

Transient study of a variable-speed Pump-as-Turbine storage unit to characterise its ability to provide ancillary services

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for obtaining the Master's degree in
Electro-mechanical Engineering, Professional Focus in Energy

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Academic year 2016-2017

Abstract

The evolution of the electricity generation landscape towards distributed and intermittent generation leads to new challenges for the grid management. Consequently, hydraulic pumped-storage power plants are most likely to be an important stake for the future. These units indeed present high energy storage and load-balancing capabilities. The development of power electronics allows to build control strategies for variable-speed hydro-power stations and enables them to compensate for fast power fluctuations in the grid. Nevertheless, these installations are usually expensive and on-site custom manufactured. Previous studies have shown that the cheap and readily available pumps can also work in turbine mode (*Pump-as-Turbines*).

In the present master thesis, a model of a variable-speed PaT storage unit is suggested in order to study its ability to provide ancillary services to the grid operator. We start by establishing a review of primary frequency and voltage control reserves. Then, the complete installation model is developed as a general case, as well as its control strategy. Finally, the model is applied to the existing hydro-power plant of *Negundo* and tested regarding ancillary services provision and fault ride-through capacity.

The results show that both primary frequency and voltage controls work in pump and turbine operations. Two different control strategies however needed to be implemented, one for each operating mode. Indeed, a direct power control via the electromagnetic torque, as achieved when pumping, is unstable in a part of the turbinning working range. This instability is identified and demonstrated. The unstable behaviour is overcome by controlling the power via a speed regulator. Last of all, the simulation demonstrates the robustness of the plant to three-phase voltage dips in case of the application of a specific control strategy.

Keywords : **PaT, variable-speed, control strategies, hydro-power, ancillary services**

Acknowledgements

First of all, we would like to acknowledge our promoter, Professor Emmanuel De Jaeger. We particularly appreciated working under his supervision due to his trust, patience and flexibility. Throughout the year, he was always encouraging and giving accurate advice whenever needed. Emmanuel De Jaeger entered our lives at the beginning of the master programme, giving us high quality classes about electrical power management, incrementally building the foundations of the present document and nourishing our interest for the specific field.

Our main gratitude goes to Thomas, whose availability and dedication to its students even lead him to the Nordic countries. From his desk in Louvain-la-Neuve or behind his computer anywhere in the world, he always provided us with valuable guidance. Our numerous discussions have been decisive in the realisation of the present master thesis. On top of his academic skills, Thomas likes to create an informal working environment favouring interactions, motivation and creativity. More than a supervising staff, he has become a reliable friend to both of us.

We would also like to thank Professor Bruno Dehez for his availability and the precious discussion concerning the field-oriented control of induction machines. However demanding, his classes about electro-mechanical converters provided us with the required technical background for the purpose of this study.

Within the scope of the *SmartWater* project, Alessandro Morabito gave us useful information about the *Negundo* experimental site. We would like to acknowledge him, as well as all the actors of the project, who indirectly took part in this work.

Last but certainly not least, we should like to extend heartfelt thanks to our families. This work indeed represents the culmination of a long educational process, to which they provided the basis and much more. We especially desire to acknowledge our parents for financing the process, their availability throughout our lives and their listening qualities. Nothing would have been possible without them.

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Nomenclature

Abbreviations

BEP	Best Efficiency Point
DAE	Differential Algebraic Equation
DG	Decentralised Generation
DSO	Distribution System Operator
EC	European Commision
ENTSO-E	European Network of Transmission System Operators for Electricity
FCR	Frequency Containment Reserves (R1)
FOC	Field-Oriented Control
FRT	Fault Ride-Through
IEC	International Electrotechnical Commission
IGBT	Insulated-Gate Bipolar Transistor
IM	Induction Machine
LV,MV,HV	Low-, Medium- and High-Voltage
ODE	Ordinary Differential Equation
PaT	Pump-as-Turbine (a pump also used in reversed mode)
PCC	Point of Common Coupling
PDE	Partial Differential Equation
PHES	Pumped-Hydroelectric Energy Storage
PLL	Phase Locked Loop
PoC	Point of Connection
PPM	Power Park Module
pu	Per Unit
PWM	Pulse Width Modulation
RES	Renewable Energy Sources
RMS	Root Mean Square

RPR	Reactive Power Reserve
rps,rpm	Rotations Per Second, Per Minute
THD	Total Harmonic Distortion
TSO	Transmission System Operator
VSI	Voltage Source Inverter

Electrical quantities

C	DC-link capacitance	$[F]$
f	Grid frequency	$[Hz]$
L_f	L-filter inductance	$[H]$
P_n	Nominal active power	$[W]$
R_g, L_g, V_g	Grid equivalent resistance, inductance and voltage	$[\Omega], [H], [V]$
S_{sc}	Short-circuit apparent power	$[VA]$
U_n	Phase-to-phase nominal RMS voltage	$[V]$
V_n	Phase-to-ground nominal RMS voltage	$[V]$
V_{dc}	DC-link voltage	$[V]$

The Hydraulic Model

ρ	Water density	$1000 [kg/m^3]$
A	Penstock pipe section area	$[m^2]$
a	Pressure wave velocity	$[m/s]$
D	Penstock pipe diameter	$[m]$
D_t	PaT diameter	$[m]$
E	Penstock pipe young modulus	$[Pa]$
e	Penstock pipe thickness	$[m]$
f	Penstock pipe friction factor	$[-]$
g	Gravity acceleration	$9.81 [m/s^2]$
H	Head pressure	$[m]$
H_{res}	Constant static head pressure	$[m]$
K	Water bulk modulus	$2.19 [GPa]$
L	Penstock pipe length	$[m]$
n	Rotational speed of the pump shaft	$[rps]$
Q	Water flow rate	$[m^3/s]$
T_m	Hydro-mechanical torque applied to the shaft	$[Nm]$

U	Axial speed of the water in the penstock pipe	$[m/s]$
P	Hydro-mechanical power applied to the shaft	$[W]$

The Induction Machine

ϕ_{rd}, ϕ_{rq}	Rotor d- and q-axis fluxes	$[Wb]$
ϕ_{sd}, ϕ_{sq}	Rotor d- and q-axis fluxes	$[Wb]$
θ_m, ω_m	Mechanical angle and speed of the rotor shaft	$[rad], [rad/s]$
θ_{em}, ω_{em}	Electrical angle and speed between the stator and rotor α - β frames	$[rad], [rad/s]$
$\xi_r, \dot{\xi}_r$	Electrical angle and speed between the rotor <i>Park</i> and α - β frames	$[rad], [rad/s]$
$\xi_s, \dot{\xi}_s$	Electrical angle and speed between the stator <i>Park</i> and α - β frames	$[rad], [rad/s]$
F	Friction factor of the rotor shaft	$[kg \cdot m^3/rad]$
H	Combined rotor and load inertia constant	$[rad \cdot s/m^4]$
i_{ra}, i_{rb}, i_{rc}	Rotor phase currents	$[A]$
i_{sa}, i_{sb}, i_{sc}	Stator phase currents	$[A]$
J	Combined rotor and load inertia coefficient	$[kg/m^2]$
L_m	Magnetising inductance	$[H]$
p	Number of pole pairs	$[-]$
R_r, L_{lr}, L_{cr}	Rotor resistance, leakage and cyclic inductances	$[\Omega], [H], [H]$
R_s, L_{ls}, L_{cs}	Stator resistance, leakage and cyclic inductances	$[\Omega], [H], [H]$
T_m	Mechanical torque	$[Nm]$
T_{em}	Electromagnetic torque	$[Nm]$
u_{r0}, i_{r0}	0-axis rotor voltage and current	$[V], [A]$
u_{ra}, u_{rb}, u_{rc}	Rotor phase voltages	$[V]$
u_{rd}, i_{rd}	d-axis rotor voltage and current	$[V], [A]$
u_{rq}, i_{rq}	q-axis rotor voltage and current	$[V], [A]$
u_{s0}, i_{s0}	0-axis stator voltage and current	$[V], [A]$
u_{sa}, u_{sb}, u_{sc}	Stator phase voltages	$[V]$
u_{sd}, i_{sd}	d-axis stator voltage and current	$[V], [A]$
u_{sq}, i_{sq}	q-axis stator voltage and current	$[V], [A]$

Other Symbols

j	Unit imaginary number
s	Laplace variable

Introduction

The world is at a turning point. The energy transition toward low carbon footprint technologies is a crucial step in the fight against climate change and toward global sustainability. In its *Horizon 20-20-20* energy-climate plan, the *European Commission* (EC) sets the ambitious objective of reaching 20% of renewable energies in the total European energy consumption by 2020. The reaching of such targets requires the still increasing penetration of decentralised renewable energy sources (RES). Most of these sources being intermittent generation, the whole electricity market must be able to adapt to these changes. In order to sustain the transition toward such decentralised generation (DG), the grid operators must provide a flexible structure allowing to integrate the diversity of new electrical production units and anticipate the development of future new trends.

Local electrical storage represents a viable alternative to the load-balancing and voltage control issues provoked by the replacement of the large central production units. It is indeed well suited to deal with continuous, small up and down fluctuations caused by intermittency and forecasting errors. Local storage constitutes a special kind of flexible DG units able to contract ancillary services with the grid operators and thus provide reserves for ensuring the system stability and security of supply.

Among other technologies, *Pumped-Hydroelectric Energy Storage* (PHES) shows a great potential for the participation in the stabilisation of the electrical system. As a matter of fact, it is a very mature technology presenting valuable technical characteristics, such as a high efficiency and a short response time of only a few seconds. PHES systems are robust, 100% removable and do not produce any greenhouse gas emissions. These considerations make it the most ecological of all storage strategies. The implementation of such installations however represents some challenges due to their dependence on the topographical conditions and to the difficulty of forecasting their profitability. As part of the *Energinsere* recruitment programme [1], the *SmartWater* project [2] analyses the possibility of allocating ancient quarries or underground cavities to fit PHES storages in Wallonie. These locations indeed represent naturally adapted sites requiring minimum installation costs. Unlike most current PHES plants being meant for high power productions at the transmission grid level, this study opens the way to the provision of regulation services to the distribution grid operators by numerous smaller sites.

Even though PHES units usually provide their turbines with guide vanes allowing the mechanical adaptability of the developed power, the emergence of highly efficient power electronics further expands the flexibility of such installations. Indeed, the adding of variable-frequency drives between the electrical machine and the grid allows to fully control the active and reactive power outputs of the unit, within their operating ranges and even without any guide vane. The presence of the electrical converter induces a very high reactivity of the power control, making this kind of PHES appropriate for the automatic droop control required to provide primary reserves. In this case, the main challenge is the proper implementation of the control, depending on the characterisation of the machine used and its corresponding transient behaviour.

The practical implementation of a pumped-hydroelectric storage leaves many possibilities regarding the choice of the hydraulic machine and the electrical appliance. Most classical PHES units make use of two separate organs : a pump coupled to an electrical motor for the conversion of electricity into potential energy, and a turbine coupled to a generator for the reverse conversion. These systems present very high fixed costs due to the duplication of the organs for the pumping and the turbinning. Turbines are especially expensive as they are custom manufactured. In order to reduce these costs, recent studies consider the use of a single pump for achieving both actions, knowingly pumping and turbinning. The name *Pump-as-Turbine* (PaT) is used in this case. Even though being sized for relatively low power ranges of maximum 500kW, pumps show some important advantages. They are indeed readily available on the market, cheap, robust and require low maintenance. The maximum power limit can be covered by coordinating several PaTs together. The main challenge related to the use of PaTs is the establishment of a complete model allowing their control, as manufacturers do not test the pumps in reverse mode. With regards to the *SmartWater* project, a practical implementation of a small test PHES using a PaT coupled to a squirrel-cage induction machine (IM) is positioned in Froyennes, near Tournai. It is called *Negundo*.

This master thesis details the transient study of a variable-speed PaT storage unit to characterise its ability to provide ancillary services. The whole analysis is centred around a *Matlab/Simulink* model validating the present thesis. Chapter 1 corresponds to the review of the ancillary services provision regulations applicable to our study case, knowingly the primary frequency and voltage controls. The general requirements for grid connection of DG units are also recalled in this chapter, setting the framework for the testing conditions of our installation. Then, the actual analysis of the installation starts in Chapter 2 with the thorough description of the model. In this chapter, we precisely characterise the underlying physics necessary for the establishment of the *Matlab/Simulink* model and the control of the converter. Afterwards, Chapter 3 defines the control strategy used to regulate the active and reactive power by the consequent switching of the variable-frequency drive. The main controller design being meant for the normal operating conditions of the installation, the adaptation of the governance algorithm in case of fault conditions is also developed in this chapter. Last but not least, the *Matlab/Simulink* implementation is used to simulate the behaviour of a practical study case in Chapter 4. In this chapter, the *Negundo* experimental site is tested to verify the possibility of providing frequency containment reserves (FCR), as well as reactive power reserves (RPR). The fault ride-through (FRT) algorithm is also subject to validation assessments in this final chapter.

Chapter 1

Ancillary services provision

The liberalisation of the electricity market and the introduction of new renewable technologies has redefined the electricity supply industry. The amount of decentralised generation (DG) has become noticeable and it is expected to keep on rising in order to meet the expectations of the European Union's *Horizon 2020* program. In order to guarantee the security of power supply with high quality standards in their systems, grid operators introduced the concept of ancillary services. These services are based on the production and consumption flexibility of generators and loads.

Due to the interconnected nature of the European electricity grid, the *European Network of Transmission System Operators for Electricity* (ENTSO-E) was established in 2009 [3] to overlook the *Internal Energy Market* [4]. Even though there does not yet exist a complete standardised categorisation of ancillary services within European countries, ENTSO-E provides a framework for the main electricity balancing constraints. This structure is the starting point from which each *Transmission System Operator* (TSO) develops its own grid code.

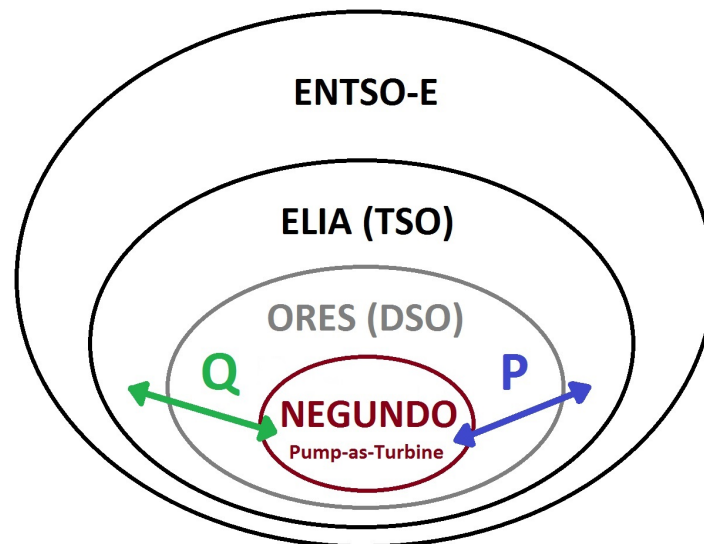


Figure 1.1: The *Negundo* project within the spheres of influence of the market operators.

In Belgium, the transmission grid is a legal monopoly owned by *Elia* [5]. The Belgian TSO defines in [6] a series of ancillary services for the power grid. These mainly concern frequency and voltage control. As mentioned in [7], they generally apply to large industrial clients directly connected to the transmission grid.

Our practical case of the *Negundo* installation in Froyennes, municipality of Tournai, implements a PHES connected to the low-voltage (LV) network. In Tournai, the *distribution grid operator* (DSO) is *ORES*. At present, DSOs do not provide any kind of ancillary services structure. We then position our analysis in the theoretical case of a LV production unit contracting ancillary services directly with the TSO and feeding the contracted powers P and Q through the distribution network. This situation is represented in Figure 1.1, showing the different operators of the system. In the future, the flexibility requirements imposed on the DG units is very likely to bring the need of such contracts, as well as direct contracts with the DSOs to stabilise the distribution grid. Different future scenarios for TSO-DSO coordination on ancillary services are indeed considered in the *SmartNet* project, funded by the *European Union's Horizon 2020 research and innovation programme* [8].

In this chapter, we detail the pertinent TSO requirements which our installation should respect in order to be able to provide primary frequency and primary voltage control. We indeed confine our study to these two precise ancillary services as both of them can be automatically integrated into the control of the converter. The provision of other services would require further projections into the communication with the TSO, which is out of the scope of the present study. We conclude this chapter with the analysis of the extra requirements applicable to all grid-connected generators.

1.1 Primary frequency control

Nowadays, most of the electricity generation is done via rotating machines synchronised and directly connected to the grid. The indicator of the balance between the active power generation and the active power consumption is thus the grid frequency. In order to ensure a sufficient electricity quality, the grid operators need to constantly balance the power consumption and generation. This justifies the need for frequency control.

Frequency control in Belgium is split into three categories : primary, secondary and tertiary. Each of them consist in the adaptation of the active power output of some power-generating module according to different time scales, respectively short-, mid- and long-term, and using their relative reserves. The primary reserves are also known as *frequency containment reserves* (FCR), as their aim is to maintain a stable system synchronous frequency. The primary reserves are used to react instantly and automatically to any change in the grid frequency. The secondary and tertiary reserves are used to adapt the generators set-points according to the perturbation that created the frequency deviation. They allow the restoration of the primary reserves.

In [9], *Elia* describes the main technical and design characteristics for primary control (R1) of the frequency. In this section, we sum up *Elia*'s requirements for providing R1. We complement this information with some considerations from the *SmartNet* report [7].

1.1.1 Droop control

The provision of FCR requires that the module keeps aside a certain amount of active power ΔP_{CB} , called the capacity band and usually expressed in *MW*, to be activated in case of a frequency deviation. Each FCR providing unit must implement a droop proportional control. It means that, once a deviation is detected, it must automatically react linearly and proportionally. The typical active power output function is the following.

$$P = P_0 + \alpha_f \Delta f$$

P_0 is the scheduled power in normal operating conditions. In the case of an electric storage plant like *Negundo*, this power is non-zero in case price-arbitrage is superimposed on FCR. α_f is the droop factor. It depends on the capacity band P_{CB} and the maximum frequency deviation before full activation Δf_{max} :

$$\alpha_f = \frac{\Delta P_{CB}}{\Delta f_{max}}$$

In its operational handbook, ENTSO-E sets forth the following global reaction R1-200mHz : symmetrical and linear, with a total activation at a frequency deviation of $\pm 200\text{mHz}$. If the deviation exceeds $\pm 200\text{mHz}$ in amplitude, the primary control generator must remain fully activated, independently of the deviation.

Elia has also developed three specific R1 service types in addition to the one of defined by the ENTSO-E : R1-up, R1-down and R1-100mHz. This allows more specific actors to contribute to the market. R1-RTE is also defined. It corresponds to the use of *RTE*, the French TSO as additional FCR provider. We do not develop these extra services, as our study case is perfectly suited to provide a symmetrical response.

In the case of the symmetrical products R1-100mHz and R1-200mHz, a deadband of $\pm 10\text{mHz}$ around 50Hz is authorised, to avoid overreactions to negligible deviations.

1.1.2 Time considerations

Primary frequency control being the first defence against frequency variation, it is crucial that the activation of FCR be as soon as possible after the variation detection. With this in mind, the TSO requires that at least 50% of the FCR capacity relative to the sensed deviation be delivered after 15 seconds, and 100% after at most 30 seconds.

Between 15 and 30 seconds, the FCR capacity is expected to increase at least linearly.

Once the desired capacity is reached, the FCR module must provide it as long as the frequency difference remains, and for a minimum time of 15 consecutive minutes.

1.1.3 Additional considerations

In order to complete this section, let us highlight some additional considerations concerning FCR.

- The TSO remunerates according to the pre-determined capacity band, which must remain available round the clock.
- Any unit desiring to provide FCR must have access to reliable local frequency measurement.
- *Elia* recently added some specific rules applicable to *assets with limited energy reservoirs*, which is the case of our installation :
 - The FCR provider must activate the FCR for as long as the frequency deviation persists unless its energy reservoir is exhausted in either the positive or the negative direction.
 - The FCR provider must always guarantee enough energy to be able to fully activate for 15 minutes.
 - The FCR provider must ensure the recovery of its energy reservoirs as soon as possible and within two hours after the clearing of the frequency deviation.

Please notice that these final considerations about assets with limited energy reservoirs highlight the need for management of the remaining available energy in the reservoirs on top of the previous active power range analysis.

1.2 Primary voltage control

Voltage control requirements are not as well defined by *Elia* as their frequency control equivalents. This ancillary service is nowadays mainly applicable to generators producing at least 25MW, in which case the TSO contracts specific amounts of reactive power, depending on the specificities of the unit, its location, and so on. The transmission grid is also provided with specific units dedicated to voltage control. In the classical centralised view of the electrical network, reactive power injections sustain the high-voltage lines in order to maintain a high-enough voltage to limit copper losses and low-enough to avoid breakdown of the insulators. Increasing the decentralised voltage control capabilities however becomes crucial due to the switch from centralised to decentralised production and the rising interconnectivity of transmission grids in Europe. It indeed induces high reactive power fluxes to sustain voltage in the transmission lines, while reducing the injection capabilities through less centralised capacity.

The switching toward decentralised generation is a two-way challenge. Even though small intermittent generating units connected to the distribution grid are likely to introduce disturbances, their increasing flexibility due to inverter-fed connections represents an opportunity to sustain the voltage level directly from the distribution level. As the propagation of reactive power in the lines is rather kept small, this strategic locality of voltage control capacity is a resource to exploit.

As observed in *Elia*'s recent presentation about reactive power management and voltage control [10], the Belgian TSO is still looking into several options and does not yet provide any precise regulatory recommendation. We can however already distinguish some similarities with frequency control, allowing to draw up a general model for voltage control.

In *Elia*'s grid support code about stabilising grid voltage levels by generating or absorbing reactive energy [11], voltage control is split into two different categories according to the considered time scale : primary and centralised voltage control. On the one hand, primary voltage control corresponds to the automatic activation of the *Reactive Power Reserve* (RPR) of *controlling production units* as soon as the voltage deviation is observed. On the other hand, centralised voltage control relies on the manual activation of the resources based on *Elia*'s computations to restore long-term voltage stability according to the fault that created the deviation. In this section, we sum up *Elia*'s general requirements for providing primary voltage control.

