2$  side and at the inverter side. The constant voltage difference leads to the constant slope current ramp through the inductor. The little spike in the power delivered by the fuel-cell system at t = 16 s is due to the vanishing of the power absorbed by the inductor when the DC current time trend changes from the raising ramp to steady state. Fig. 8.19 shows the level of stored  $LH_2$ , assuming initial value of 100 kg. The initial slightly falling trend is due to the required cooling/pumping power and parasitic loss which has to be provided by the fuel-cell system when there is no RES power availability.



Figure 8.15 RES power availability during the start-up of the system.



Figure 8.16 DC power delivered to the inverter.



Figure 8.17 Power supplied by the  $LH_2$  energy storage system.



 $\label{eq:Figure 8.18} {\rm \ DC} \ {\rm voltage} \ {\rm at \ the \ RES/LH_2} \ {\rm side} \ {\rm and} \ {\rm at \ the \ inverter \ side} \ {\rm of \ the \ line}.$ 



**Figure 8.19** Level of stored  $LH_2$ .

#### 8.5.2 Realistic fast fluctuating wind power availability

Realistic data of fast fluctuating RES power availability for the case of a wind farm are here considered, as shown in Fig. 8.20. Both the DC voltage of the inverter and the DC power which has to be supplied to the inverter are arbitrarily varied through linear ramps during the simulation. Fig. 8.21 shows the assumed time trend of the inverter DC voltage. Fig. 8.22 and 8.23 show respectively the actual DC power delivered to the inverter and the current through the line, which closely follow the reference values. Fig. 8.24 shows the time trend of the duty cycle control signal of the chopper during the simulation. Fig. 8.25 shows a magnification of the current diagram in a restricted time scale. The predicted error in the line DC current is kept below 1 A, thanks to the fast response of the hydrogen system. The current ripple due to variations in the RES power availability is certainly assimilable with that produced by power electronics switching, and do not pose any problem for the reliable operation of the superconducting cable. The exceeding available RES power is absorbed by the  $LH_2$  storage system, as shown in Fig. 8.26. Finally, Fig. 8.27 shows the level of stored  $LH_2$ . At the end of the simulation, the hydrogen system produced about  $21.5 \,\mathrm{kg}$  of  $\mathrm{LH}_2$  and absorbed 1242 kWh total energy, with an overall efficiency of 68.2% with respect to the hydrogen HHV.



Figure 8.20 Realistic RES power time trend for the case of wind farm.



 $Figure \ 8.21 \quad {\rm Assumed \ time \ trend \ of \ the \ DC \ voltage \ of \ the \ inverter.}$ 



Figure 8.22 DC power delivered to the inverter (on this diagram scale, it closely follows the reference value).



Figure 8.23 DC current through the line (on this diagram scale, it closely follows the reference value).



Figure 8.24 Time trend of the duty cycle control signal of the chopper.



 $\label{eq:Figure 8.25} {\rm \ DC\ current\ through\ the\ line\ in\ comparison\ with\ the\ reference\ value.}$ 



Figure 8.26 Power supplied by the  $LH_2$  energy storage system.



 $\label{eq:Figure 8.27} {\bf Evel of stored } LH_2.$ 

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# Conclusions

The goal of this thesis was the in-depth study of the main aspects involved in the mechanical and electrical design of a combined  $MgB_2/LH_2$  system for concurrent transport of electric power and liquid hydrogen produced by large scale RES power plants, and the development of a complete electrical model of the system aimed at evaluating the working characteristics and the performance of the system with reference to realistic scenarios of application. The thesis was organized into three parts.

Part I treats the mechanical and electrical design of the  $MgB_2/LH_2$  combined line. In Chapter 2 a numerical model for the estimation of the heat leakage through a multi-layer insulated cable cryostat was developed. The model separately takes into account the three main heat transfer contributions due to radiation, solid conduction and residual gas conduction, and can be suitably used to evaluate the dependence of the MLI performance on the actual geometry configuration and the characteristics of the insulating layers. In Chapter 3 the section of the MgB<sub>2</sub> DC superconducting cable was designed. The AC loss due to the typical current ripple produced by power electronics converters was evaluated, and found to be negligible with respect to thermal leakage through the cryostat. In Chapter 4 a method for the design of the LH<sub>2</sub> flow cross section of the combined line was developed. A simplified set of compressible fluid stationary flow equations was derived, which is valid when the flow speed is far from the sound speed in the fluid. These flow equations are explicit in the temperature and pressure gradients, and allow to easily draw diagrams which can be suitably used in general for the design of the refrigeration circuits of superconducting cables and cryogenic pipelines. The results show the feasibility of reaching multi-km distance between pumping/cooling stations for  $MgB_2/LH_2$  superconducting lines.

Part II (Chapter 5) deals with the energy aspects involved in hydrogen liquefaction and vaporization. After a review of the actual systems today used in the liquid hydrogen and liquid natural gas markets, advanced vaporization systems aimed at recovering part of the energy spent for liquefaction were introduced and evaluated. It was shown that the use of recovery systems can lead to a significant increase in the amount of the extracted energy per kg of  $LH_2$ , thus improving the overall energy storage and conversion chain efficiency.

Part III treats the electrical modeling and simulation of the system under realistic working conditions. In Chapter 6 a general circuital model for electrochemical PEM devices (fuel cell, electrolyzer or regenerative fuel cell) was developed. The model, whose simulation results were validated through comparison with experimental data available in literature, allows to take into account both electric and working delay dynamic effects, parasitic loss due to internal currents and fuel crossover, and to accurately describe the stack voltage characteristic also in the low current density zone. In Chapter 7 a method was developed to obtain the average DC equivalent circuit of a PWM converter, which is valid when the switching frequency is sufficiently high to make negligible variations in the state variables (currents through inductors, and voltage across capacitors). The model allows to easily take into account non-ideal behaviors such as conduction loss, finite commutation time, and finite storage time of electronic components. Finally, in Chapter 8 the design methods and the simulation models introduced in the former chapters were used for the complete design and simulation of the  $MgB_2/LH_2$  combined system with reference to a realistic scenario of application. The chosen case of a plant crossing the Gibraltar's strait for the connection of large scale RES power plants sited in North Africa to the European grid allowed to point out peculiar technological difficulties which arise in such a severe environmental background. However, two possible crossing routes for the  $MgB_2/LH_2$ combined line were identified and compared. The electrical system simulation was carried out considering a start-up of the system and a typical working condition using realistic data of fluctuating wind power availability. The implemented control strategy of the hydrogen energy storage system showed to allow very precise power regulation and system stability.

In conclusion, the design methods and simulation models introduced in this thesis allowed to demonstrate the feasibility of the  $MgB_2/LH_2$  combined system for large scale RES power transport and regulation. On a more general point of view, they represent significant contributions in the field of superconducting and cryogenic pipeline design, cryogenic fuels processing, as well as power electronics and electrochemical devices modeling.

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