1.2.1 Droop control

Controlling production units are characterised by their capability to continuously adapt their reactive power output. The provision of RPR requires that the module keeps aside a certain amount of reactive power ΔQ_{CB} , called the capacity band and usually expressed in $MVar$, to be activated in case of a voltage deviation measured at the point of common coupling. Similarly to primary frequency control, we consider that each RPR providing unit must implement a droop proportional control. It means that, once a deviation is detected, it must automatically react linearly and proportionally. The typical reactive power output function is the following.

$$Q = \alpha_v \Delta V$$

We here have $Q_0 = 0MVar$, as DG units are not usually meant to provide a constant reactive power output. We could nonetheless easily superimpose a non-zero scheduled power in normal operating conditions if required by the grid. α_v is the droop factor. It depends on the capacity band ΔQ_{CB} and the maximum voltage deviation before full activation ΔV_{max} .

$$\alpha_v = \frac{\Delta Q_{CB}}{\Delta V_{max}}$$

Unfortunately, neither the ENTSO-E, nor the Belgian TSO provide any general information about the voltage band to be covered by the control. We then decided the sizing presented in Figure 1.2 according to the following considerations :

- P_n being the active nominal power of the installation, we assume the capacity band to be $\Delta Q_{CB} = 0.33P_n$, as proposed by *Elia*'s future plan in term of reactive power [10].
- As the grid voltage can change from $0.9U_n$ to $1.1U_n$ according to the extra requirements for grid connection in the following subsection, a reasonable strategy is to develop all the reactive installation capacity when the grid voltage reaches its limit values.

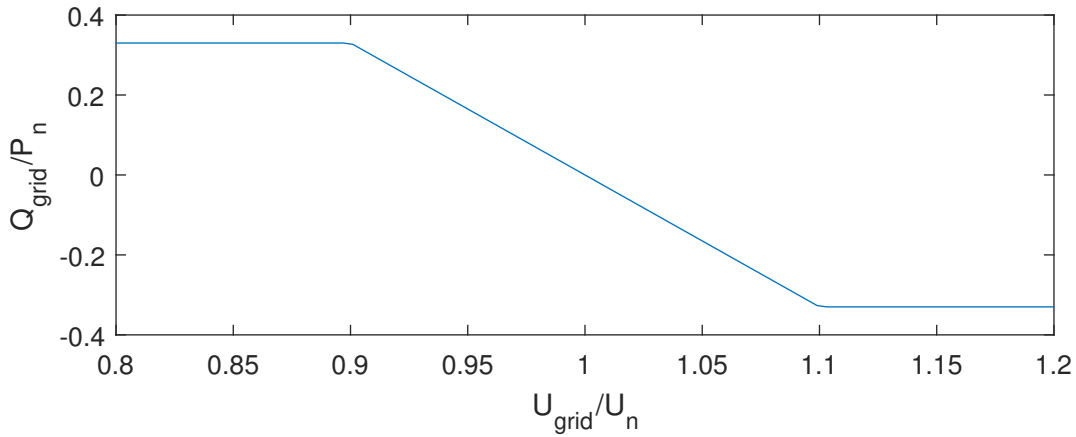


Figure 1.2: Reactive power management : droop control strategy.

1.2.2 Time considerations

Primary voltage control being the first defence against voltage variation, it is crucial that the activation of RPR be as soon as possible after the variation detection. Controlling production units usually use voltage regulators able to perform correction within a matter of just a few seconds. With this in mind, the TSO requires in [6] that the total reaction time be located between 0 and 30 seconds.

Elia does not provide further information about time limits. We then consider that, once the desired capacity is reached, the FCR module must provide it as long as the frequency difference remains.

1.2.3 Additional considerations

In order to complete this section, let us highlight some additional considerations concerning RPR.

- Similarly to the frequency case, the TSO remunerates according to the reserved control bands based on a unit price, the volume contracted in $MVar$ and the length of use.
- The RPR capacity must remain available round the clock.
- Any unit desiring to provide RPR must have access to reliable local voltage measurement.

1.3 Additional requirements

On top of the previous considerations for the production of services to sustain the grid, our installation is also subject to the specific technical prescriptions for DG units connected to the distribution network. These prescriptions are established by *Synergrid*, the Belgian federation of the electricity and gas systems operators, and summed up in [12].

The main requirements concern the acceptance of the possible variations of the grid voltage and frequency by the DG installation. In this section, we start by studying the voltage amplitude and frequency ranges in which the installation must continue to operate normally. This means that the production of FCR and VCR must not be influenced by the variations within these ranges of the network conditions at the *Point Of Connection*, PoC.

Then, grid operators also set requirements to prevent production units from tripping in case of voltage dips in the network. Voltage dips are due to faults occurring in the grid. Once the fault is cleared, the recovery of normal operation is much faster in case the production units remained connected.

Finally, some additional recommendations apply to units connected through a converter. In this case, the injection of harmonics and inter-harmonics into the grid are likely to cause damage to sensitive load and must be kept to a minimum.

1.3.1 Voltage tolerance

According to [12], the DG installation is required to be able to continuously operate even if the voltage at the point of connection with the grid is varying within a $\pm 10\%$ range around its nominal value of 400V.

Our study case is directly subject to this constraint. The ability of maintaining normal operation with a grid voltage varying from $0.9U_n$ to $1.1U_n$ is tested in Chapter 4 simulating the *Negundo* site.

1.3.2 Frequency tolerance

According to [12], the DG installation is required to be able to continuously operate even if the frequency at the point of connection with the grid is varying within a $\pm 2\%$ range around its nominal value of 50Hz. For a maximum time period of 30 minutes, the installation must also be able to operate with a supplying frequency located between 47.5Hz and 51.5Hz.

In practice, the presence of the back-to-back converter allows to decouple the machine from the grid, in terms of frequency. The variable-speed operation of the machine is indeed the main requirement for variable-power production. Therefore, the frequency tolerance of our installation is inherently verified and is not tested in Chapter 4.

1.3.3 Voltage dip tolerance

In [12], *Synergrid* sets very low *Fault Ride-Through* requirements for greater than 1MVA production units. It indeed imposes these units to be able to overcome a 70% residual voltage dip for 0.2 second and a 80% residual voltage dip for 1.5 seconds.

As the last update of source [12] is from 2012, and in the present context of increasing support of the grid by DG, we decided to neglect the requirements of the previous reference in terms of voltage dip tolerance. Instead, we here take into consideration the requirements set by the

European Commission (EC) in its 2016 *Network code on the requirements for connection of grid generators* [13]. In this report, the EC precisely determines a fault-ride-through capability curve for *Power Park Modules* (PPM). Our installation is a typical PPM according to its definition in [13] : "*PPM means a unit [...] generating electricity, which is [...] connected through power electronics, and that also has a single connection point to a [...] distribution system*".

The FRT curve of PPMs is given in Figure 1.3, in which we observe that the current European regulation in terms of FRT is much harder than the ones of *Synergrid* in their last update. Expression 1.1 gives the exact values characterising the FRT curve. These are the retained voltage during the fault, the clearance time of the fault and its associated voltage, and the recovery time and voltage.

$$\begin{cases} u_{ret} = 0.1 \text{ [pu]} \\ u_{clear} = 0.15 \text{ [pu]} , \quad t_{clear} = 0.145 \text{ [s]} \\ u_{rec} = 0.9 \text{ [pu]} , \quad t_{rec} = 2.25 \text{ [s]} \end{cases} \quad (1.1)$$

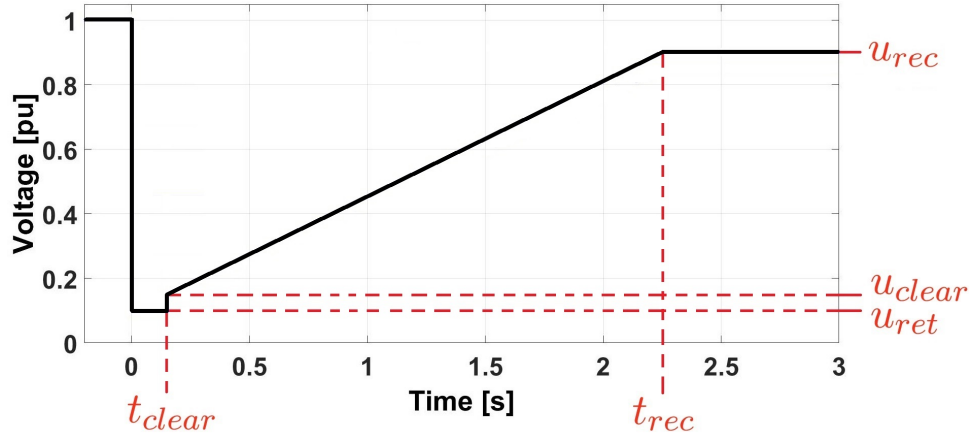


Figure 1.3: Fault Ride-Through capacity curve for Power Park Modules [13].

1.3.4 Harmonics

In [12], *Synergrid* stands that the level of harmonics and inter-harmonics produced by the DG installation must not provoke any perturbation in the distribution grid. In order to do so, *Synergrid* advises in [14] to add filters between the polluting source and the grid to further reduce the propagation of these polluting harmonics into the network. *Synergrid* specifies that the installations must respect the standards set by the *International Electrotechnical Commission* (IEC). The norm concerning the maximum *Total Harmonic Distortion* (THD) in LV is the *CEI 61000-3-12* standard.

Chapter 2

Description of the general model

The aim of this study is to simulate a special implementation of a pumped-hydroelectric energy storage unit, using a single pump for both pumping and turbining. This *Pump-as-Turbine* (PaT) is mechanically coupled to the rotor shaft of a squirrel-cage *Induction Machine* (IM). The IM itself is electrically coupled to the LV grid via a variable-frequency drive.

The installation is composed of three main parts. These are shown in Figure 2.1. The first and central part is the hydraulic machine, represented by the *pump* block. For several reasons, the use of single pumps for both pump and turbine modes presents important advantages. Pumps are indeed cheap, readily available on the market, robust and easy to maintain.

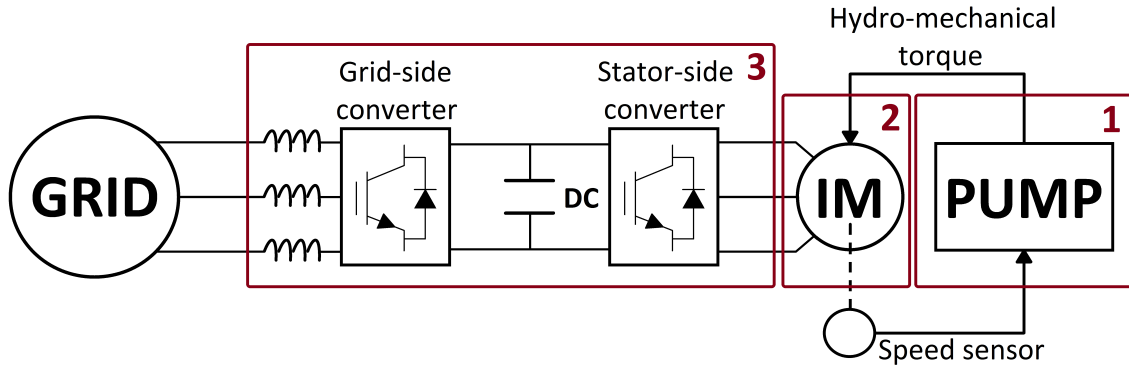


Figure 2.1: Outline of the installation.

The *pump* block actually models the PaT as well as the water column interacting with it. This model is presented in Section 2.1. As manufacturers do not provide any information about the reversing of pumps, we need to establish an accurate model of the hydraulic phenomenon, in order to determine the transient behaviour of the hydro-mechanical torque applied on the shaft, as a function of the shaft rotating speed. The pump is to be used in both pump and turbine modes. The operation of this block thus ranges from positive to negative rotational speeds.

Secondly, the *IM* block represents the asynchronous motor coupled with the pump. As a matter of fact, pumps are usually directly sold with a squirrel-cage induction machine. These are indeed also cheap, readily available on the market, robust and easy to maintain. Asynchronous machines have been widely studied. Thorough models already exist, upon which we base ourselves. The variable-speed control of such machines is however not straightforward. To do so, the chosen strategy is the *Field-Oriented Control* (FOC). It requires the transformation from the three-phase equations into a time-invariant vector system. The establishment of the IM model is given in Section 2.2. The control at the stator-side controller of the active power produced by the whole

installation relies on this model.

Finally, the third block represents the interface between the LV grid and the machine. Its complete model is established in Section 2.3. It is itself split into two different parts : the back-to-back VSI converter and the harmonics filter. This section also models the grid. It is indeed the modeling of the grid-side on the left of the converter that forms the basis for the reactive power regulation by the grid-side controller, as well as the active power transmission. If directly coupled to the grid, induction machines can only operate at one operating point, depending on the mechanical torque applied to the rotor shaft. As pumps do not have regulating valves, the applied mechanical torque is not variable and so is the mechanical power. The ancillary services provision then requires the feeding of the machine through a reversible back-to-back converter. This converter allows the full control and reversibility of the active and reactive power flows.

2.1 The hydraulic model

In this section, the hydraulic part of the installation is presented. As shown in Figure 2.2, the hydraulic installation is composed of four main components : the upper reservoir, the lower reservoir, the penstock and the pump at the bottom of the penstock. The penstock pipe is characterised by its length L and cross-sectional area A . As the pump is also used in turbine mode, we call it a *Pump-as-Turbine* (PaT). The goal of this section is to build the hydraulic model, which takes the rotational speed of the rotor shaft n as input and returns the mechanical torque T_m produced by the hydraulic system. T_m is also called the *hydro-mechanical* torque. We here use the rotational speed n expressed in rotations per second for convenience. Please notice that it is the same variable as the mechanical speed ω_m of the induction machine rotor shaft, to within a constant.

Here are the main physical quantities characterising the system :

- H_{res} , the net head at the PaT entrance [m]
- Q , the flow rate in the penstock [m^3/s]
- $n = \frac{\omega_m}{2\pi}$, the hydraulic machine rotational speed [RPS]
- T_m , the hydro-mechanical torque applied by the PaT on the shaft [Nm]

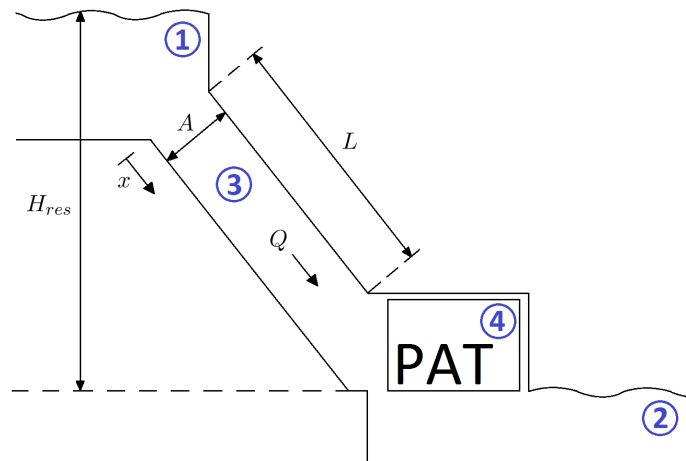


Figure 2.2: Overview of the hydraulic system.

In order to simplify the hydraulic model, the reservoirs levels are considered steady, fixing the static head H_{res} of the PaT at a constant value. This enables to obtain a simple model made of the interaction between the equivalent water column contained in the penstock and the hydraulic machine.

2.1.1 Water column

The subsystem representing the water column is used to recover the mass flow rate Q flowing through the hydraulic machine, as a function of its head H . To do so, two different approaches are developed, with different levels of complexity and fidelity to the reality.

The first model is also the simplest. It is here called *inelastic* and takes several hypothesis into consideration. Its reduced complexity is due to the fact that the problem reduces to a simple representation by an *Ordinary Differential Equation* (ODE), which is solved in a

straightforward manner.

The second model is called *elastic* and is closer to the reality. In this case, the problem is expressed by a set of *Partial Differential Equations* (PDEs) involving both the time and the axial direction of the penstock pipe and requiring time and space discretisations. The interest of the elastic model over the inelastic equivalent is detailed later in this section.

The inelastic model

This total hydraulic inelastic model is presented in Figure 2.3, showing the input and output to the model, as well as the global interaction between the water column and the PaT.

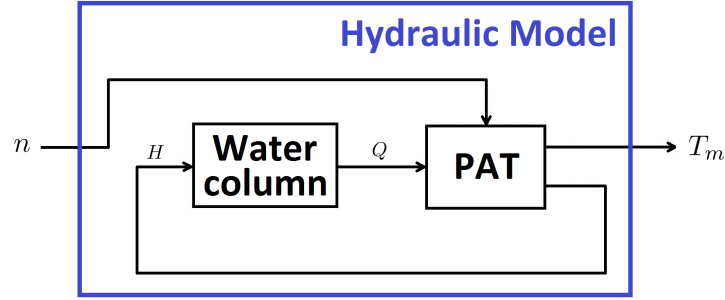


Figure 2.3: Overview of the inelastic hydraulic model.

This model assumes the following three main simplifications :

- The water is incompressible.
- The penstock pipe is inelastic.
- There is no friction in the penstock.

These assumptions allow to consider the water characteristics as constants along the penstock pipe radius and axis. It reduces the *Navier-Stokes* equations to a simple balance of forces along the pipe axis. The water column dynamics is then simply characterised by *Newton's* second law :

$$(\rho LA) \frac{dU}{dt} = -A(\rho g) \Delta H \quad (2.1)$$

ρ being the water density, ρLA represents the total mass in the penstock. U being the axial speed of the water, $\frac{dU}{dt}$ is the acceleration of the fluid. With g being the gravity acceleration, the right-hand term of Equation 2.1 is the equivalent force applied to the column due to the pressure difference.

Defining the water flow rate $Q = \rho AU$, Equation 2.1 is reduced to the desired form in the *Laplace* domain.

$$\boxed{\frac{Q}{\Delta H} = -\frac{Ag}{L} \frac{1}{s}} \quad (2.2)$$

Expression 2.2 highlights the existence of a first order transfer function between the flow rate Q and the pressure difference ΔH between the steady state head H_{res} and the actual head at the PaT entrance H .

Figure 2.4 shows the block diagram of the inelastic water column model. The initial condition Q_i for the flow rate is obtained knowing the initial head $H_i = H_{res}$ and initial speed n_i for the pump shaft. These two conditions allow to recover Q_i by iterations on the PaT characteristic curves, which are presented later in this section.

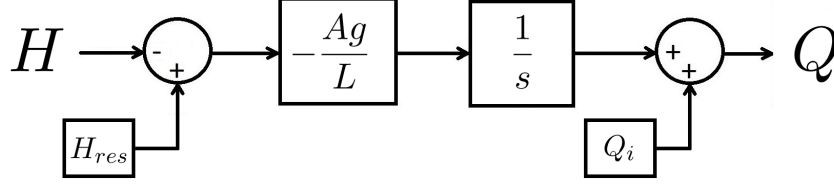


Figure 2.4: Inelastic model block diagram.

The elastic model

In the elastic case, the model now takes into considerations the three following considerations :

- The water is compressible, K being its bulk modulus.
- The penstock pipe is elastic. E , e , D are respectively its young modulus, thickness and diameter.
- There is friction in the penstock, f being its friction factor.

This more complete model is built upon the two conservation equations developed in reference [15]. We here simply recall their final form 2.3, to be used for our model.

$$\begin{cases} \frac{\partial H}{\partial t} + \frac{a^2}{gA} \frac{\partial Q}{\partial x} = 0 \\ \frac{\partial Q}{\partial t} + gA \frac{\partial H}{\partial x} + \frac{fQ|Q|}{2DA} = 0 \end{cases} \quad (2.3)$$

These equations require the definition of the wave velocity a , which depends on the water and pipe physical properties. This velocity corresponds to the propagation speed of the pressure wave along the axis x of the pipe.

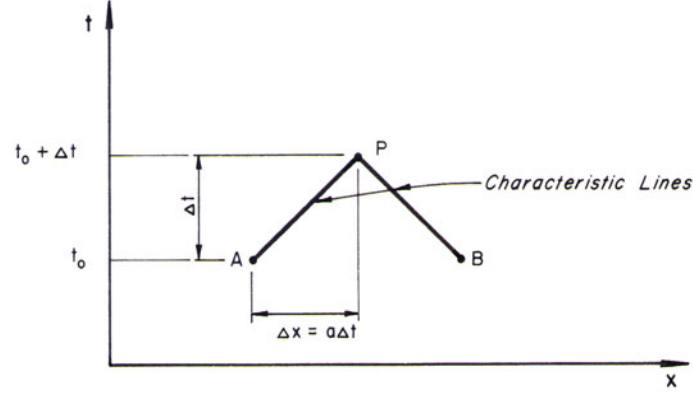
$$a \triangleq \left| \frac{dx}{dt} \right| = \sqrt{\frac{K/\rho}{1 + \frac{DK}{eE}}} \quad (2.4)$$

Equations 2.3 respectively represent the continuity equation and the momentum equation. Together, they form a system of PDEs depending on the two independent variables t and x . As detailed in [15], the characteristic method is typically chosen to solve this system. This method is illustrated in Figure 2.5.

The characteristic method uses the definition of the wave velocity to link the two variables together and convert the PDEs into a set of two ODEs in the only remaining variable t .

$$\begin{cases} \frac{dQ}{dt} + \frac{gA}{a} \frac{dH}{dt} + \frac{fQ|Q|}{2DA} = 0 \\ \frac{dQ}{dt} - \frac{gA}{a} \frac{dH}{dt} + \frac{fQ|Q|}{2DA} = 0 \end{cases} \quad (2.5)$$

The independent space variable x has been removed in Equations 2.5. These equations are now only valid along the characteristic line and not in the full x - t plane anymore. In Figure 2.5, the line segment A-P and B-P represent the characteristic lines relative to Equations 2.5.

Figure 2.5: Characteristic line in the x - t plane [15].

The knowledge of these ODEs allows to calculate the coupled time evolution of the flow rate Q and the head H . In practice, the simulation solves these equations at every time step. As Equations 2.5 are verified along the characteristic lines only, there is a constraint between the time step Δt and the spatial step Δx used to solve the system. The link between the two is the wave velocity.

$$\Delta x = a\Delta t \quad (2.6)$$

As the spatial step is fixed by the time step, we are only able to simulate a discretised approximation of the penstock pipe length. The precision of this approximation depends on the chosen time step.

From Figure 2.5, we observe that, if the initial conditions for points A and B are known at t_0 , the solution for point P at the next time step can be recovered. Indeed, the integration of Equations 2.5 from t_0 to $t_0 + \Delta t$ gives the linear system 2.7 linking the water flow rate at point P Q_P to its head H_P .

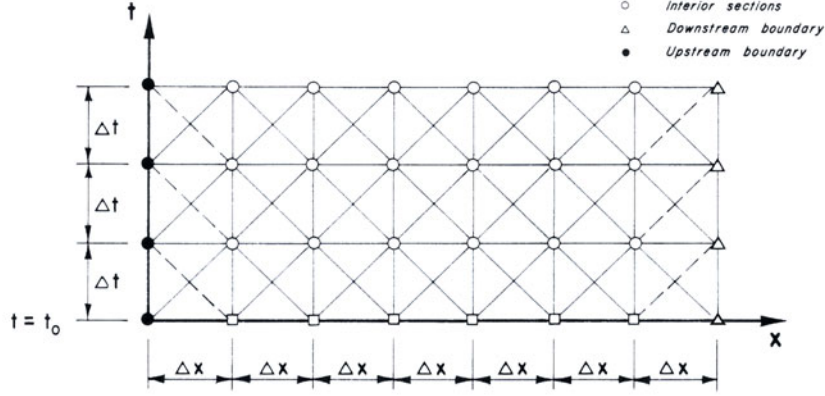
$$\begin{cases} Q_P = C_A - \frac{gA}{a}H_P \\ Q_P = C_B + \frac{gA}{a}H_P \end{cases} \quad (2.7)$$

The constants C_A and C_B are given by

$$\begin{cases} C_A = Q_A + \frac{gA}{a}H_A - \frac{f}{2DA}\Delta t Q_A |Q_A| \\ C_B = Q_B - \frac{gA}{a}H_B - \frac{f}{2DA}\Delta t Q_B |Q_B| \end{cases} \quad (2.8)$$

Figure 2.6 gives the the full x - t grid for the first time steps. The previous method allows to obtain all the interior points of the grid. There remains however a limitation for the points on the extreme left and right. These are only sourced by one characteristic line each. In order to obtain a solution for these points, there is a need to consider upstream and downstream boundary conditions.

Upstream boundary condition In the case of the points P_L on the extreme left, only the second of Equations 2.5 is valid, leading to a single linear equation.

Figure 2.6: Characteristic grid in the x - t plane [15].

$$Q_{P_L} = C_B + \frac{gA}{a} H_{P_L} \quad (2.9)$$

Seeing that we consider constant level reservoirs, the head at the upper reservoir is constant and equal to H_{res} . We then simply need to replace H_{P_L} by this value in Equation 2.9 to recover the upstream boundary.

Downstream boundary condition In the case of the points P_R on the extreme right, only the first of Equations 2.5 is valid, leading to a single linear equation.

$$Q_{P_R} = C_A - \frac{gA}{a} H_{P_R} \quad (2.10)$$

The missing downstream boundary condition is here given by the model of the PaT, whose characteristic curves link the flow rate to the head at the entrance of the machine, thus at the downstream boundary of the water column.

Which model in which conditions?

The choice between the elastic or inelastic model is a trade-off between computational cost and results precision. The inelastic model is indeed much faster, but it neglects some important features of fluid dynamics. On the other hand, the elastic model shows a greater potential for accuracy. The use of the elastic implementation makes sense if the influence of the travelling pressure wave largely affects the results. It is the case only if the variations at the downstream boundary condition are faster than the time for the wave to travel back and forth in the pipe. This round-trip time is computed using the definition of the wave velocity.

$$\Delta t = \frac{2L}{a}$$

It seems obvious that the simulation time step needs to be chosen much smaller than this Δt in order for the spatial discretisation to become significant. Otherwise, according to relation 2.6, the pipe would be divided in less than one spatial element.

The graphical comparison of both elastic and inelastic models is carried out in Chapter 4 about the *Negundo* study case.

2.1.2 Hydraulic machine : a Pump-as-Turbine

At the bottom of the water column is located the hydraulic machine. For the inelastic model, this subsystem of the total hydraulic model takes the flow rate Q given by the hydraulic machine and the rotational speed of the shaft as inputs. It is used to recover the head H at PaT entrance and the hydro-mechanical torque T_m applied by the fluid on the pump shaft. For the elastic model, this subsystem takes in input the the rotational speed of the shaft and gives the downstream boundary condition and the hydro-mechanical torque T_m .

As explained in the introduction to this section, our study focuses on a particular hydraulic machine. The name *Pump-as-Turbine* is rather explicit. Even though manufacturers do not intend pumps to be used for turbinning, they also work in reverse mode. Some recent studies aimed at determining the behaviour of these PaTs ([16], [17], [18] and [19]). They however undergo some disadvantages :

- Hydraulic characteristics in turbine mode are not given by the manufacturers. Pumps are indeed not required to work in reverse mode. It is therefore impossible to exactly predict their characteristics and working ranges in this case.
- Pumps rarely exceed 500kW of maximum power. These low power ranges limit their ability to provide FCR. TSOs indeed do not usually contract primary reserves with less than 1MW power plants. Multiple units would be required to provide significant services, as mentioned in [20].
- Unlike typical turbines being built with valves, pumps do not present any flow regulating device. This implies extremely poor performances away from the *Best Efficiency Point* (BEP)[21] .

Despite these drawbacks, PATs present strong assets [22], which justify the scope of this study :

- Pumps are cheaper than reversible machines.
- Pumps are readily available on the market.
- Pumps are robust and easy to maintain.

In order to use the model for pumping and turbinning, we now need to define the characteristic curves of the machine in both of these modes. The characteristic curves of a pump or a turbine typically draw the evolution of the head H and the mechanical power P of the machine as functions of the water flow rate Q , for a certain speed n . Several curves are superimposed to represent several working speeds n . Each operating point is thus characterised by a vector (H, P, Q, n) .

Knowing the coordinates (H_1, P_1, Q_1, n_1) of an operating point at speed n_1 , the similitude principle allows to obtain its equivalent (H_2, P_2, Q_2, n_2) at speed n_2 using the similitude relations 2.11.

$$\begin{cases} Q_2 = Q_1 \frac{n_2}{n_1} \\ H_2 = H_1 \left(\frac{n_2}{n_1} \right)^2 \\ P_2 = P_1 \left(\frac{n_2}{n_1} \right)^3 \end{cases} \quad (2.11)$$

These relations come from the fact that, even if the speed of the shaft is changed, the speed triangle remains similar. It leads to the following conclusion : all the working points of the pump,

for any rotational speed, are retrievable as soon as the characteristic curves $H(Q)$ and $P(Q)$ are known for at least one specific speed.

As this study aims at controlling the pump in variable-speed conditions, it would be interesting to use these speed similitude properties 2.11 to develop a general model for the pump being independent of the speed variable. This is done by introducing the speed normalisation of the characteristic variables Q , H and P . They respectively lead to the definition of the following three dimensionless variables : the flow rate variable ϕ , the head variable ψ and the power variable π .

$$\boxed{\phi = \frac{Q}{nD_t^3} \quad \psi = \frac{gH}{n^2 D_t^2} \quad \pi = \frac{P}{\rho n^3 D_t^5}} \quad (2.12)$$

On top of the physical constant D_t , g , and ρ , we observe that the dimensionless variable of 2.12 make use of the similitude relations to normalise the flow rate, the head pressure and the hydro-mechanical power, respectively by n , n^2 and n^3 .

According to the previous considerations, the typical manufacturer $H(Q)$ and $P(Q)$ curves valid for one speed become the two single dimensionless curves $\psi(\phi)$ and $\pi(\phi)$ valid for any speed. The rest of this section strives to find these characteristic curves for both working modes.

Pump characteristics

Choosing a pump available on the market, the corresponding manufacturer datasheet gives the characteristic curves $H_p(Q)$ and $P_p(Q)$ for some particular speeds, in pump mode. The subscript p stands for *pump mode*. The dimensionless pumping curves $\psi_p(\phi_p)$ and $\pi_p(\phi_p)$ are extracted using the three expressions 2.12. The pump efficiency is computed in the following manner.

$$\eta_p(\phi_p) = \frac{\rho g H_p Q_p}{P_p} = \frac{\phi_p \psi_p}{\pi_p} \quad (2.13)$$

The previous Equation 2.13 highlights the dependence of the efficiency on the dimensionless flow rate variable ϕ_p . The characteristic curves then allow to extract the BEP, which is the operating point of the pump with the highest efficiency. It simply corresponds the dimensionless flux ϕ_{pb} maximising the function $\eta_p(\phi_p)$. It is then linked to the dimensionless operating point $(\phi_{pb}, \psi_{pb}, \pi_{pb})$, with the subscript b standing for *BEP*.

Turbine characteristics

PaT characteristic curves are not given by pump manufacturers and difficult to predict based on the characterisation in pump mode. A straightforward and unique relation relevant to any pump in turbine mode is impossible to find due to pumps variety and complexity [21]. Several authors suggest different relations, based on experimental data, to estimate the turbine BEP as a function of the BEP in pump mode. Among others, [16], [17], [18] and [19] offer some promising results.

The last article about the experimental characterisation of centrifugal PaTs at different speeds [19] is the most recent and accurate of these sources. In this article, the BEPs considerations are used to develop a method allowing the full recovery of the turbine characteristic curves based on the pump characterisation. We use the correlations of this article in terms of the dimensional variables H , P and Q , and fit them to the normalised variables ϕ , ψ and π using Equations 2.12.

The recovery of the full turbinning characteristics for our model follows a two-step procedure : approximation of the BEP first and then, extrapolation of the curves around the BEP. The extra subscript t is introduced to express *turbine mode* conditions.

Step 1 The starting point is to estimate the turbinning BEP as a function of its pumping equivalent, which is itself either given by the manufacturer or computed using the previous considerations. Using reference [19], the following relations are computed.

$$\frac{\psi_{tb}^{-0.5}}{\psi_{pb}^{-0.5}} = 1.3980 \frac{\phi_{pb}^{0.5}}{\psi_{pb}^{0.75}} + 0.6464 \quad (2.14)$$

$$\frac{\phi_{tb}^{0.5}}{\psi_{tb}^{0.75}} = 0.9413 \frac{\phi_{pb}^{0.5}}{\psi_{pb}^{0.75}} - 0.0101 \quad (2.15)$$

$$\frac{\pi_{tb}^{0.5}}{\psi_{tb}^{1.25}} = 0.849 \frac{\pi_{pb}^{0.5}}{\psi_{pb}^{1.25}} - 0.0206 \quad (2.16)$$

The BEP in pump mode being known and given by $(\phi_{pb}, \psi_{pb}, \pi_{pb})$, Equation 2.14 returns the dimensionless head in turbine mode at the BEP ψ_{pb} . The dimensionless flux ϕ_{tb} and power π_{tb} are then extracted using Equations 2.15 and 2.16.

Step 2 The last step is to build the turbine curves $\psi_t(\phi_t)$ and $\pi_t(\phi_t)$ using the turbine BEP conditions. This is done by applying the following equations adapted from [19].

$$\begin{aligned} \frac{\psi_t}{\psi_{tb}} &= 1.0283 \left(\frac{\phi_t}{\phi_{tb}} \right)^2 - 0.5468 \left(\frac{\phi_t}{\phi_{tb}} \right) + 0.5314 \\ \frac{\pi_t}{\pi_{tb}} &= -0.3092 \left(\frac{\phi_t}{\phi_{tb}} \right)^3 + 2.1472 \left(\frac{\phi_t}{\phi_{tb}} \right)^2 - 0.8865 \left(\frac{\phi_t}{\phi_{tb}} \right) + 0.0452 \end{aligned} \quad (2.17)$$

Similarly to Equation 2.13, the turbine efficiency is computed using the following equation.

$$\eta_t(\phi_t) = \frac{P_t}{\rho g H_t Q_t} = \frac{\pi_t}{\phi_t \psi_t}$$

Operating range

The theoretical model developed in this section assumes the possibility of working along the full characteristic curves, ranging from zero to maximum efficiency. In reality, pumps are manufactured to operate closely to the BEP. Datasheets indeed simply provide advisable working ranges around the BEP. The use of the PaT far from its BEP is likely to reach the stability limits of the pump or lead to cavitation. Steady-state operation in this area is impossible, as it risks to permanently damage the installation.

The real operating limits of the PaT are neglected in the present study. Their exact identification would require experimental analysis of real pumps, which is out of the scope of this study. They should however be considered for further studies.

2.2 The squirrel-cage induction machine

Squirrel-cage asynchronous generators are the most commercialised three-phase machines. Hence, there already exists plenty of information about their functioning. In this section, we do not develop the extended equations of the asynchronous motor, as they are readily available in multiple sources. We rather focus on the establishment of a concise model allowing to apply *field-oriented control* in the *Park* coordinate system.

2.2.1 General three-phase equations

The basic three-phase electrical equations characterising the stator and rotor phase voltages of an induction machine are the following

$$\begin{cases} u_{sa} = R_{sa}i_{sa} + \frac{d\phi_{sa}}{dt} \\ u_{sb} = R_{sb}i_{sb} + \frac{d\phi_{sb}}{dt} \\ u_{sc} = R_{sc}i_{sc} + \frac{d\phi_{sc}}{dt} \end{cases} \quad (2.18)$$

$$\begin{cases} u_{ra} = R_{ra}i_{ra} + \frac{d\phi_{ra}}{dt} \\ u_{rb} = R_{rb}i_{rb} + \frac{d\phi_{rb}}{dt} \\ u_{rc} = R_{rc}i_{rc} + \frac{d\phi_{rc}}{dt} \end{cases} \quad (2.19)$$

The first subscript of each variable is either *s* or *r*. It stands for *stator* or *rotor*. The second subscript represents the phase, *a*, *b* or *c*.

These equations are generic. In practice, induction generators are symmetrical, so that $R_{sa} = R_{sb} = R_{sc} = R_s$ and $R_{ra} = R_{rb} = R_{rc} = R_r$. By its nature, the squirrel-cage rotor has also short-circuited equivalent windings. This means $u_{ra} = u_{rb} = u_{rc} = 0$.

In their three-phase form, the flux linkages vary according to the variation of the mutual inductance during the rotation of the rotor. In order to apply the field-oriented control to the machine, it is crucial to obtain a common rotating reference frame for both stator and rotor components. This will allow to fully develop these equations into their decoupled *dq* form, using position-independent parameters of the machine.

The *direct-quadrature transform* projecting the three-phase values onto the rotating *Park* reference frame is presented in Appendix C.

The development of equations 2.18 and 2.19 and the application of the transform is long and irrelevant to this study. The reader eager to obtain more precision will find it in [23].

2.2.2 Park equations

The electrical machine used for the sake of our simulation is the *Asynchronous Machine in SI Units* found in the *Simscape/Power Systems* library of *Simulink*. We directly extract the *Park* equations from its documentation [24] and reference [25]. We then develop them into the final form that enables the application of independent direct- and quadrature-current control.

The equivalent model

The general form of the stator voltage equations in terms of the direct and quadrature components are the following

$$\begin{cases} u_{sd} = R_s i_{sd} + \frac{d\phi_{sd}}{dt} - \dot{\xi}_s \phi_{sq} \\ u_{sq} = R_s i_{sq} + \frac{d\phi_{sq}}{dt} + \dot{\xi}_s \phi_{ds} \end{cases} \quad (2.20)$$

These equations are composed of three terms. The first one corresponds to the voltage drop across the equivalent resistance of the stator windings. The second one is the counter-electromotive force, as stated by *Lenz's Law*. It is due to the variation of the magnetic flux intercepted by the stator windings. The third one is a coupling term due to the rotation of the reference frame at a speed $\dot{\xi}_s$ by comparison to the still stator frame.

The rotor voltage equations in terms of the direct and quadrature components have the same general form as the stator equations.

$$\begin{cases} u_{rd} = R_r i_{rd} + \frac{d\phi_{rd}}{dt} - \dot{\xi}_r \phi_{rq} \\ u_{rq} = R_r i_{rq} + \frac{d\phi_{rq}}{dt} + \dot{\xi}_r \phi_{rd} \end{cases} \quad (2.21)$$

The stator flux linkage equations are given here under. They are composed of terms due to both stator and rotor currents.

$$\begin{cases} \phi_{sd} = (L_m + L_{ls}) i_{sd} + L_m i_{rd} \\ \phi_{sq} = (L_m + L_{ls}) i_{sq} + L_m i_{rq} \end{cases} \quad (2.22)$$

The sum of the leakage inductance L_{ls} and the mutual inductance L_m is called the *cyclic inductance* and is written L_{cs} .

Equations 2.23 define the rotor flux linkage. This time, the sum of the rotor leakage inductance and the mutual inductance has already been replaced by its cyclic inductance L_{cr} .

$$\begin{cases} \phi_{rd} = L_{cr} i_{rd} + L_m i_{sd} \\ \phi_{rq} = L_{cr} i_{rq} + L_m i_{sq} \end{cases} \quad (2.23)$$

The equivalent electrical circuit of the machine in *Park* coordinates is presented in Figure 2.7. Please note that only the direct and quadrature components will be further studied. As observed in the figure, there exists a complete decoupling between the zero and dq systems. As mentioned in [23], both stator and rotor homopolar systems do not contribute to the electromechanical energy conversion and can only be a source of iron losses through the resistance. Hence there is no point in feeding them with non-zero voltage.

In our case, there can not be any homopolar rotor current as the use of a squirrel-cage rotor implies the short-circuiting of its equivalent windings. This means $u_{r0} = i_{r0} = 0$.

In regards to the stator, homopolar currents can be avoided through wye-connection with isolated neutral point of the windings in the terminal box. If so, the sum of the stator currents is zero, so as the homopolar component. This also means $u_{s0} = i_{s0} = 0$.

Development of the electrical equations

In the case of a squirrel-cage generator, the only possible control is on the applied voltage at the stator windings. Hence, we will now focus on developing the stator voltage equations into a form that is suitable for the application of the control. The rotor voltage equations do not require further expansion.

By substitution of the flux components definition 2.22 and 2.23 into the stator voltage equations 2.20, we obtain

$$\begin{cases} u_{sd} = R_s i_{sd} + \frac{d}{dt} (L_{cs} i_{sd} + L_m i_{rd}) - \dot{\xi}_s (L_{cs} i_{sq} + L_m i_{rq}) \\ u_{sq} = R_s i_{sq} + \frac{d}{dt} (L_{cs} i_{sq} + L_m i_{rq}) + \dot{\xi}_s (L_{cs} i_{sd} + L_m i_{rd}) \end{cases} \quad (2.24)$$

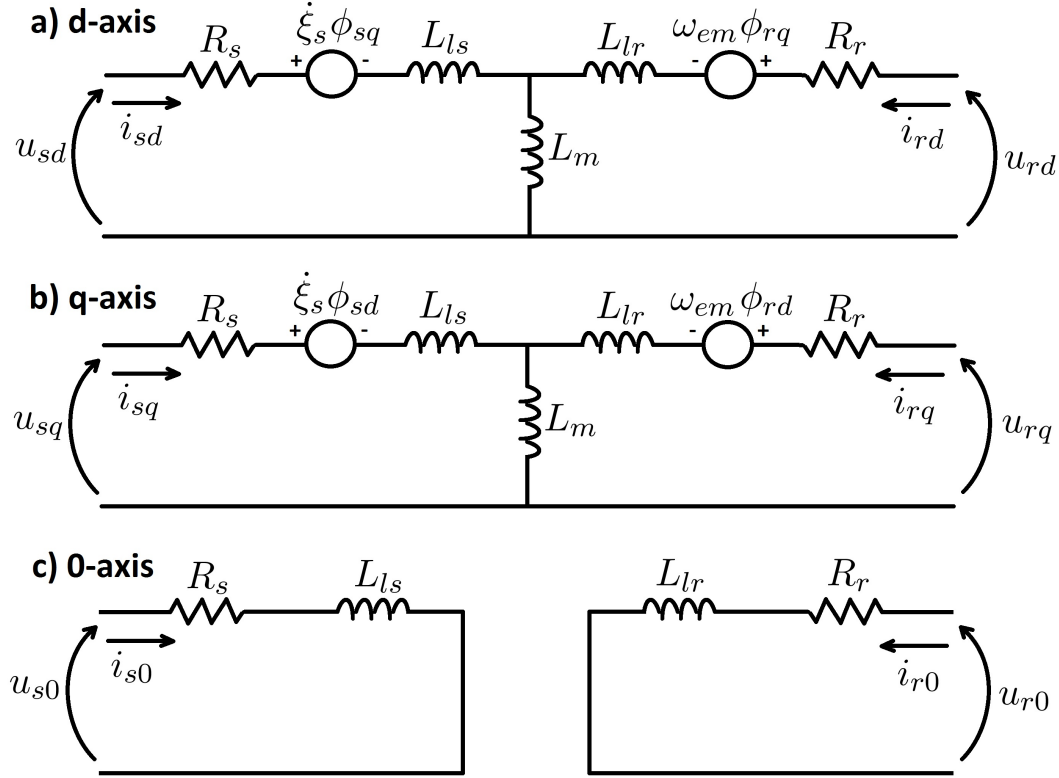


Figure 2.7: Electrical system of the squirrel-cage induction machine - direct, quadrature and zero components. Adapted from [25].

In this form, the stator voltage components explicitly rely on the rotor currents i_{rd} and i_{rq} , which are both inaccessible for direct control. We then use equations 2.23 to bring out these components as functions of the flux linkages and the stator currents

$$\begin{cases} i_{rd} = \frac{1}{L_{cr}}(\phi_{rd} - L_m i_{sd}) \\ i_{rq} = \frac{1}{L_{cr}}(\phi_{rq} - L_m i_{sq}) \end{cases} \quad (2.25)$$

We substitute equations 2.25 into 2.24 to obtain the stator voltage equations in terms of the stator currents and the rotor flux linkages.

$$\begin{cases} u_{sd} = R_s i_{sd} + \frac{d}{dt} \left[L_{cs} i_{sd} + \frac{L_m}{L_{cr}} (\phi_{rd} - L_m i_{sd}) \right] - \dot{\xi}_s \left[L_{cs} i_{sq} + \frac{L_m}{L_{cr}} (\phi_{rq} - L_m i_{sq}) \right] \\ u_{sq} = R_s i_{sq} + \frac{d}{dt} \left[L_{cs} i_{sq} + \frac{L_m}{L_{cr}} (\phi_{rq} - L_m i_{sq}) \right] + \dot{\xi}_s \left[L_{cs} i_{sd} + \frac{L_m}{L_{cr}} (\phi_{rd} - L_m i_{sd}) \right] \end{cases} \quad (2.26)$$

Sorting this equation, can isolate the current components from the terms involving the fluxes.

$$\begin{cases} u_{sd} = R_s i_{sd} + \left(1 - \frac{L_m^2}{L_{cs} L_{cr}}\right) L_{cs} \frac{di_{sd}}{dt} + \frac{L_m}{L_{cr}} \frac{d\phi_{rd}}{dt} - \dot{\xi}_s \left[\left(1 - \frac{L_m^2}{L_{cs} L_{cr}}\right) L_{cs} i_{sq} + \frac{L_m}{L_{cr}} \phi_{rq} \right] \\ u_{sq} = R_s i_{sq} + \left(1 - \frac{L_m^2}{L_{cs} L_{cr}}\right) L_{cs} \frac{di_{sq}}{dt} + \frac{L_m}{L_{cr}} \frac{d\phi_{rq}}{dt} + \dot{\xi}_s \left[\left(1 - \frac{L_m^2}{L_{cs} L_{cr}}\right) L_{cs} i_{sd} + \frac{L_m}{L_{cr}} \phi_{rd} \right] \end{cases} \quad (2.27)$$

In order to refine this equation and recover the same form as in [23], we first need to define a constant σ such that

$$\sigma \triangleq 1 - \frac{L_m^2}{L_{cs} L_{cr}} \quad (2.28)$$

From [23], we know that the flux linkages become independent of the rotor electrical position θ_{em} if

$$\xi_s - \xi_r - \theta_{em} = \text{constant} \quad (2.29)$$

This implies that the d and q axes of the stator and rotor reference frames are stationary by comparison to each other. Furthermore, if this constant value is fixed to zero,

$$\xi_s - \xi_r - \theta_{em} = 0 \quad \Leftrightarrow \quad \dot{\xi}_s = \omega_{em} + \dot{\xi}_r \quad (2.30)$$

these reference frames overlap exactly and we can align the rotor flux $\bar{\phi}_r = \phi_{rd} + j\phi_{rq}$ on the d-axis such that

$$\bar{\phi}_r = \phi_{rd} \quad (2.31)$$

and

$$\phi_{rq} = \frac{d\phi_{rq}}{dt} = 0 \quad (2.32)$$

Injecting equations 2.28, 2.30 and 2.32 into the previous voltage equations finally gives

$$\begin{aligned} u_{sd} &= R_s i_{sd} + \sigma L_{cs} \frac{di_{sd}}{dt} \\ &\quad + \frac{L_m}{L_{cr}} \frac{d\phi_{rd}}{dt} - (\omega_{em} + \dot{\xi}_r) \sigma L_{cs} i_{sq} \\ u_{sq} &= R_s i_{sq} + \sigma L_{cs} \frac{di_{sq}}{dt} \\ &\quad + (\omega_{em} + \dot{\xi}_r) \left(\sigma L_{cs} i_{sd} + \frac{L_m}{L_{cr}} \phi_{rd} \right) \end{aligned} \quad (2.33)$$

This final form of the stator voltage equations highlights the dependence between the current and the voltage in both direct and quadrature directions. Remaining coupling terms are also present and need to be taken into account in the design of the controller.

The reference frame position

One of the main challenges of the chosen field-oriented control is the approximation of the stator reference frame position in order to apply the *dq transform*. The instantaneous position of this frame is given by ξ_s . As long as the initial condition is known, it can be recovered from the integration of its speed

$$\dot{\xi}_s = \omega_{em} + \dot{\xi}_r \quad (2.34)$$

The determination of $\dot{\xi}_s$ then requires obtain the speed of the rotor reference frame $\dot{\xi}_r$. Using equation 2.21 of the rotor q-axis voltage, we obtain the following

$$0 = R_r i_{rq} + \dot{\xi}_r \phi_{rd} \quad \Leftrightarrow \quad \dot{\xi}_r = -\frac{R_r i_{rq}}{\phi_{rd}} \quad (2.35)$$

Indeed, the squirrel-cage rotor windings present no voltage source and in the chosen reference frame, the quadrature rotor flux linkage is zero. The latter also allows to transform the rotor q-flux equation 2.23 and isolate the expression of i_{rq} .

$$0 = L_{cr}i_{rq} + L_m i_{sq} \quad \Leftrightarrow \quad i_{rq} = -\frac{L_m}{L_{cr}} i_{sq} \quad (2.36)$$

By injecting this expression into 2.35

$$\dot{\xi}_r = L_m \frac{R_r}{L_{cr}} \frac{i_{sq}}{\phi_{rd}} \quad (2.37)$$

and defining the rotor time constant

$$T_r \triangleq \frac{L_{cr}}{R_r} \quad (2.38)$$

we obtain

$$\xi_s(t) = \int_0^t \frac{L_m}{T_r} \frac{i_{sq}}{\phi_{rd}} dt + \theta_{em}(t) \quad (2.39)$$

This expression of the stator angle $\xi_s(t)$ requires to evaluate both the rotor flux and electrical position in real time. We see in the next section that the rotor flux is directly linked to the stator d-current. Regarding the rotor electrical angle, it is usually recovered by integration of the rotor mechanical speed ω_m .

$$\theta_{em}(t) = p \left(\int_0^t \omega_m(t) dt + \theta_{m,0} \right) \quad (2.40)$$

$\theta_{m,0}$ being the initial angular position of the rotor and p , the number of pole pairs.

The flux linkage

We now simply need to find a way to approximate the rotor flux ϕ_{rd} based on the computation of the stator current i_{sd} . Similarly to equations 2.35, we use $u_{rd} = 0$ and $\phi_{rq} = 0$ in the rotor d-axis equation 2.21 to find an expression of the d-axis rotor current in terms of the d-axis rotor flux.

$$0 = R_r i_{rd} + \frac{d\phi_{rd}}{dt} \quad \Leftrightarrow \quad i_{rd} = -\frac{1}{R_r} \frac{d\phi_{rd}}{dt} \quad (2.41)$$

Injecting this expression in the rotor d-axis flux linkage equation 2.23 gives

$$\phi_{rd} = -\frac{L_{cr}}{R_r} \frac{d\phi_{rd}}{dt} + L_m i_{sd} \quad (2.42)$$

which leads to the following transfer function

$$\frac{\Phi_{rd}(s)}{I_{sd}(s)} = \frac{L_m}{1 + T_r \cdot s} \quad (2.43)$$

It is here crucial to notice that, the rotor flux linkage is fully determined by the d-axis component of the stator current. The magnetisation of the machine relies only on the d-current.

The electromagnetic torque

The *simulink* documentation [24] provides the following equation for the electromagnetic torque produced by the motor

$$T_{em} = p(\phi_{sd}i_{sq} - \phi_{sq}i_{sd}) \quad (2.44)$$

Using the stator flux equations 2.22 gives

$$T_{em} = pL_m(i_{rd}i_{sq} - i_{rq}i_{sd}) \quad (2.45)$$

in which we inject the rotor current equations 2.25 to obtain the final form of the electromagnetic torque

$$T_{em} = p \frac{L_m}{L_{cr}} \phi_{rd} i_{sq} \quad (2.46)$$

This expression highlights the fact that the electromagnetic torque is simply proportional to the product of the rotor flux linkage and the q-axis stator current. It is shown in the previous subsection that the rotor flux linkage only relies on the d-axis component of the current. Fixing the flux linkage $\phi = \phi_{rd}$ by controlling the d-current, the torque can then be controlled independently through the regulation of the q-current i_{sq} . These considerations are at the centre of our control strategy.

2.2.3 Mechanical system

In order to complete the model of the squirrel-cage induction machine, we need to determine the mechanical evolution of the rotor shaft under stress. In [24], the mechanical system is modelled by the *per unit* equation

$$\frac{d}{dt} \left(\frac{\omega_m}{\omega_{nom}} \right) = \frac{1}{2H} \frac{(T_{em} - F\omega_m - T_m)}{T_{nom}} \quad (2.47)$$

Given the definition of the inertia constant H

$$H \triangleq \frac{1}{2} J \frac{\omega_{nom}^2}{P_{nom}} = \frac{1}{2} J \frac{\omega_{nom}}{T_{nom}} \quad (2.48)$$

we observe, by injection of 2.48 into 2.47, that it corresponds to the classical mechanical equation in SI units.

$$J \frac{d\omega_m}{dt} = T_{em} - F\omega_m - T_m \quad (2.49)$$

The acceleration of the rotor is proportional, up to the inertia coefficient J, to the difference between the electromagnetic torque T_{em} and the mechanical resistance due to the friction factor F and the mechanical torque T_m .

The watchful reader will notice that the whole hydraulic system developed in section 2.1 is here hidden behind the variable T_m , which highly depends on the rotational speed ω_m .

2.3 Interface with the grid

Now that both the hydraulic and induction machines models are established, this section analyses the different elements interfacing the electrical grid and the asynchronous motor. Figure 2.8 highlights the presence of a converter and a filter before connecting the installation to the main grid. The converter is used as a variable-speed drive.

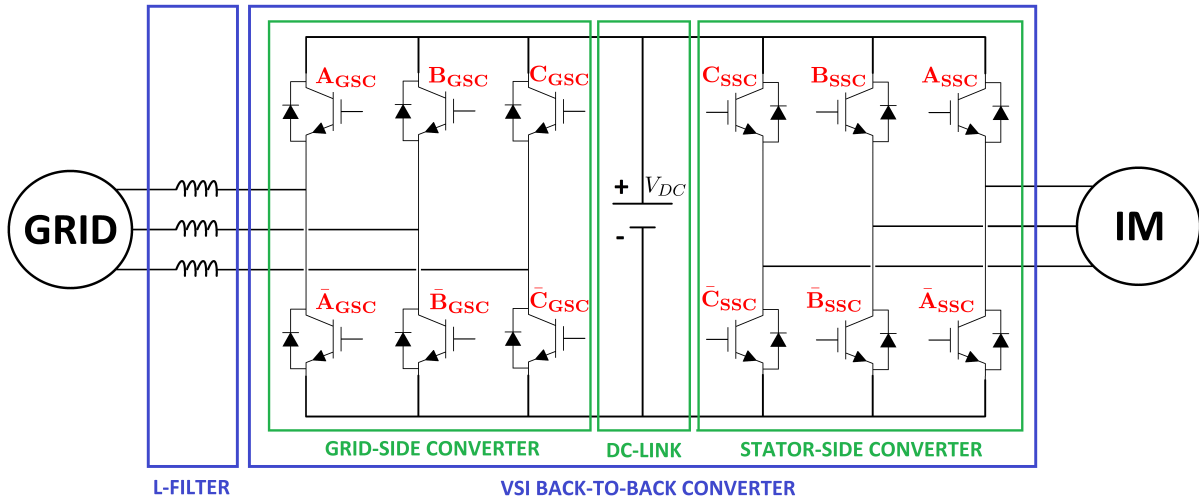


Figure 2.8: Overview of the interface with the grid.

We start by presenting the converter. It is a crucial part of the installation, as the only control variables are the switching signals of the transistors at both ends of this converter. The second part is dedicated to the filter, which reduces the amplitude of the harmonic components injected into the grid. Then, a simple grid representation is given, in order to test the different requirements for grid connection of our installation. Finally, we establish the equations dedicated to the future control of the grid-side converter.

2.3.1 Back-to-back VSI converter

The power electronics interfacing the grid and the induction machine consist in a symmetrical three phase back-to-back converter. Both sides of the converter correspond to *Voltage Source Inverters* (VSIs), whose DC source is the link voltage V_{DC} . The whole flexibility of the total installation required for ancillary services provision relies on this converter :

- **Control of P :** The converter is a variable frequency drive. It allows to control the active power output P of the induction machine. Removing the converter, the installation would stabilise at one operating point, as the pump does not provide any control variable. With the converter, the variable-speed operation of the installation becomes possible, through control of the stator voltage phase and amplitude. The hydro-mechanical power of the pump varies with the speed of the rotor shaft, so does the active power output of the induction machine.
- **Control of Q :** The presence of the DC-link between the two VSIs enables decoupling between both parts of the converter in terms of reactive power Q transmission. The link is indeed seen as an ideal voltage source by the two converters. Therefore, only the active power balance between both sides must be respected. This allows to regulate the reactive power flow at the grid-side converter, independently from the rest of the installation. The active power being fixed, the control of the reactive power permits to chose the power factor. In case of a direct connection to the grid, the induction machine has a fixed power

factor. Moreover, it is usually quite low, as the magnetisation of the machine requires reactive power.

- **Bidirectional power flows :** The layout of the converter being given in Figure 2.8, it uses coupled *Insulated-Gate Bipolar Transistors* (IGBTs) and diodes as bidirectional switches. These are indeed four-quadrant switches, capable of conducting current in both directions, under positive or negative voltage. This property of the switches allows the full reversal of the power flows.

This part about the converter covers its sizing, its control, the determination of the DC-link voltage and some additional considerations about its modeling.

Sizing of the converter

The size of the converters is chosen according to the magnitude of the maximal current flowing through it.

- **Stator-side :** At the stator-side of the asynchronous machine, the converter is sized by the rated apparent power of the machine. The maximal current allowed is indeed equal to the division of this apparent power by the nominal voltage.
- **Grid-side :** At the grid-side, the converter sizing relies on the chosen voltage control strategy. In case the grid operator does not require any voltage support by injection of reactive power, the rated active power sets the limit. This is due to the possibility of producing at unity power factor, minimising the current passing through the converter. In our case, droop control is implemented, with a reactive power band of $0.33P_n$. Hence, the converter is sized for $S = \sqrt{(0.33P_n)^2 + P_n^2} = 1.053P_n$ [VA] and considering the grid nominal voltage.

Control of the converter

The voltage at both terminals of the converter is controlled by the IGBT switches. For each VSI, the commutation of the switches generates a phase-to-phase voltage having two different possible values depending on the switches states : $-V_{DC}$ or $+V_{DC}$. The *Pulse Width Modulation* (PWM) is used to transform the sinusoidal three-phase order into the six pulses controlling the switching of the six IGBTs. These signals are the control variables of the total installation. They are shown in red in Figure 2.8. For each VSI, there exists only three control signals, as A_{SSC} and $\bar{A}_{SSC}$ are linked by the complementary relation. The same applies to phases B and C and to the controlling signals of the grid-side VSI.

Figure 2.9 illustrates the *Simulink* implementation of the stator-side converter control by PWM. We here directly use the *Simulink* PWM block and do not recall the theoretical considerations behind its implementation.

The voltage generated at the terminals of the converter is not exactly equal to its reference. It rather resembles a series of impulses whose duty cycles are proportional to the desired instantaneous voltage amplitude. This is due to the binary nature of the available voltage at the terminals of the DC-link. Nevertheless, the fundamental frequency component of the terminal voltage signal is equal to the sinusoidal reference. The rest of the frequency spectrum simply corresponds to harmonics to be filtered.

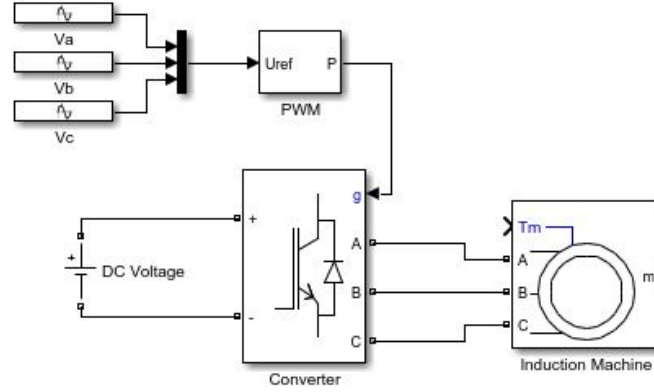


Figure 2.9: Switches control with PWM strategy.

The DC-link voltage

The voltage at the terminals of the DC-link capacitor is a critical value of the system. It determines the available amplitude for the output signals of the VSIs. As this voltage is common for both the converters, it is sized according to the maximal amplitude required either at the grid or at the stator-side. V_{abc} being the phase-to-ground RMS voltage at the converter terminals, we obtain V_{dc} using the following relation 2.50.

$$V_{dc} = 2\sqrt{2}V_{abc}^{max} \quad (2.50)$$

Modeling the converter

As mentioned in a previous paragraph, a PWM block is used to generate the switching order of the VSIs. In order to operate properly, PWM requires the use of a carrier frequency being around 60 times higher than the frequency of the reference signals. In our case, using a machine meant for 50Hz, this leads to a carrier frequency of 3kHz.

Then, the time step of the simulation must be chosen to have a minimum of 20 samples per carrier cycle, giving $\Delta t_{min} = \frac{1}{20 \cdot 3e3} = 33\mu s$. This relatively small time step induces high computational costs.

Moreover, the use of the switches creates harmonics distorting the signals and complicating the analysis of the results. These considerations lead us to the creation of two different models of the converter : the continuous model requiring high computational costs and the averaged, faster model.

- **The continuous model :** The continuous model makes use of two *Universal Bridge* converters from the *Simulink* library. These are linked by a capacitor as DC-link. Choosing the power electronic device to be *IGBT-Diode*, setting the snubber resistance and capacitance to *infinity* and the internal resistance to 0 allows to obtain an ideal behaviour of the switches. This model is shown in Figure 2.10. It is used when simulating real cases requiring high accuracy.
- **The averaged model :** This model consists in short-circuiting the PWM and the switches in order to focus on the equivalent average signals. The reference voltages are in this case used to control voltage sources, as represented in Figure 2.11. This implementation neglects

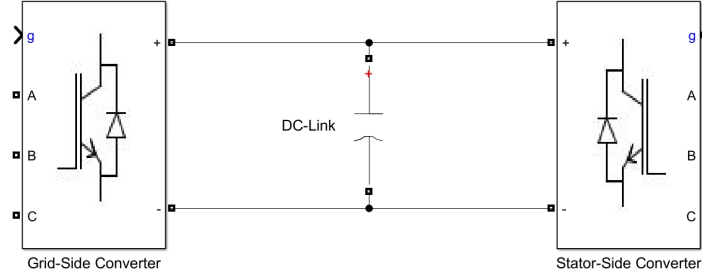


Figure 2.10: The continuous back-to-back VSI converter model.

the dynamics of the switches and all the harmonic content of the signals. The simulation of this model is much faster than the continuous equivalent.

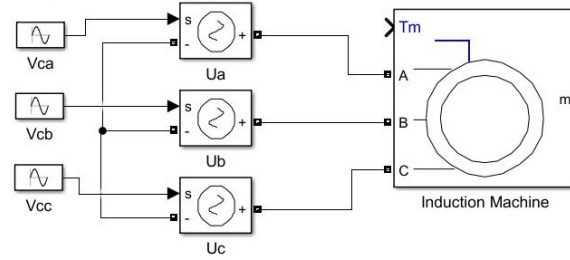


Figure 2.11: Averaged model of the machine-side converter.

As seen in Figure 2.11, the switches are replaced by controlled voltage sources. This example only highlights the connection of the average model to the induction machine, but the same is done on the grid-side. This implies that there is no physical connection at the DC-link between the two parts of the converter. In order to implement the power balance between the two sides and observe the evolution of the DC voltage, we need a model for the DC-link. This is done in Equation 2.51 by considering that the variation of the energy stored in the capacitor corresponds to the active power imbalance between both sides.

$$P_{grid} - P_{stator} = C \frac{d}{dt} \left(V_{dc}^2 \right) \quad (2.51)$$

Equation 2.51 shows that the whole active power is transmitted through the converter as long as the DC-link voltage remains steady.

2.3.2 L-filter

When connecting a new installation to the grid, one of the requirements is to limit the injected *Total Harmonic Distortion* (THD). By its nature, the back-to-back converter is a great harmonics producer. There is then a need to introduce a filter between the grid and the converter. In this study, we simply add a typical L-filter consisting in a three-phase inductance. An L-filter is a simple low-pass filter. Its sizing is a trade-off. The bigger the inductance L_f , the smaller the harmonics injected into the grid. But the size of the inductance is limited by its cost and its associated dynamics. Indeed, the inductance represents an inertia to current variations, as shown in the following equation.

$$\frac{di_L(t)}{dt} = \frac{V_L(t)}{L_f} \quad (2.52)$$

These dynamics considerations are crucial when considering the primary voltage control. A high reactivity of the installation limits the size of L_f .

Recent research has shown that other filters, like LCL filters, may be cheaper and more effective, due to the smaller size of the required inductors [26]. LCL filters however represent sizing challenges due to the resonant frequency.

2.3.3 Grid modeling

The grid is modelled by a *Thévenin* equivalent circuit, made of an ideal three-phase voltage source in series with a three-phase impedance. The *Thévenin* single-phase equivalent is shown in Figure 2.12, within the *Grid* block. The impedance includes a resistance R_g and an inductance L_g . It depends on the *Point of Connection* (PoC) of the installation with the grid. The impedance characteristics are computed from the nominal voltage, the short-circuit apparent power S_{sc} and the X/R ratio characterising the PoC.

$$\begin{cases} R_g = \sqrt{\frac{3V_n^2}{S_{sc}((X/R)^2+1)}} \\ L_g = \frac{(X/R) \cdot R_g}{2\pi f} \end{cases} \quad (2.53)$$

If the PoC is located close to a transformer station, the impedance between the PoC and the transformer is small. This leads to high values of S_{sc} . As the transformer is mostly composed of inductive elements and the line is short, the X/R ratio is in this case high.

On the contrary, an installation connected far from a transformer station leads to a low S_{sc} and a low X/R ratio. The resistive effects of the line are in this case predominant over the inductive effects of the transformer.

2.3.4 Considerations relative to the power control of the grid-side converter

The grid-side converter has a central position within the overall installation. It is indeed responsible for the transmission of the active power flux between the grid and the machine. It also allows to vary the reactive power flow between the grid and the converter. In order to efficiently control the grid-side controller, we need to establish the relation between the voltage output of this converter and the power flowing through it. The voltage is indeed the only control variable of this part of the system. The establishment of this relation is a two step procedure. First, the equations linking the active and reactive powers to the currents are defined. Then, the voltages are linked to the corresponding current components.

Figure 2.12 presents the single-phase equivalent circuit of the line between the grid-side converter and the equivalent voltage source of the grid. The current is considered positive when it flows from the grid to the converter. The phase of the voltage at the PoC is taken as reference, as it is the point where the grid voltage is locally measured.

As the grid-side controller uses a vector control similar to the one of the stator-side, the results are here directly presented in terms of the dq components. The dq transform presented in Appendix C is applied to the three-phase values using here the synchronous angle as reference for the *Park* frame.

Power-to-current relation

The power contracted by the grid operator must be furnished at the PoC. The apparent power expression is given here below by the simple vector expression.

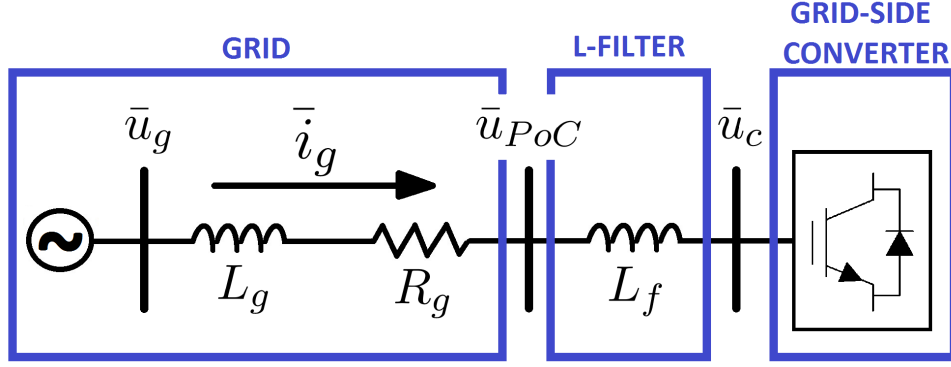


Figure 2.12: Grid to grid-side converter : single-phase equivalent circuit.

$$\bar{S}_{PoC} \triangleq \bar{u}_{PoC} \bar{i}_g^* = u_{PoC} (i_{gd} - j i_{gq}) \quad (2.54)$$

The phase reference being the one of the voltage $\bar{u}_{PoC}$, the d-axis of the reference frame is aligned with the voltage and we have $u_{PoC,d} = u_{PoC}$, $u_{PoC,q} = 0$. Equation 2.54 can then be split into the two Equations 2.55 to highlight the specific influence of the current components on the relative active and reactive powers.

$$\begin{aligned} P_{PoC} &= u_{PoC} i_{gd} \\ Q_{PoC} &= -u_{PoC} i_{gq} \end{aligned} \quad (2.55)$$

Equations 2.55 indeed show that the active power is controlled by the direct-current i_{gd} , while the reactive power is controlled by the quadrature-current i_{gq} .

Voltage-to-current relation

In order to control the current at the desired values, we need to determine the link between these and the voltage $\bar{u}_c = u_{cd} + j u_{cq}$. This is done using the expression of the voltage drop across the filter inductance L_f , which gives, in terms of the phase values :

$$V_{PoC,abc} = V_{c,abc} + L_f \frac{di_{g,abc}}{dt} \quad (2.56)$$

The application of the dq transform to Equation 2.56 gives the following relations, in terms of the direct and quadrature components.

$$\begin{aligned} u_{cd} &= L_f \frac{di_{gd}}{dt} - \omega L_f i_{gq} + u_{PoC,d} \\ u_{cq} &= L_f \frac{di_{gq}}{dt} + \omega L_f i_{gd} \end{aligned} \quad (2.57)$$

ω here simply corresponds to the synchronous frequency f :

$$\omega \triangleq 2\pi f$$

Chapter 3

Control strategy

The provision of ancillary services requires the ability to accurately control the active and reactive power outputs of the installation. Indeed, the supply of FCR is based on the adaptation of the active power P as a function of the frequency difference between the local measurements and the reference synchronous frequency of 50Hz. Regarding the reactive power Q , it needs to be regulated according to the voltage amplitude difference between the measurement at the point of connection and the reference LV value of 400V in order to provide RPR.

The control variables of the system are the switching cycles on both sides of the converter. The stator-side converter acts on the voltages feeding the induction machine. These voltages in turn control the currents, thus the electromagnetic torque and the active power of the installation. On the other side, the converter determines the voltages and currents feeding the grid. These influence both the electrical active and reactive power components. The grid-side converter is then used to ensure the transmission of the active power through the converter, while controlling the reactive power output of the installation. Figure 3.1 shows the respective allocation of the power controls. Even though the positive reference direction for the power flows is taken to be from the grid to the installation, the double-arrows represents the bi-directionality of these flows.

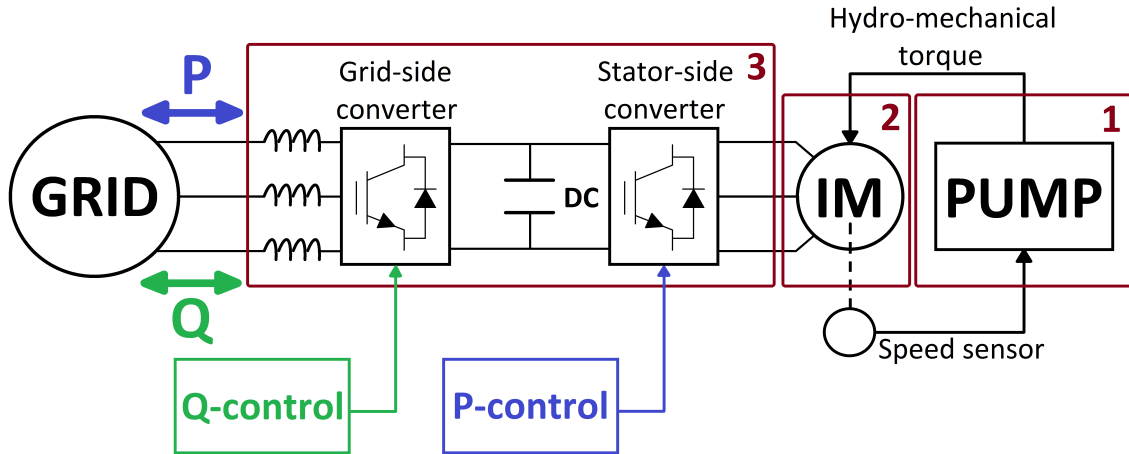


Figure 3.1: Control of the installation.

First of all, Section 3.1 determines the active power control of the stator-side converter. This block takes as inputs the three-phase currents $\{i_{sa}, i_{sb}, i_{sc}\}$ measured at the entrance of the stator of the IM, as well as the grid frequency f recovered from the grid-side converter. It outputs the reference three-phase stator voltages $\{u_{sa}, u_{sb}, u_{sc}\}$ accordingly, controlling the switching of the transistors of the converter after PWM.

Afterwards, the control of the reactive power production is detailed in Section 3.2. This block measures the voltage and currents $\{u_{PoC,a}, u_{PoC,b}, u_{PoC,c}\}$ and $\{i_{ga}, i_{gb}, i_{gc}\}$ of the grid at the PoC, along with the DC-link voltage V_{dc} . It computes the reference three-phase grid voltages $\{u_{ca}, u_{cb}, u_{cc}\}$ accordingly, to be converted by PWM into the switching signals of the grid-side converter.

The main control structure presented hereabove applies to the normal operation of the installation, the grid voltage amplitude remaining within the $\pm 10\%$ range around the nominal amplitude, as explained in Section 1.3. In case of faults leading to large voltage drops, the grid operator requires the installation to be able to overcome a certain FRT curve given in Section 1.3 and showing the time evolution of the grid voltage. Section 3.3 presents the adaptation of the overall control algorithm of the installation in case of such faults. Two distinct methods are developed, in order to adapt to the intrinsic differences between the fault occurring while pumping or turbinning.

3.1 Active power control : stator-side

In this section, we present the thorough analysis of the algorithm behind the control of the stator side converter switching cycles, allowing to vary the active power of the machine as a function of the frequency measurement difference with the nominal frequency of the grid. This part strongly relies on the model of the induction machine developed in Section 2.2. For controlling the pump in its reverse turbine mode, the knowledge of the steady-state behaviour of the pump is also required.

The core of the algorithm is based on vector control, also called *Field-Oriented Control* (FOC). Vector control requires to transform the three-phase speed and time dependent system into a two-coordinate time invariant system. This represents some implementation challenges, but comes with a great deal of advantages, as expressed in [27]. Among those is the fact that the projection on the d and q axes leads to a control structure that is similar to the one of a DC motor. Indeed, the d-component regulates the flux, while the q-component determines the electromagnetic torque. Both of them are constants in the reference frame.

Unlike the classical three-phase control structures, vector control is very efficient both in steady-state and transient operations, as the electrical quantities are regulated instantaneously using the complete model. Hence the high dynamic performances in terms of response times and power conversion. In addition to these considerations, the algorithm used is also adaptable to other types of motors, such as synchronous generators.

Figure 3.2 shows the outline of this active power control algorithm.

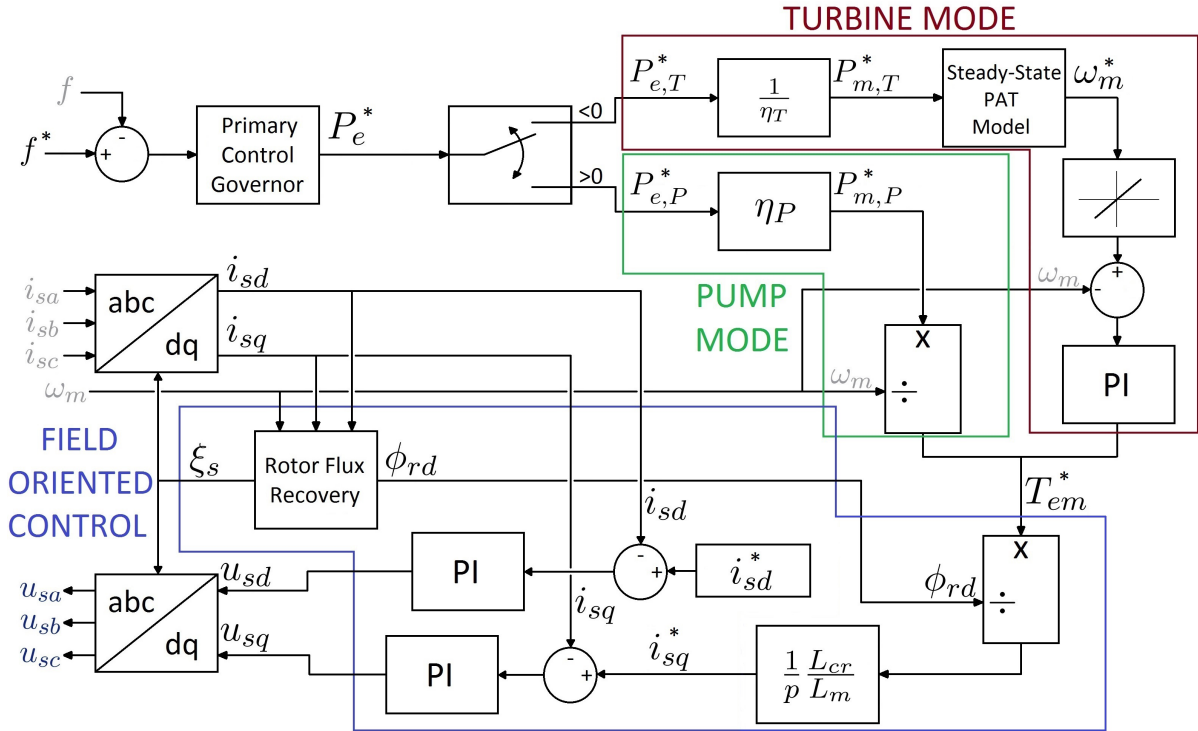


Figure 3.2: Outline of the stator-side active power control.

All the inputs in Figure 3.2 are in gray and correspond to measurements from the installation. We observe the instantaneous grid-side frequency f , the incoming stator currents $\{i_{sa}, i_{sb}, i_{sc}\}$ and the rotor mechanical speed ω_m .

Similarly, the outputs of the control are in blue. They consist in the instantaneous desired

three-phase voltages feeding the stator of the induction machine, $\{u_{sa}, u_{sb}, u_{sc}\}$. These voltages are themselves the inputs to the PWM controlling the switches of the stator side converter.

The body of the control algorithm is then split into main functions, such as the *primary control governor* determining the required electrical power production or consumption by the installation as a function of the grid frequency difference from its reference value. Afterwards, the power order needs to be transformed into an electromagnetic torque reference as this is the only control variable. This transformation varies according to the operating mode of the pump : pump or turbine.

As inner control loop is the field-oriented control. It adapts the stator voltages according to the currents corresponding to the desired torque production.

3.1.1 Field-oriented control

The vector control of the asynchronous generator consists in the decoupled adjusting of the rotor flux by the current i_{sd} and of the electromagnetic torque by the current i_{sq} . The reference frame used for this purpose is the reference *Park* frame aligned with the rotor flux. Figure 3.3 represents the tracking of these *Park* coordinates, from the stator and rotor relative frames. The rotor flux $\phi = \phi_{rd}$ and the stator currents $\{i_{sd}, i_{sq}\}$ are also represented along their respective axis.

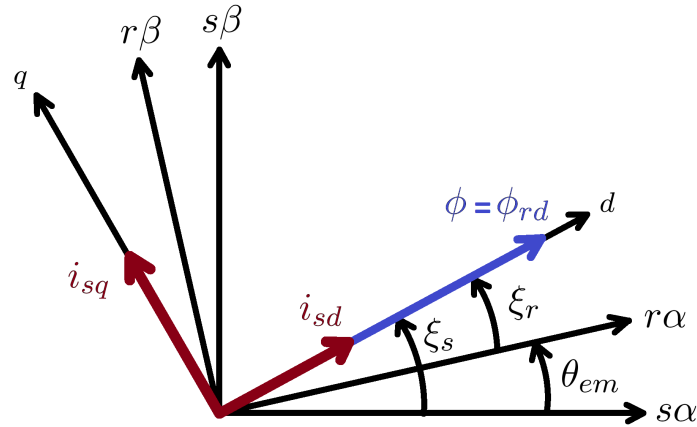


Figure 3.3: Tracking of the stator and rotor dq-axis. Inspired by [23]

The *dq* transform

The dq transform is central to the application of field-oriented control. The complete development of this transform is presented in Appendix C. We here simply recover the final expressions of the direct and inverse transforms applied to the stator currents and voltages.

The *abc-to-dq* block of Figure 3.2 implements the direct dq transform. It uses the following matrix equations 3.1, which transform the measured three-phase stator currents $\{i_{sa}, i_{sb}, i_{sc}\}$ into the two coordinate time-invariant system $\{i_{sd}, i_{sq}\}$.

$$\begin{bmatrix} i_{sd} \\ i_{sq} \end{bmatrix} = \sqrt{\frac{2}{3}} \begin{bmatrix} \cos(\xi_s) & \sin(\xi_s) \\ -\sin(\xi_s) & \cos(\xi_s) \end{bmatrix} \begin{bmatrix} 1 & -\frac{1}{2} & -\frac{1}{2} \\ 0 & \frac{\sqrt{3}}{2} & -\frac{\sqrt{3}}{2} \end{bmatrix} \begin{bmatrix} i_{sa} \\ i_{sb} \\ i_{sc} \end{bmatrix} \quad (3.1)$$

On the other side, the *dq-to-abc* block of Figure 3.2 implements the inverse dq transform. It uses the following matrix equations 3.2, which transform the computed $\{u_{sd}, u_{sq}\}$ back into their three-phase voltage components $\{u_{sa}, u_{sb}, u_{sc}\}$.

$$\begin{bmatrix} u_{sa} \\ u_{sb} \\ u_{sc} \end{bmatrix} = \sqrt{\frac{3}{2}} \begin{bmatrix} \frac{2}{3} & 0 \\ -\frac{1}{3} & \frac{1}{\sqrt{3}} \\ -\frac{1}{3} & -\frac{1}{\sqrt{3}} \end{bmatrix} \begin{bmatrix} \cos(\xi_s) & -\sin(\xi_s) \\ \sin(\xi_s) & \cos(\xi_s) \end{bmatrix} \begin{bmatrix} u_{sd} \\ u_{sq} \end{bmatrix} \quad (3.2)$$

Both the direct and inverse transforms 3.1 and 3.2 make use of the electrical angle ξ_s between the rotating magnetic flux linkage and the stator reference frame. The precise recovery of this angle is crucial to the well-being of the current injection in both direct and quadrature directions. In order to do so, we use the considerations of section 2.2, which are summed up hereunder.

$$\begin{cases} \xi_s(t) = \int_0^t \frac{L_m}{T_r} \frac{isq}{\phi_{rd}} dt + \theta_{em}(t) \\ \frac{\Phi_{rd}(s)}{I_{sd}(s)} = \frac{L_m}{1+T_r \cdot s} \\ \theta_{em}(t) = p(\int_0^t \omega_m(t) dt + \theta_{m,0}) \end{cases} \quad (3.3)$$

Figure 3.4 shows the *simulink* implementation of the reference frame position approximation scheme. Using the same convention as for Figure 3.2, the data recovered from the measures on the motor are shown using the gray background color. These are the phase currents $\{i_a, i_b, i_c\}$ and the rotor mechanical angle θ_m . The single output of this block is the angle ξ_s and is shown in blue.

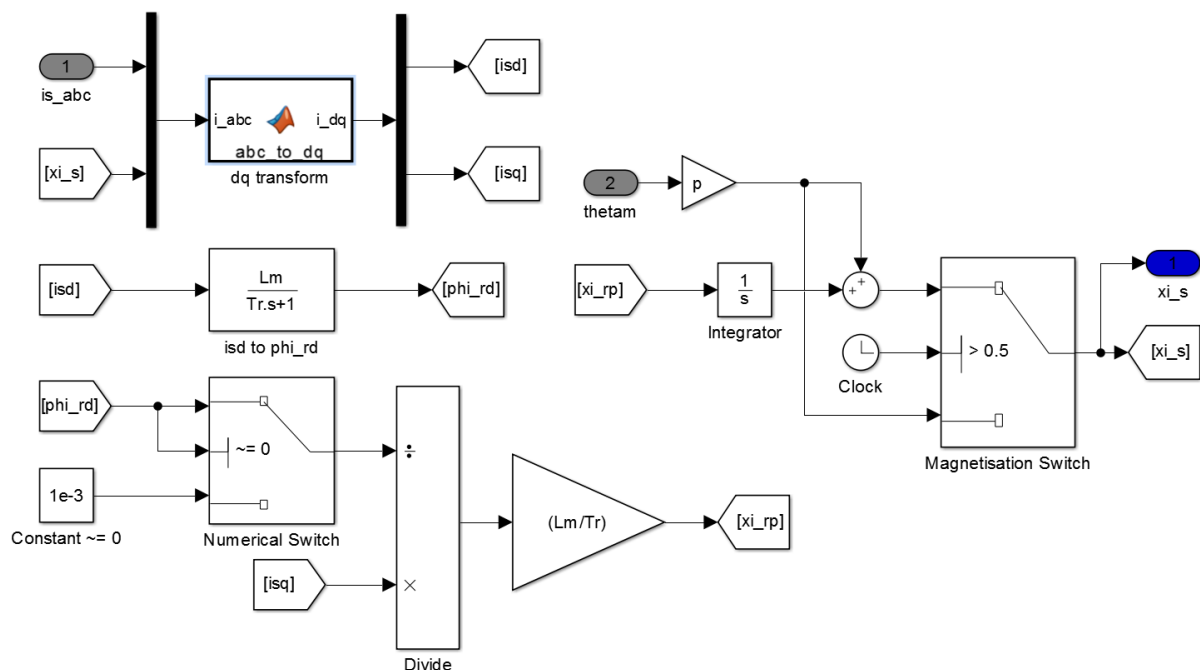


Figure 3.4: *Simulink* implementation of the *Park* frame position approximation.

We observe that this block requires the use of the dq transform to obtain the direct and quadrature currents i_{sd} and i_{sq} , which are both respectively needed for the rotor flux approximation and the rotor speed $\dot{\xi}_r$. As the application of the transformation itself makes use of the stator angle ξ_s ,

we observe that this subsystem presents an algebraic loop. Models with algebraic loops define a system of *Differential Algebraic Equations* (DAEs). From [28], we know that *simulink* does not solve DAEs directly. It solves the algebraic equations numerically, performing iterations at each time step of the ODEs solver. The value recovered for $\xi_s(t)$ is then but an approximation, up to the chosen numerical tolerance.

In Figure 3.4, we use the mechanical angle θ_m to recover $\theta_{em} = p\theta_m$, as it is directly available as output of the chosen induction machine. In practice, we need to precisely determine the initial rotor position and integrate the rotor mechanical speed, as expressed in the third equation of 3.3.

Please note that the implementation of the angle recovery scheme presents two unexpected switches. The first one, on the left of Figure 3.4 is a numerical switch avoiding the undefined result of the division by zero occurring at the first time step, as the machine is not yet magnetised and the rotor flux $\phi_{rd} = 0$.

The second switch, on the right, plays a dual role. On the one hand, it enables the magnetisation of the machine during the first time steps. Indeed, the following relation

$$\xi_s = \theta_{em}$$

applies in the first moments, which implies that the flux linkage is aligned with the rotor reference frame. In this case, the quadrature current component is forced to zero, enabling the rotor flux to settle unconstrained before the start of the torque control.

On the other hand, this second switch also allows to break the algebraic loop during the first moments. It then provides appropriate initialisation for the loop iterative solver when the magnetisation time is over.

The flux ϕ_{rd}

As put forward in Section 2.2 modelling the asynchronous generator, the flux linkage is independently regulated by the direct current i_{sd} . This is shown by the previously demonstrated transfer function between the two.

$$\frac{\Phi_{rd}(s)}{I_{sd}(s)} = \frac{L_m}{1 + T_r \cdot s}$$

This first order transfer function shows that the flux linkage stabilises at a value proportional to the stator d-current, with a rise time depending on the rotor time constant T_r .

For the sake of our control strategy, the flux must remain steady. Our choice is to magnetise the machine using a d-current allowing to reach the maximal power of the machine. Indeed, in case of a pump developing a lower power at the nominal speed of the IM than the nominal power of it, it forces the IM to enter the *field-weakening region* in order to develop its rated power. If magnetised at its nominal conditions, the IM is then unable to reach the rated power. For the reader eager to obtain more information about the sizing of the magnetising current i_{sd} , the thorough development of the maximal power procedure is given in Appendix B.

This part aims at determining the control scheme to be used to regulate the stator d-current at its nominal value. As it is the voltage which can be commanded at the stator controller terminal, we need to remember the d stator voltage equation 2.33, which is reminded hereunder.

$$\begin{aligned}
 u_{sd} = & R_s i_{sd} + \sigma L_{cs} \frac{di_{sd}}{dt} \\
 & + \frac{L_m}{L_{cr}} \frac{d\phi_{rd}}{dt} - (\omega_{em} + \dot{\xi}_r) \sigma L_{cs} i_{sq}
 \end{aligned}
 \tag{3.4}$$

This equation highlights the dependency between the d stator voltage and current. Neglecting the coupling terms of this equation and considering a constant rotor flux ϕ_{rd} , we observe that there exists a first order transfer function $G(s)$ between the two.

$$G(s) = \frac{I_{sd}(s)}{U_{sd}(s)} = \frac{1}{R_s + \sigma L_{cs} \cdot s}$$

In order to regulate the d-current, we insert this equivalent electrical system into the current-regulating closed-loop control shown in Figure 3.5. In this figure, we observe the injection of the coupling terms in red right between the controller $C_C(s)$ and the saturation block. We here neglect the derivative term of ϕ_{rd} due to the previous considerations concerning the transfer function between i_{sd} and ϕ_{rd} . The flux slowly follows the current and can then be assumed constant by comparison to the fast-varying current.

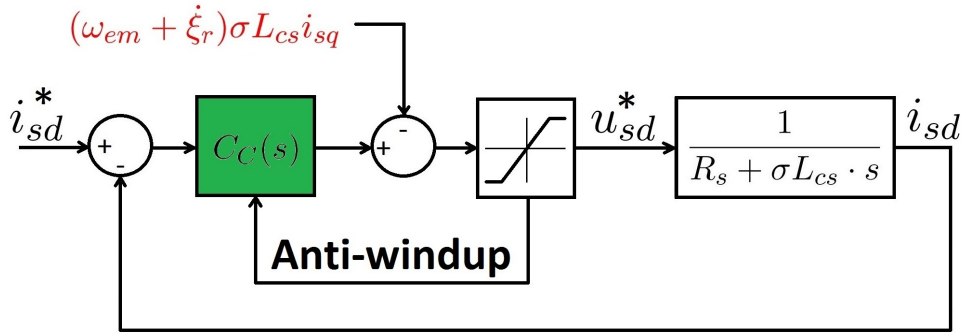


Figure 3.5: d-current regulation loop.

The control scheme of Figure 3.5 also implies the average model assumption : it is supposed that the voltage applied at the equivalent model of the machine exactly corresponds to its reference value u_{sd}^* .

We now need to tune the current controller $C_C(s)$ and determine the saturation limits of the voltage as well as the anti-windup scheme.

Current controller $C_C(s)$: The use of a *Proportional-Integral* (PI) controller allows to both adjust the error sensibility and cancel any possible steady-state error. The theoretical form of the current controller is shown below, k_{pc} being the proportional gain and k_{ic} , the integral gain. The c subscripts stand for *current*.

$$C_C(s) = k_{pc} + \frac{k_{ic}}{s}$$

The tuning method for such PI gains is given in Appendix D. We here simply use the function $[k_{pc}, k_{ic}] = PI_tuning(1, \sigma L_{cs}, R_s, \zeta, \frac{\omega_0}{2\pi})$ returning the required PI gains.

$$k_{ic} = \sigma L_{cs} \omega_0^2 \quad k_{pc} = 2\sigma L_{cs} \zeta \omega_0 - R_s$$

Saturation and anti-windup : By design, the total voltage applied at the terminals of the stator phases cannot exceed the nominal voltage U_{nom} specified by the manufacturer. In the case of a squirrel-cage asynchronous motor, the flux linkage is only produced by the d-component of the stator current, not by a permanent magnet.

In order to ensure the desired magnetisation of the machine, we decided to give priority to the d-component over the q-component. This leads to the following saturation constraint on the stator d-voltage reference.

$$MAX\left(-\sqrt{2}V_{nom}, -\frac{V_{dc}}{2}\right) \leq \sqrt{\frac{2}{3}}u_{sd}^* \leq MIN\left(\sqrt{2}V_{nom}, \frac{V_{dc}}{2}\right) \quad (3.5)$$

Inequalities 3.5 mean that the d-voltage order is limited either by the nominal conditions of the machine, or by the maximum accessible voltage at the terminals of the DC-link. Functions $MIN(x, y)$ and $MAX(x, y)$ respectively return the minimum and the maximum between the two arguments x and y. One could ask why the direct stator voltage is affected by a $\sqrt{\frac{2}{3}}$ factor. This due to the fact that the *dq0 transform* conserves the power but not the amplitude.

In order to avoid integrator windup while the output voltage saturates, we implemented an anti-windup similar to the *clamping* method of the *simulink* $PID(s)$ block. This method is given in [29]. It consists in the cancellation of the integral term as soon as the voltage saturates.

The electromagnetic torque T_{em} :

Once the flux linkage is set to its constant value, the electromagnetic torque T_{em} varies linearly with the stator q-current according to the following equation, previously developed in Section 2.2.

$$T_{em} = p \frac{L_m}{L_{cr}} \phi_{rd} i_{sq}$$

The linearity between the two opens the way to direct control of the torque by simple regulation of the quadrature stator current.

This part aims at determining the control scheme to be used to regulate the stator q-current at its desired value. In order to do so, we need to remember the q stator voltage equation 2.33, which is reminded hereunder.

$$u_{sq} = R_s i_{sq} + \sigma L_{cs} \frac{di_{sq}}{dt} + (\omega_{em} + \dot{\xi}_r) \left(\sigma L_{cs} i_{sd} + \frac{L_m}{L_{cr}} \phi_{rd} \right)$$

(3.6)

This equation highlights the dependency between the q stator voltage and current. Neglecting the coupling terms of this equation, we observe that the equivalent electrical system underlying the quadrature component is identical to the one representing the direct component. Both are characterised by the same transfer function $G(s)$.

$$G(s) = \frac{I_{sq}(s)}{U_{sq}(s)} = \frac{1}{R_s + \sigma L_{cs} \cdot s}$$

In order to regulate the q-current, we insert this equivalent electrical system into the current-regulating closed-loop shown in Figure 3.6. In this figure, we observe the injection of the coupling terms in red right between the controller $C_C(s)$ and the saturation block.

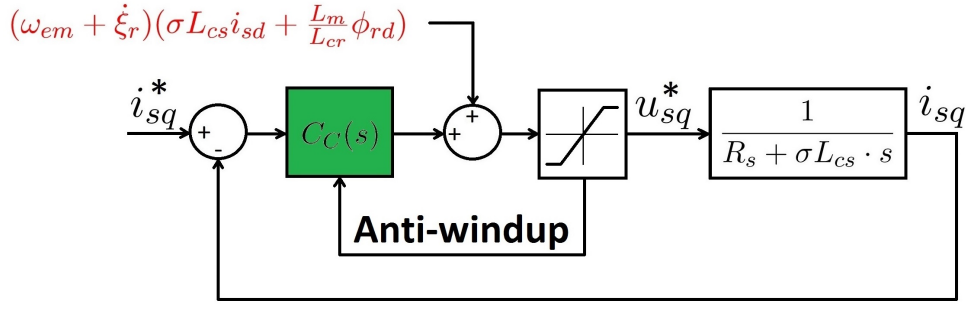


Figure 3.6: q-current regulation loop.

The control scheme of Figure 3.6 also implies the average model assumption : it is supposed that the voltage applied at the equivalent model of the machine exactly corresponds to its reference value u_{sq}^* .

We now need to tune the current controller $C_C(s)$ and determine the saturation limits of the voltage as well as the anti-windup scheme.

Current controller $C_C(s)$: Seeing that the electrical system is characterised by the same transfer function $G(s)$, the tuning of the PI controller leads to the same results as the ones obtained in the case of the d-current control.

The computed proportional and integral q-current gains are recalled hereunder.

$$\boxed{k_i = \sigma L_{cs} \omega_0^2 \quad k_p = 2\sigma L_{cs} \zeta \omega_0 - R_s}$$

Saturation and anti-windup : As the priority is given to the magnetisation of the machine, the quadrature component needs to adapt accordingly. This leads to the following constraint on the stator q-voltage reference.

$$-\sqrt{\left[MIN\left(\sqrt{2}V_{nom}, \frac{V_{dc}}{2}\right)\right]^2 - \left[\sqrt{\frac{2}{3}}u_{sd}^*\right]^2} \leq \sqrt{\frac{2}{3}}u_{sq}^* \leq \sqrt{\left[MIN\left(\sqrt{2}V_{nom}, \frac{V_{dc}}{2}\right)\right]^2 - \left[\sqrt{\frac{2}{3}}u_{sd}^*\right]^2} \quad (3.7)$$

In this case, the saturation of the stator q-voltage relies both on the nominal conditions of the machine and the maximum accessible voltage at the terminals of the DC-link, as well as on the value of the stator d-voltage reference. In case of a variation of the flux, requiring a quick adaptation of the d-current, u_{sd}^* will vary, and so will the saturation limits of the q-voltage u_{sq}^* .

In order to avoid integrator windup while the output voltage saturates, we implemented the same *clamping* anti-windup method as the one used for the direct component control scheme.

3.1.2 Power considerations

Now that the inner loops have been defined in order to ensure field-oriented control, let us analyse the outer power control loop and, more specifically, the mechanisms set up to transform an electrical power reference into some electromagnetic torque to be applied on the rotor shaft.

It is here important to differentiate the method between the pump and turbine modes. Indeed, the power control in pumping mode is both straightforward and very reactive. Unfortunately,

the application of the same control scheme to the turbine mode leads to instability, which we overcome using a specific strategy.

In this subsection, we focus on the electrical power output or input at the stator of the induction machine. In order to exactly control the power at the terminals of the whole installation, knowingly at the point of common coupling, this would require the addition of a higher-level control loop, allowing to compensate for the electrical losses in the converter and the connecting lines. These considerations are irrelevant to our study and will hence not be discussed here.

Pump mode

When pumping, the interactions between the electrical machine and the hydraulic behaviour lead to stability of the overall system. It allows to use the definition of the mechanical power to simply divide it by the measured rotational speed ω_m and convert it into an electro-mechanical torque reference.

$$T_{em,ref} = \frac{P_{m,ref}^P}{\omega_m} \quad (3.8)$$

As the desired power is the electrical one at the stator, we need to find an expression for the electrical efficiency η_P of the machine, which corresponds to the mechanical to electrical power ratio. The difference between the two is due to the electromagnetic losses in the motor. The general expression of the efficiency is given here.

$$\eta_P = \frac{P_m}{P_e} = 1 - \frac{P_{losses,e}}{P_e}$$

In practice, the compensation of the electrical losses in pump mode makes use of a somewhat more complex algorithm leading to more precise results than a simple gain η_P . We start by the following expression of the electrical power in *Park* coordinates and then try to bring out the mechanical power and the losses.

$$P_e = u_{sd}i_{sd} + u_{sq}i_{sq} \quad (3.9)$$

The development of this equation requires the previous stator voltage equations of section 2.2 in their final form 2.33, which we recall hereunder.

$$\begin{cases} u_{sd} = R_s i_{sd} + \sigma L_{cs} \frac{di_{sd}}{dt} + \frac{L_m}{L_{cr}} \frac{d\phi_{rd}}{dt} - (\omega_{em} + \dot{\xi}_r) \sigma L_{cs} i_{sq} \\ u_{sq} = R_s i_{sq} + \sigma L_{cs} \frac{di_{sq}}{dt} + (\omega_{em} + \dot{\xi}_r) (\sigma L_{cs} i_{sd} + \frac{L_m}{L_{cr}} \phi_{rd}) \end{cases} \quad (3.10)$$

Injecting the expressions for u_{sd} and u_{sq} of 3.10 into 3.9 gives

$$\begin{aligned} P_e = & \left[R_s i_{sd} + \sigma L_{cs} \frac{di_{sd}}{dt} + \frac{L_m}{L_{cr}} \frac{d\phi_{rd}}{dt} - (\omega_{em} + \dot{\xi}_r) \sigma L_{cs} i_{sq} \right] i_{sd} \\ & + \left[R_s i_{sq} + \sigma L_{cs} \frac{di_{sq}}{dt} + (\omega_{em} + \dot{\xi}_r) (\sigma L_{cs} i_{sd} + \frac{L_m}{L_{cr}} \phi_{rd}) \right] i_{sq} \end{aligned}$$

As the aim is to produce or consume a precise quantity of electrical power in steady-state, we neglect all the time derivatives present in the previous power expression.

We also observe the respective cancellation of both terms proportional to the product of i_{sd} and i_{sq} . This leads to the following simplified equation.

$$P_e = R_s (i_{sd}^2 + i_{sq}^2) + (\omega_{em} + \dot{\xi}_r) \frac{L_m}{L_{cr}} \phi_{rd} i_{sq} \quad (3.11)$$

The general purpose of this development being to find the link between the electrical power P_e and the mechanical power $P_m = T_{em}\omega_m$, we need to identify the expression of P_m in equation 3.11. This requires the manipulation of the electromagnetic torque expression.

$$T_{em} = p \frac{L_m}{L_{cr}} \phi_{rd} i_{sq} \quad \Leftrightarrow \quad \frac{L_m}{L_{cr}} \phi_{rd} i_{sq} = \frac{T_{em}}{p} = \frac{P_m}{p\omega_m} \quad (3.12)$$

Injecting equation 3.12 into 3.11, we get the final form 3.13 of the expression linking the mechanical power to the electrical one. The first part of the losses are the stator losses due to the stator resistor and the second part are the rotor copper losses.

$$P_e = P_m + \left[R_s(i_{sd}^2 + i_{sq}^2) + \dot{\xi}_r \frac{T_{em}}{p} \right] \quad (3.13)$$

From the previous equation, we extract the following formula transforming the electrical power reference at the stator into the corresponding mechanical power reference.

$$P_{m,P}^* = P_{e,P}^* - \left[R_s(i_{sd}^2 + i_{sq}^2) + \dot{\xi}_r \frac{L_m}{L_{cr}} \phi_{rd} i_{sq} \right]$$

Turbine mode

In turbine mode, the interactions between the electrical machine and the hydraulic transient behaviour lead to instability of the system when applying the same control algorithm as in pump mode. The cause of the instability is the hydraulic torque transient characterised by a much larger time constant than the electrical one. The instability only occurs in one part of the pump working range. Indeed, instability occurs for high negative flow rates ϕ .

One could imagine only working in the stable range and keep the same control algorithm as in the pump mode. Reducing the working range widely lowers the maximum active power which can be produced by the PaT. Moreover, the stable working range corresponds to low efficiencies, reducing the overall efficiency of the installation. For the reader eager to get more information about the instability, its thorough study is conducted in Appendix A. We here focus on the chosen alternative control solution, which requires further insight into the installation model.

Our choice is to use a speed controller to adapt the electromagnetic torque to the desired rotational speed. This method is indeed stable, but it requires another step, mapping the desired power to the corresponding rotational speed of the machine. This can be done using the hydraulic model of the pump. This part is highly dependent of the hydraulic characteristic curves.

Equations 3.14 given below presents the equation system to solve in order to find the turbinning power-to-speed relation. This system comes from the characteristic curves $\psi_t(\phi)$ and $\pi_t(\phi)$ and the definition of the dimensionless variables ψ and π .

$$\begin{cases} \psi(\phi) \frac{n^2 D_t^2}{g} = H \\ \pi(\phi) \rho n^3 D_t^5 = P \end{cases} \quad (3.14)$$

The system is composed of four variables : ϕ , n , H , P . Considering steady state condition, H is equal to the available head at turbine entrance H_{res} . We here have two relations for three unknowns, allowing to find a relation between the speed n and the power P .

There remains however one important limitation : as the mapping between the speed and the power neglects the hydraulic transient, the instantaneousness of the control is lost. Only the steady-state power output is controlled.

It is also important to notice the first-order filter added to smoothen the speed reference transition rates. Smoother speed transitions allow the avoidance of high current peaks impacting the quality of the active power output. The comparison of the control with the filter and without filter is carried out in the *Negundo* study case.

The speed controller : In order to design the speed controller, we need to find the transfer function between the rotational speed ω_m and the stator q-current i_{sq} . The evolution of the mechanical system is given by equation

$$J \frac{d\omega_m}{dt} = T_{em} - F\omega_m - T_m \quad (3.15)$$

allowing to highlight the relation between ω_m and T_{em} . As the electromagnetic torque linearly depends on i_{sq} , we simply extract the transfer function

$$G(s) = \frac{\Omega_m(s)}{T_{em}(s)} = \frac{1}{F + J \cdot s} \quad (3.16)$$

which then only needs to be multiplied by the corresponding gain. The overall speed regulation loop is shown in Figure 3.7. This figure highlights the nesting of the inner q-current control loop within the speed control loop.

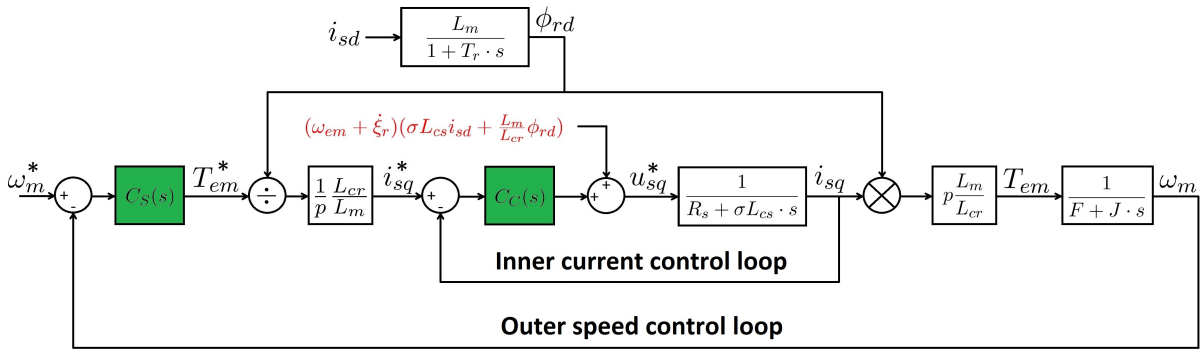


Figure 3.7: Overall speed regulation loop.

The inner loop was analysed in the previous subsection. In order to separately tune the inner and outer controllers, the choice of natural frequencies is crucial. The time constant of the outer loop must be sufficiently larger than the inner one to ensure the decoupling of both controls. Indeed, from the much faster inner electrical loop viewpoint, the outer mechanical system is considered as constant at its timescale.

On the other hand, the outer loops needs to be much slower in order to consider the inner loop as ideal, such that $i_{sq}^* \simeq i_{sq}$. This leads to the equivalent rotational speed regulation loop of Figure 3.8. The saturation block and anti-windup loop are also shown in this figure.

The choice for the speed controller $C_S(s)$ is again a PI, tuned according to the method of Appendix D. We simply adapt the parameters used in function $[k_{ps}, k_{is}] = PI_tuning(1, J, F, \zeta, \frac{\omega_0}{2\pi})$ and recover the PI gains.

$$k_{is} = J\omega_0^2 \quad k_{ps} = 2J\zeta\omega_0 - F$$

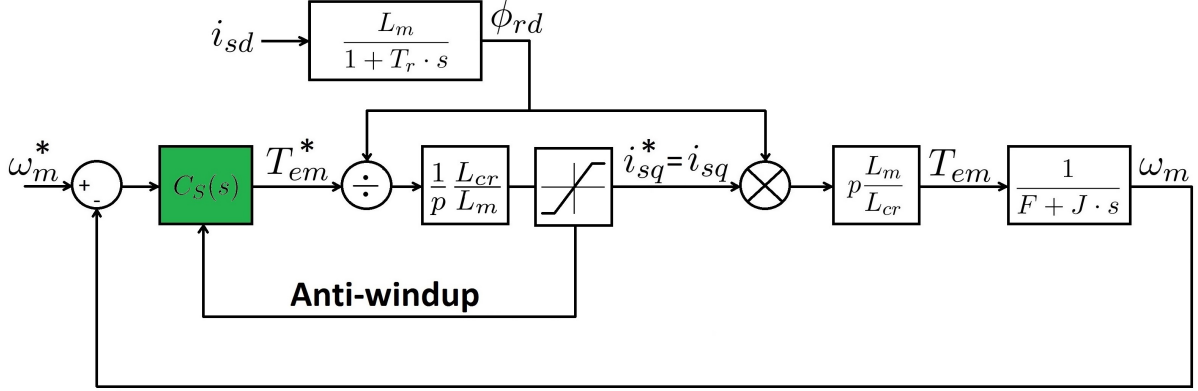


Figure 3.8: Equivalent rotational speed regulation loop.

The saturation on the reference quadrature stator current, i_{sq}^* allows to constrain the output current reference in order to avoid the maximum current allowed by the converter and the induction machine. As the saturation is applied on the reference signal, the controller is tuned with a unity damping in order to avoid overshoot and therefore a signal larger than its saturation.

$$-\sqrt{i_{s,max}^2 - i_{sd}^2} \leq i_{sq}^* \leq \sqrt{i_{s,max}^2 - i_{sd}^2} \quad (3.17)$$

In order to avoid integrator windup while the reference current saturates, we implemented the same *clamping* anti-windup method as the one used for the direct and quadrature currents control schemes.

Efficiency η_T : In case of the pump being used in reversed mode, the instantaneousness of the control being lost, there is no point applying the instantaneous electrical losses compensation algorithm developed for the pump mode. We instead determine a fixed efficiency

$$\eta_T = \frac{P_e}{P_{hydro}} = 1 - \frac{P_{losses,e} + P_{losses,m}}{P_{hydro}}$$

which is applicable to the considered power range. In the case of a field implementation requiring high precision, the little deviations from the reference power will be coupled to the losses in the converter and power lines and cancelled by the addition of the outer controller adapting the exchanges at the point of common coupling.

3.1.3 Primary control governor

The grid codes relative to the provision of *frequency containment reserve* (FCR) are detailed in section 1. These considerations lead to the application of automatic droop control of the active electrical power, as a function of the frequency difference to the synchronous one, knowingly 50Hz in Belgium. Figure 3.9 represents three possible applications for the installation, each of them implementing symmetrical R1-200 mHz. As the control allows the variation of the power in both directions, the two asymmetrical cases R1-up and R1-down are disregarded.

Please remember that we are here in the case of an *asset with limited energy reservoirs*, which implies that the installation should be activated as long as the frequency deviation persists, unless its energy reservoir is exhausted in either direction [9].

Case 1 : turbine-pump

In the first case of Figure 3.9, the installation is fully dedicated to FCR and the machine remains still while the frequency measurement is close enough to 50Hz. Once the frequency difference

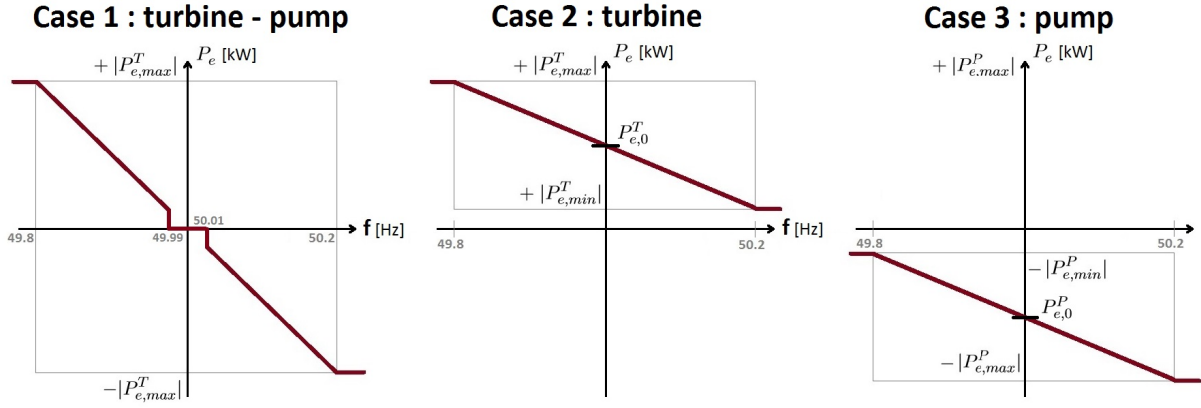


Figure 3.9: Active power droop control. Inspired by [9].

exceeds the dead-band of ± 10 mHz, the machine starts accordingly. The dead-band avoids the start of the machine for negligible power production. In case of a positive deviation, the installation is used as a load and water is pumped toward the higher reservoir. In case of a negative deviation, it is used as a generator. The capacity band here depends on the maximum power output in turbine mode $|P_{e,max}^T|$, as the power produced in turbine mode is lower than the one developed in pump mode, under the same conditions. Equations 3.18 represent the output power function of this case.

$$\begin{cases} P_e = +|P_{e,max}^T| & , f \leq 49.8 \text{ Hz} \\ P_e = \alpha_1 \Delta f & , 49.8 \text{ Hz} \leq f \leq 49.99 \text{ Hz} \\ P_e = 0 & , 49.99 \text{ Hz} \leq f \leq 50.01 \text{ Hz} \\ P_e = -\alpha_1 \Delta f & , 50.01 \text{ Hz} \leq f \leq 50.2 \text{ Hz} \\ P_e = -|P_{e,max}^T| & , f \geq 50.2 \text{ Hz} \end{cases} \quad (3.18)$$

The droop factor α_1 is defined hereafter using the power and the frequency deviations limits.

$$\alpha_1 = \frac{0 - |P_{e,max}^T|}{50.0 - 49.8} = -5 |P_{e,max}^T| \left[\frac{\text{kW}}{\text{Hz}} \right]$$

The frequency deviation Δf is simply the difference between the reference synchronous frequency of 50 Hz and the measured frequency f .

$$\Delta f = (f^* - f) = (50 - f) \text{ [Hz]}$$

Case 2 : turbine

The second and third cases are similar to each other, except that the second uses the installation as a pump and the third, as a turbine. In both modes, the control algorithm allows to reach any desired power, the only limitations being the operational limits of the induction machine and the pump. Both of these cases superimpose the provision of primary reserve on top of a certain scheduled power $P_{e,0} \neq 0$ used for price arbitrage. The available FCR capacity band directly relies on $P_{e,0}$. As we here mainly mean to provide FCR, we give $P_{e,0}$ the median value between the maximum and minimum available power. In these cases, we neglect the allowed dead-band around the nominal frequency.

$$\begin{cases} P_e = +|P_{e,max}^T| & , f \leq 49.8 \text{ Hz} \\ P_e = P_{e,0}^T + \alpha_2 \Delta f & , 49.8 \text{ Hz} \leq f \leq 50.2 \text{ Hz} \\ P_e = +|P_{e,min}^T| & , f \geq 50.2 \text{ Hz} \end{cases} \quad (3.19)$$

Equations 3.19 represent the output power function of this case. The droop factor is now given by

$$\alpha_2 = \frac{|P_{e,min}^T| - |P_{e,max}^T|}{50.2 - 49.8}$$

Case 3 : pump

In pump mode, only the operating ranges vary from the turbine mode. We then have the same structure for the droop control equations and the definition of its factor.

$$\begin{cases} P_e = -|P_{e,min}^P| & , f \leq 49.8Hz \\ P_e = P_{e,0}^P + \alpha_3 \Delta f & , 49.8Hz \leq f \leq 50.2Hz \\ P_e = -|P_{e,max}^P| & , f \geq 50.2Hz \end{cases} \quad (3.20)$$

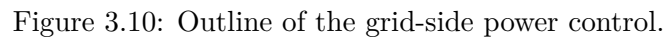
$$\alpha_3 = \frac{-|P_{e,max}^P| + |P_{e,min}^P|}{50.2 - 49.8}$$

Frequency measurement

The droop control implementation requires the precise knowledge of the grid frequency. This information is recovered by the grid-side controlling block, which makes use of a *Phase Locked Loop* (PLL) to obtain the stator electrical angle and apply the *dq transform*.

In this section, we present the thorough analysis of the algorithm behind the control of the grid-side converter switching cycles, allowing to vary the reactive power injection in the grid as a function of the voltage measurement difference with the nominal voltage of the grid. This part strongly relies on the model of the interface with the grid in Section 2.3.

For the same reasons explained in Section 3.1 about the stator-side control, the controller presented here also makes use of vector control, which applies the dq transform to operate on constant vector components instead of time-varying three-phase signals. The reference frame considered in this case is a synchronously rotating dq frame, aligned with the voltage at the point of connection $\bar{u}_{PoC}$.



The subscripts g , PoC and c respectively stand for *grid*, *Point of Connection* and *converter*, while the three phases are represented by the subscripts a , b and c .

The block diagram of Figure 3.10 is divided into three parts. The inner part is the vector control, adapting the voltage components to the desired currents. The outer loops set the current references as functions of the required powers. On one hand, the V_{dc} controller ensures the transmission of the active power P by controlling the DC-link voltage at a constant value. On the other hand, the Q controller uses a droop control on the voltage at the PoC to determine the reactive power reference.

3.2.1 Vector control

The vector control of the grid-side converter consists in the decoupled adjusting of the outgoing grid current components i_{gd} and i_{gq} as functions of the converter voltages $\{v_{ca}, v_{cb}, v_{cc}\}$. Similarly to the procedure followed in Section 3.1 for the field-oriented control of the induction machine, this part covers the projection of the three-phase values on the rotating reference frame, as well as the tuning of the PI controller and the analysis of the saturations and anti-windup scheme.

Reference frame considerations

The PoC is the connection point of the installation with the grid. It is the exact position where the active and reactive powers are exchanged with the grid. It seems the reasonable place to measure the currents and voltages, setting the reference frame used for the whole control of the grid-side converter.

A *Phase Locked Loop* (PLL) is used to track down the angular position ξ_{PoC} of the rotating $\bar{V}^{PoC}$ voltage. This block also returns the instantaneous amplitude of the voltage V^{PoC} , as well as the grid frequency f . f simply corresponds to the derivative of ξ_{PoC} , to a factor 2π . The practical implementation makes use of the *PLL (3ph)* block from the *Simulink Simscape Power System* toolbox.

The *abc-to-dq* block of Figure 3.10 implements the direct dq transform developed in Appendix C. It uses the following matrix equations 3.21, which transform the measured three-phase currents $\{i_{ga}, i_{gb}, i_{gc}\}$ into the two coordinate time-invariant system $\{i_{gd}, i_{gq}\}$.

$$\begin{bmatrix} i_{gd} \\ i_{gq} \end{bmatrix} = \sqrt{\frac{2}{3}} \begin{bmatrix} \cos(\xi_{PoC}) & \sin(\xi_{PoC}) \\ -\sin(\xi_{PoC}) & \cos(\xi_{PoC}) \end{bmatrix} \begin{bmatrix} 1 & -\frac{1}{2} & -\frac{1}{2} \\ 0 & \frac{\sqrt{3}}{2} & -\frac{\sqrt{3}}{2} \end{bmatrix} \begin{bmatrix} i_{ga} \\ i_{gb} \\ i_{gc} \end{bmatrix} \quad (3.21)$$

On the other side, the *dq-to-abc* block of Figure 3.10 implements the inverse dq transform. It uses the following matrix equations 3.22, which transform the computed converter voltages $\{u_{cd}, u_{cq}\}$ back into their three-phase voltage components $\{u_{ca}, u_{cb}, u_{cc}\}$.

$$\begin{bmatrix} u_{ca} \\ u_{cb} \\ u_{cc} \end{bmatrix} = \sqrt{\frac{3}{2}} \begin{bmatrix} \frac{2}{3} & 0 \\ -\frac{1}{3} & \frac{1}{\sqrt{3}} \\ -\frac{1}{3} & -\frac{1}{\sqrt{3}} \end{bmatrix} \begin{bmatrix} \cos(\xi_{PoC}) & -\sin(\xi_{PoC}) \\ \sin(\xi_{PoC}) & \cos(\xi_{PoC}) \end{bmatrix} \begin{bmatrix} u_{cd} \\ u_{cq} \end{bmatrix} \quad (3.22)$$

The PI controller

Once the three-phase variables are transformed into their dq equivalents, we need to establish the inner control scheme of the currents. This requires the knowledge of the equivalent electrical

system at the output of the grid-side converter. Established in Section 2.3, the equations linking the grid current $\bar{i}_g$ and the converter voltage $\bar{v}_c$ are recalled hereunder.

$$\begin{cases} u_{cd} = L_f \frac{di_{gd}}{dt} - \left[\dot{\xi}_{PoC} L_f i_{gq} - u_{PoC,d} \right] \\ u_{cq} = L_f \frac{di_{gq}}{dt} + \dot{\xi}_{PoC} L_f i_{gd} \end{cases} \quad (3.23)$$

In red are the coupling terms and the measured voltage $u_{PoC,d} = u_{PoC}$ which is considered as an external input, independent from the applied voltage $\bar{u}_c$. Neglecting these additional terms, we observe that the direct and quadrature components are decoupled, allowing u_{cd} to independently control i_{gd} and u_{cq} to control i_{gq} in the same manner. Furthermore, the two first-order relations are the same, leading to a single transfer function $G(s)$ representing the electrical system.

$$G(s) = \frac{I_{g,dq}(s)}{U_{c,dq}(s)} = \frac{1}{L_f \cdot s} \quad (3.24)$$

Based on the understanding of the equivalent electrical system, the direct and quadrature currents control loops are built and shown in Figures 3.11 and 3.12. For the same reasons detailed in Section 3.1 about the stator-side controller, we here again implement a PI current controller. Similar saturation and anti-windup methods are added, to take into account the operating limits of the converter.

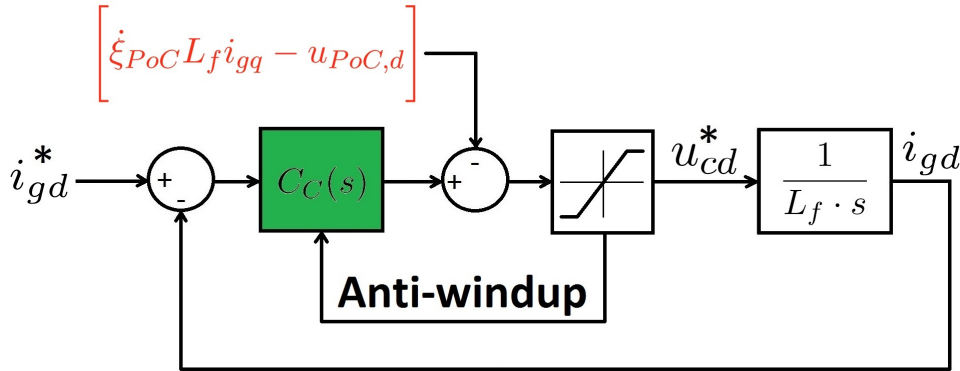


Figure 3.11: d-current regulation loop.

These two control loops are very similar, except for the coupling terms added to the output of the proportional and integral gains.

The tuning of the proportional and integral gains of the current control function $C_C(s)$ again uses the method method given in Appendix D. We here simply use the function $[k_{pc}, k_{ic}] = PI_tuning(1, L_f, 0, \zeta, \frac{\omega_0}{2\pi})$ returning the required PI gains as functions of the desired cut-off frequency ω_0 and damping ζ .

$$\begin{cases} k_{ic} = L_f \omega_0^2 \\ k_{pc} = 2L_f \zeta \omega_0 \end{cases}$$

Please note that, as $G(s)$ is the same for both direct and quadrature systems, the controller functions $C_C(s)$ are also identical. The damping ζ must be chosen greater than one in order to avoid overshoots. Indeed, the currents must never exceed their reference, as it might damage the converter switches.

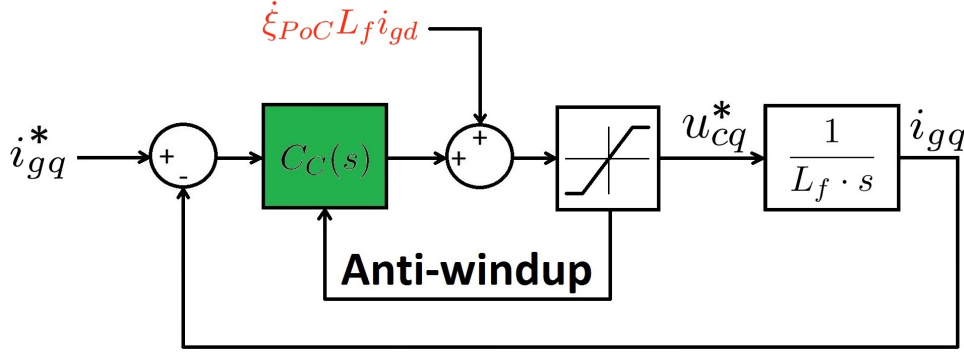


Figure 3.12: q-current regulation loop.

Saturation and anti-windup

Saturation blocks are crucial to the model. If not saturated at the maximum and minimum available voltages of the DC-link, the amplitude of the voltages u_{cd} and u_{cq} can exceed this limit and lead to a non-linear behaviour of the PWM block. The reference signals fed into the pulse width modulator are in this case greater than the PWM carrier and the system becomes out of control.

As the operating range of the DC-link voltage is split into the two voltage components, the chosen strategy is to prioritise the quadrature component over its direct equivalent. This allows to provide a maximum reactive power support of the grid in case of a voltage drop. The following limits for u_{cq} are obtained.

$$-\frac{V_{dc}}{2} \leq \sqrt{\frac{2}{3}} u_{cq} \leq \frac{V_{dc}}{2} \quad (3.25)$$

Again, the factor $\sqrt{\frac{2}{3}}$ is required to compensate for the fact that the chosen dq transform conserves the power, but not the amplitudes of the voltages and currents. On the other side, the presence of the factor $\frac{1}{2}$ multiplying the DC voltage is due to the position of the switches. The full DC voltage is applied between two phases. It then needs to be divided by two to represent the voltage applied to a single phase of the stator.

The remaining span of the voltage is given to the direct component, according to expression 3.26.

$$-\sqrt{\left(\frac{V_{dc}}{2}\right)^2 - \left(\sqrt{\frac{2}{3}} u_{cq}\right)^2} \leq \sqrt{\frac{2}{3}} u_{cd} \leq \sqrt{\left(\frac{V_{dc}}{2}\right)^2 - \left(\sqrt{\frac{2}{3}} u_{cq}\right)^2} \quad (3.26)$$

In terms of anti-windup, we implement the same *clamping* method as the one used for all the controllers in Section 3.1.

3.2.2 DC-link voltage control

The active power P_{stator} production of the installation is controlled at the stator-side. The grid-side converter however plays an important role in the transmission of this active power through the whole converter and to the grid. The active power balance of the installation is shown in Figure 3.13 and expressed by Equation 3.27.

$$P_{stator} + P_{capacitor} = P_{grid} \quad \Leftrightarrow \quad P_{grid} - P_{stator} = C \frac{d}{dt} \left(V_{dc}^2 \right) \quad (3.27)$$

Equation 3.27 highlights the fact that the full active power is transmitted through the converter only if the DC-link voltage is maintained constant. A constant voltage at the terminals of the capacitance indeed means a constant energy stored inside it. In order to do so, we need to find a way to control the voltage V_{dc} . This is done by remembering that the active power flowing from the grid is proportional to the direct-current i_{gd} .

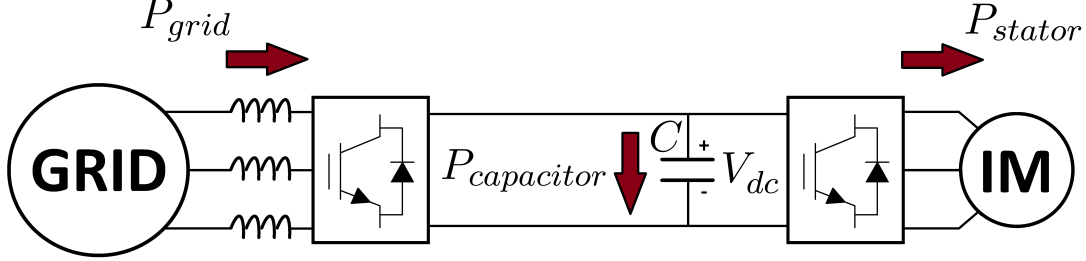


Figure 3.13: Overall active power balance.

Using the grid active power expression developed in Section 2.3, Equation 3.27 becomes

$$P_{stator} + C \frac{d}{dt} \left(V_{dc}^2 \right) = u_{PoC} i_{gd} \quad (3.28)$$

Equation 3.28 allows to recover the transfer function $H(s)$ linking i_{gd} to V_{dc}^2 .

$$H(s) = \frac{V_{dc}^2(s)}{I_{gd}(s)} = \frac{u_{PoC}}{C \cdot s} \quad (3.29)$$

We here use V_{dc}^2 instead of V_{dc} , as it allows to tune the controller on the basis of the simple first-order transfer function $H(s)$.

The overall DC voltage regulation loop is shown in Figure 3.14. This figure highlights the nesting of the inner d-current control loop within the outer voltage loop.

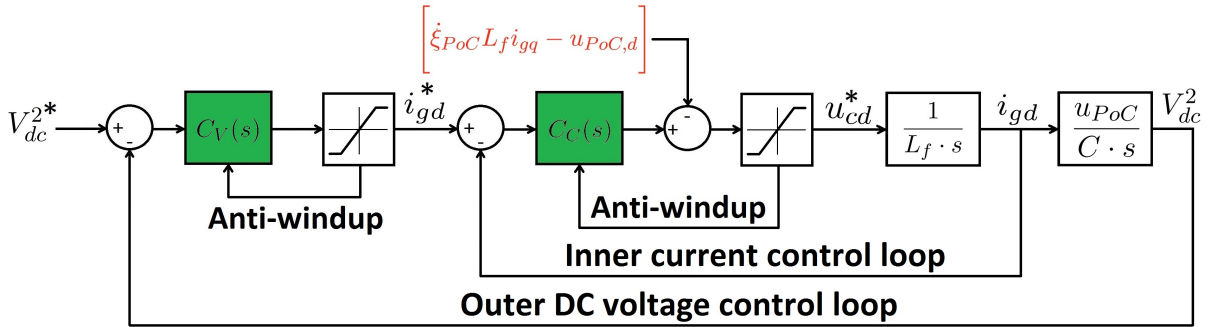


Figure 3.14: Overall DC voltage regulation loop.

The inner loop was analysed in the previous subsection. Assuming the efficient control of the d-current and that the time constant of the outer loop is chosen sufficiently larger than the inner one, we consider the current as equal to its reference, as seen from the V_{dc} controller viewpoint. This allows to simplify the previous system, which becomes the one of Figure 3.15.

$H(s)$ being a first-order transfer function, the application of a PI controller is again adequate. The tuning of the PI gains of the function $C_V(s)$ again require the method of Appendix D. We simply use the function $[k_{pc}, k_{ic}] = PI_tuning(u_{PoC}, C, 0, \zeta, \frac{\omega_0}{2\pi})$ returning the required PI gains

a functions of the desired cut-off frequency ω_0 and damping factor ζ . A typical value for the cut-off frequency is ten times smaller than the one of the current controller. This allows to verify the previous hypothesis.

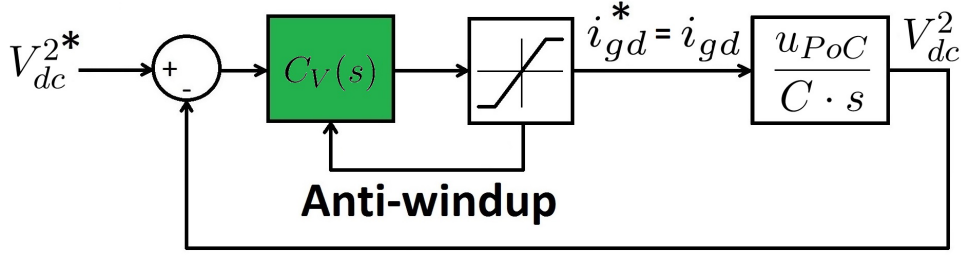


Figure 3.15: Equivalent V_{dc} regulation loop.

Saturation and anti-windup

The converter uses switches which are highly sensitive to current overload. In order to limit the total current reference at the maximal acceptable threshold $I_{g,max}$, a saturation block is added. Priority is still given to the quadrature component. This lead to the following inequalities setting the limits of the direct-current reference.

$$-\sqrt{(I_{g,max})^2 - \left(\sqrt{\frac{2}{3}}i_{gq}^*\right)^2} \leq \sqrt{\frac{2}{3}}i_{gd}^* \leq \sqrt{(I_{g,max})^2 - \left(\sqrt{\frac{2}{3}}i_{gq}^*\right)^2} \quad (3.30)$$

In terms of anti-windup, we still implement the same *clamping* method as the one used for all the controllers in Section 3.1.

3.2.3 Reactive power control

The control variable of the grid-side converter is the reactive power Q . The presence of the back-to-back converter allows the total decoupling between the reactive power flowing from the grid-side and the reactive power at the machine-side. Hence, the installation is able to produce or consume power at any power factor.

The aim of the reactive power flexibility is to provide support to the grid voltage in case of a deviation from its nominal value. The chosen strategy in Section 1.2 is the droop control, permitting to supply the grid operator with voltage containment reserves. Droop control means that the reactive power injected in the grid is linear and proportional to the measured voltage deviation from its nominal value at the point of connection.

$$Q^* = \alpha_v \Delta u_{PoC}$$

The limits of the response to the deviation are determined by the maximal ability to deliver 33% of the installation nominal active power when there is a voltage deviation of 10%. The proportional gain α_v is sized accordingly.

$$\alpha_v = \frac{0.33P_n}{0.1U_{PoC,n}}$$

The reactive power control is meant for voltage deviations below and above the nominal voltage. The operating range of Q is then $[-0.33P_n, +0.33P_n]$. The Q control block diagram is recalled in Figure 3.16. In this figure, the first saturation block limits the reference Q^* to the defined operating range. In case of larger voltage drops than 10%, this block is crucial to prevent the

q-current from saturating and the complete blocking of the active power transmission.

Using the relation between the reactive power and the quadrature-current developed in Section 2.3, the current reference i_{gq}^* is obtained from Equation 3.31.

$$i_{gq}^* = \frac{-Q^*}{u_{PoC}} \quad (3.31)$$

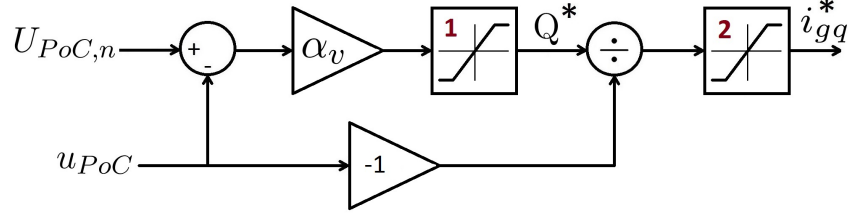


Figure 3.16: Q outer regulation block diagram.

Saturation

As in the case of the direct current reference, the quadrature component must be limited to a maximal value, expressed by the following inequalities.

$$-I_{g,max} \leq \sqrt{\frac{2}{3}} i_{gq}^* \leq I_{g,max} \quad (3.32)$$

3.3 Fault Ride-Through : adaptation of the control strategy

As explained in Section 1.3 and recalled in the introduction to the present chapter, our installation is also required to stay connected to the grid, even in case of faults provoking large voltage dips. The FRT curve applicable to our system was previously given in Figure 1.3. During faulted behaviour, the main requirement becomes the overcoming of the fault. The presence of the back-to-back converter makes it possible, as it partly decouples the machine from the grid. In this case, the FCR and RPR provision is altered and the remaining flexibility of production is allocated to reactive power production as a priority.

The avoidance of tripping the electrical machine allows a quicker recovery of the normal conditions after the fault clearance. It however presents some additional challenges for the installation, which needs to adapt its overall control strategy.

Indeed, the dropping of the voltage at the PoC reduces the capacity to transmit large power flows from the grid to the machine, as the maximal electrical current I_{max} flowing through the converter is limited to avoid damaging the switches. Equation 3.33 hereunder recalls the expression of the maximal power transmission at the PoC.

$$\|S_{PoC}\| = u_{PoC} \sqrt{i_{gd}^2 + i_{gq}^2} \quad \Leftrightarrow \quad \|S_{PoC}\|_{max} = u_{PoC} I_{max} \quad (3.33)$$

In case of a fault, the decrease of u_{PoC} indeed reduces the maximal apparent power $\|S_{PoC}\|_{max}$. This leads to the possible mismatch between this maximal transmissible power and the active and reactive power production requirements for the provision of the relative ancillary services.

The faulted behaviour of the installation highly depends on the operating mode during the occurrence of the fault : pump or turbine. This section presents the type of expected misbehaviour and the chosen solution to overcome it, for both cases.

3.3.1 Pump mode

While pumping, the installation takes active power from the grid. Let us here consider that the frequency of the grid is constant so that the active power consumed by the pump is steady. In case of a voltage dip, the transmission of active power through the grid-side converter saturates and the imbalance between P_{grid} and P_{stator} causes the DC-link voltage to decrease, according to Equation 3.27 recalled hereunder.

$$P_{grid} - P_{stator} = C \frac{d}{dt} \left(V_{dc}^2 \right)$$

If V_{dc} decreases, so does the maximal voltage amplitude applicable to the stator phases of the IM. Indeed, the relationship between $V_{s,max}$ and V_{dc} is the following.

$$V_{s,max} = \frac{V_{dc}}{2\sqrt{2}}$$

During the discharge of the DC-link capacitor, its voltage happens to reach a value constraining the capacity of the pump to develop the required power and the system inherently adapts P_{stator} to the maximal available active power at the grid-side.

Critical situation

The critical situation occurs after the fault clearance, as the grid voltage $\bar{u}_g$ recovers and the voltage at the PoC comes back to a value close to its nominal one. At this moment, the active

power transmission block of the grid-side controller ceases the opportunity to increase the DC-link voltage back to its nominal value. However, if the voltage drop at the terminals of the capacity is too deep, this action leads to the instability of the system.

This instability due to the DC-link voltage drop is explained using the single-phase equivalent model of the line connecting the installation to the ideal *Thévenin* equivalent voltage source, which is shown in Figure 3.17.

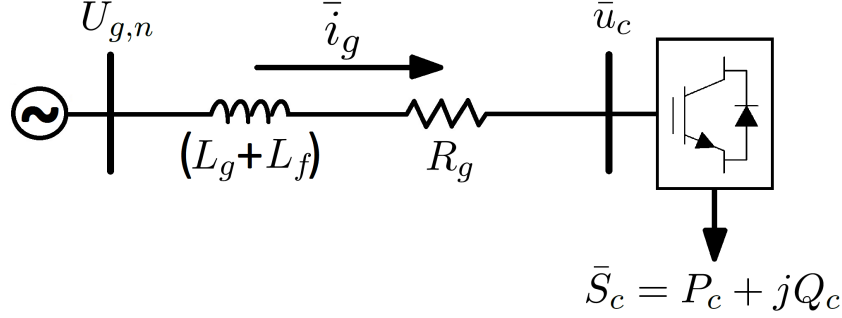


Figure 3.17: Single-phase equivalent line connection to the grid.

In Figure 3.17, the impedance of the line $\bar{X}_g = R_g + jL_g$ and the inductance of the filter L_f are merged together to simplify the problem. At the voltage recovery, we assume the nominal amplitude of the grid voltage $U_{g,n}$. In this case, the voltage droop control leads to zero reactive power injection and a unitary power factor. The attentive reader will have noticed that the FRT curve forecasts a voltage recovery at $0.9pu$ after a fault clearance, in which case the reactive power output should be of $0.33P_n$ and not zero. The previous assumption however allows to illustrate the unstable behaviour without loss of generality.

In this configuration, there is one *PQ* node at the converter-side of the line, where P_c and Q_c to be transmitted are fixed. The other is a *swing* node, at the grid-side of the line, where only $U_{g,n}$ is fixed. Figure 3.18 represents the typical evolution of the voltage at the end of a power line as a function of the transmitted power.

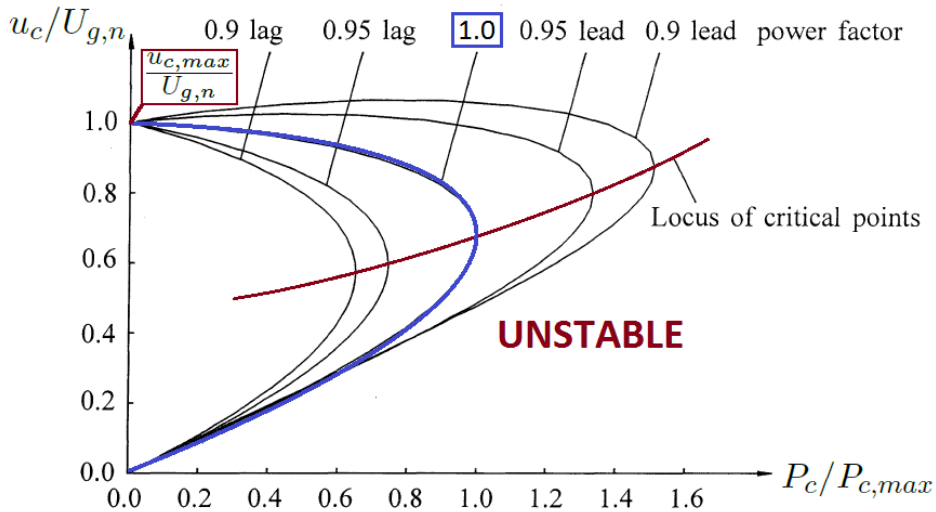


Figure 3.18: Nose curve stability analysis. Adapted from [30].

In this figure, we observe that, along the unitary power factor curve, there exists two different voltages for transmitting a certain amount of power. Only the part of the curve above the *locus*

of critical points corresponds to a stable transmission. In our case, the amplitude u_c of the voltage at the converter is limited by the value of V_{dc} and, if too low, only the unstable part of the curve becomes reachable after the fault clearance. In order to ensure the stability of the system after the fault clearance and be able to produce any power P_c , a sufficient condition is to maintain the DC-link voltage at a value verifying the following condition

$$V_{dc} \geq 2\sqrt{2}u_{c,max} \quad (3.34)$$

The previous considerations lead to the conclusion that the stability of the system in this case depends on many variables such as :

- The DC-link capacitance C and reference voltage V_{dc}^*
- The filter inductance L_f
- The line characteristics : short-circuit power S_{sc} and X/R ratio
- The active power consumed by the pump during the fault
- The duration and depth of the voltage dip
- The recovered grid voltage amplitude u_g .

The only possibility to guarantee the sufficient stability condition of Equation 3.34 is to oversize the DC-link reference voltage, which is inconsistent with the will to keep the installation costs to the minimum. In order to keep a cheap system, as well as remove its sensitivity to voltage dips, the control strategy is adapted.

Adapted algorithm

The new strategy in case of a fault occurring while pumping is shown in Figure 3.19. In this case, the voltage drop at the PoC limits the power exchanges between the installation and the grid. As explained previously, the priority is given to the provision of reactive reserve to sustain the grid. If the grid-side converter current is not saturated, the remaining power capacity is allocated to the transmission of the active power towards the IM. The power flows are then totally determined by the grid-side controller.

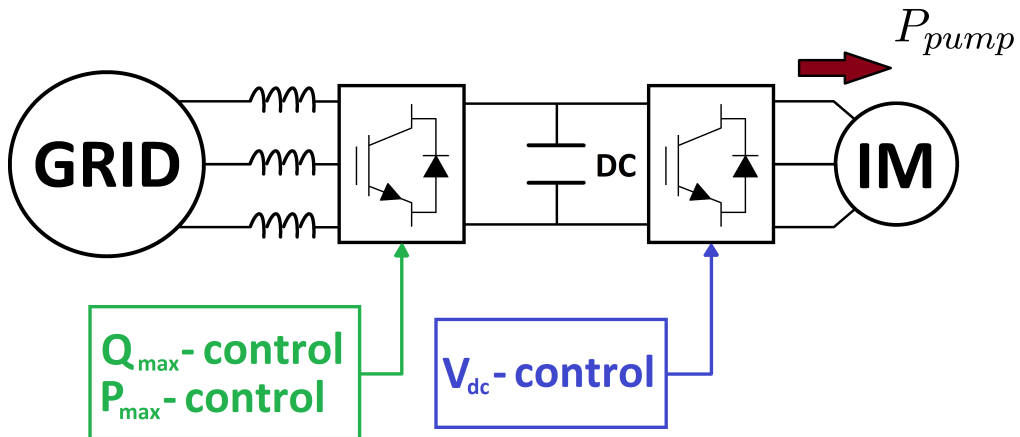


Figure 3.19: Adapted FRT control strategy : pump mode.

As the DC-link voltage must remain steady in order to avoid post-fault instabilities, the stator-side controller inherits its regulation in case of fault. The control of V_{dc} ensures that the active power transmitted to the pump is exactly, to the losses, equal to the active power taken from the grid.

3.3.2 Turbine mode

While turbinning, the installation produces active power and transmits it to the grid through the converter. Let us assume again a steady frequency and active power reference. In case of a fault, the transmission of active power saturates and the imbalance between P_{grid} and P_{stator} causes the DC-link voltage to increase, according to Equation 3.27.

Critical situation

In the case of a DC-voltage rise, there is not any risk of unstable behaviour of the system after the fault clearance. However, the sizing of the capacitor and of the whole converter relies on the maximum admissible voltage of the link. Indeed, the sensitivity of the capacitor, transistors and diodes forces the DC voltage to remain below a certain threshold. The critical behaviour is then during the fault, when the surplus energy produced by the turbine causes the DC voltage to rise.

Adapted algorithm

Among others, the solution consisting in the dissipation of the surplus energy proves to be effective. To do so, the system requires the introduction of a *braking chopper*, as shown in Figure 3.20. In our case, we implement the chopper block in parallel with the DC-capacitor by a simple resistor of resistance $R_{chopper}$ in series with a switch.

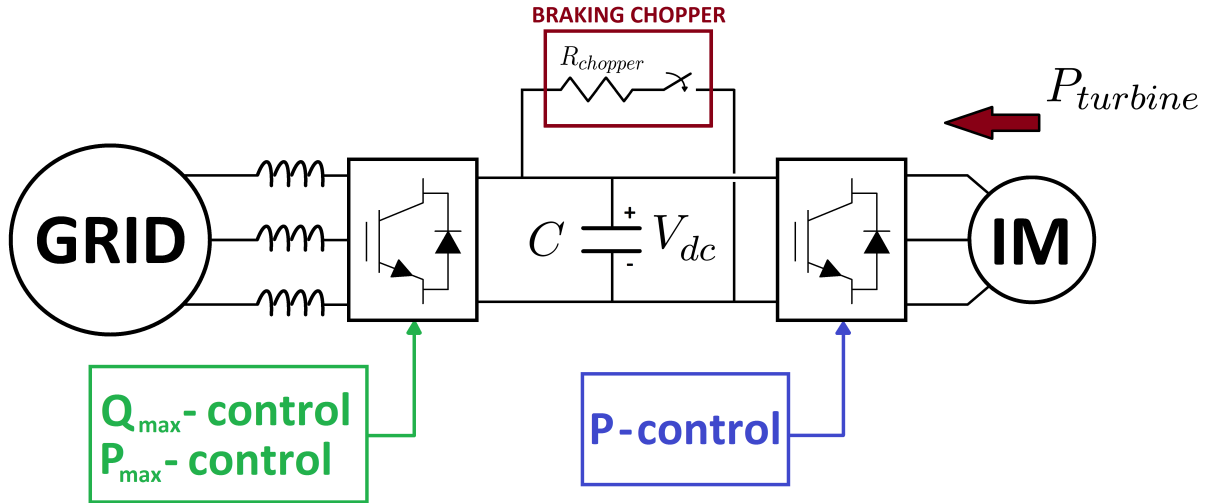


Figure 3.20: Adapted FRT control strategy : turbine mode.

The switch in the chopper block remains normally open as long as the installation operates in normal conditions. When a fault occurs and a voltage rise is measured at the terminals of the DC-link, the switch is closed, allowing to dissipate the surplus energy in the resistance and to maintain the DC-voltage within a pre-determined range.

The resistor sizing dimensioning needs to take the maximum admissible current of the converter into consideration. The drawbacks to this method is the loss of energy and the need to evacuate the additional heat produced by the resistor.

Chapter 4

The *Negundo* study case

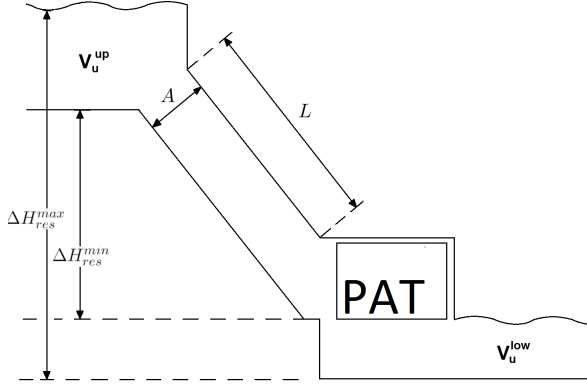
In this chapter, we apply the model previously constructed in Chapters 2 and 3 to a real study case. Within the global *SmartWater* project, a micro PHES experimental site called *Negundo* has been installed in Froyennes, Belgium. The goal of this site is to characterise the working conditions of a variable speed PaT unit. As the building of the installation is still in progress, the lack of available data makes the comparison impossible with our simulation results.

In Section 4.1, we sum up the main characteristics of the site and the components of the system, which are required for the practical simulation of the study case : the site layout, the PaT, the IM, the converter and the output filter. The tuning of the controllers is also given, completing the model.

Then, the simulation results are given in Section 4.2. The first part of the simulation consists in the comparison of the elastic and inelastic water column models, as well as the comparison of the averaged and continuous models of the converter, in order to determine the most appropriate system for the rest of the simulations. Afterwards, the second part validates the implemented power control strategy in both pumping and turbinning operations, considering the possible provision of FCR. The last part of the simulation consists in testing the robustness of the installation in case of voltage dips. The ability to provide automatic droop voltage control is also tested during these voltage drops.

4.1 System overview

The main characteristics of the *Negundo* site are summed up in Figure 4.1 and Table 4.1. They are extracted from the complete layout of the installation given in Appendix E. ΔH_{res}^{max} and ΔH_{res}^{min} are respectively the maximal and minimal available head pressures, corresponding to the cases when either the upper or the lower reservoir is full. V_u^{low} and V_u^{up} are respectively the lower and upper reservoir volumes, while L and A determine the penstock pipe.



Parameter	Value
ΔH_{res}^{max}	9.75 m
ΔH_{res}^{min}	5.9 m
V_u^{low}	625 m ³
V_u^{up}	1500 m ³
L	79.50 m
A	0.099 m ²

Table 4.1: *Negundo* main hydraulic data.

Figure 4.1: The *Negundo* hydraulic layout.

This section is the reflection of Chapter 2. We here adapt the previous theoretical considerations to the practical implementation of the *Negundo* case. In this sense, the hydraulic characteristic curves of the PaT are recovered, in order to be injected into the full hydraulic model developed in Section 2.1. Then, the electrical features of the induction machine, the back-to-back converter and the L-filter are gathered. Finally, the PI gains tuning is given.

4.1.1 Pump-as-Turbine characteristic curves

The pump selected for the *Negundo* project is the centrifugal pump *ICN300-315* manufactured by *Ensival-Moret*. Its technical datasheet is also given in Appendix E, where we observe the pump characteristic curves together with the nominal power of the pump being about 16.7kW. As predicted, the turbine characteristics are unknown. The goal of this section is to build the dimensionless characteristics curves $\psi(\phi)$ and $\pi(\phi)$ in both pump and turbine modes.

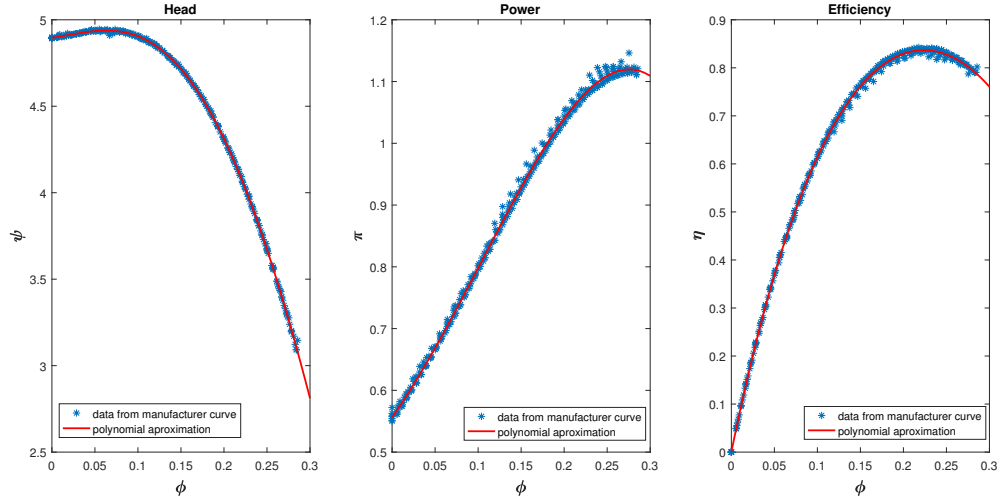
Pump mode

The speed normalisation of the six dimensional curves $H(Q)$ and $P(Q)$ from the datasheet by the rotational speeds leads to the dimensionless $\psi_p(\phi)$ and $\pi_p(\phi)$ of Figure 4.2. The additional efficiency curve $\eta_p(\phi)$ is computed using the other two curves and the previously developed relation 4.1.

$$\eta_p(\phi) = \frac{P_{hydro}}{P_{mec}} = \frac{\rho g H_p Q_p}{P_p} = \frac{\phi \psi(\phi)}{\pi(\phi)} \quad (4.1)$$

We notice in Figure 4.2 that the normalisation of the dimensional data extracted from the curve indeed lead to a single dimensionless polynomial approximation in red. This confirms the predicted independence on the speed due to the similitude principle. We observe that the head characteristic $\psi(\phi)$ fits the manufacturer data almost perfectly, while its power equivalent $\pi(\phi)$ is less accurate. This phenomenon is due to the extraction method used and the difficulty for precise sampling in the case of the power curves.

The dimensionless efficiency $\eta(\phi)$ presents a maximum on the considered flow rate range. Using the polynomial approximation curves, we deduce the *Best Efficiency Point* in pumping conditions.

Figure 4.2: Dimensionless characteristics $\psi_p(\phi)$, $\pi_p(\phi)$ and $\eta_p(\phi)$ - pump mode.

The nominal conditions returned by the manufacturer are also normalised in order to compare them to the computed BEP. These results are presented in Table 4.2.

	Nominal conditions	<i>Best Efficiency Point</i>
ϕ_p	0.24	0.22
ψ_p	3.78	4.04
π_p	1.10	1.08
η_p [%]	83.4	83.7

Table 4.2: Nominal conditions and BEP - pump mode.

We here indeed observe that the nominal conditions defined by the manufacturer closely correspond to the BEP.

Turbine mode

Once the dimensionless pumping characteristic curves are recovered, their turbining equivalents are constructed using the two-step procedure detailed in Section 2.1. First of all, the turbining BEP is computed according to its pumping equivalent using Equations 2.14, 2.15 and 2.16. The results are presented in Table 4.3.

	<i>Best Efficiency Point</i>
ϕ_t	-0.26
ψ_t	5.23
π_t	1.12
η_t [%]	82.0

Table 4.3: BEP - turbine mode.

In this case, the flow rate variable at the BEP has a negative value. This is due to the fact that the water flux is reversed in the case of turbining. We also observe that the turbine mode presents a lower maximal efficiency than the pump mode, as well as a higher net head and a higher flow rate, in absolute terms.

The second step consists in building the full turbine dimensionless curves based on the turbin-ing BEP. Equations 2.17 lead to the following polynomial expressions of the dimensionless characteristic curves in turbine mode.

$$\begin{aligned}\psi_t(\phi) &= 71.0063\phi^2 - 9.8201\phi + 2.4821 \\ \pi_t(\phi) &= -18.1357\phi^3 + 32.7551\phi^2 - 3.5172\phi + 0.0466\end{aligned}\tag{4.2}$$

The characteristic curves of the machine in both pump and turbine modes are merged in Figure 4.3. The following convention is used. A positive flow rate ϕ corresponds to the pump mode, in light blue, while a negative flow rate corresponds to the turbine mode, in dark blue. We here also consider that the hydro-mechanical power and rotational speed are always positive.

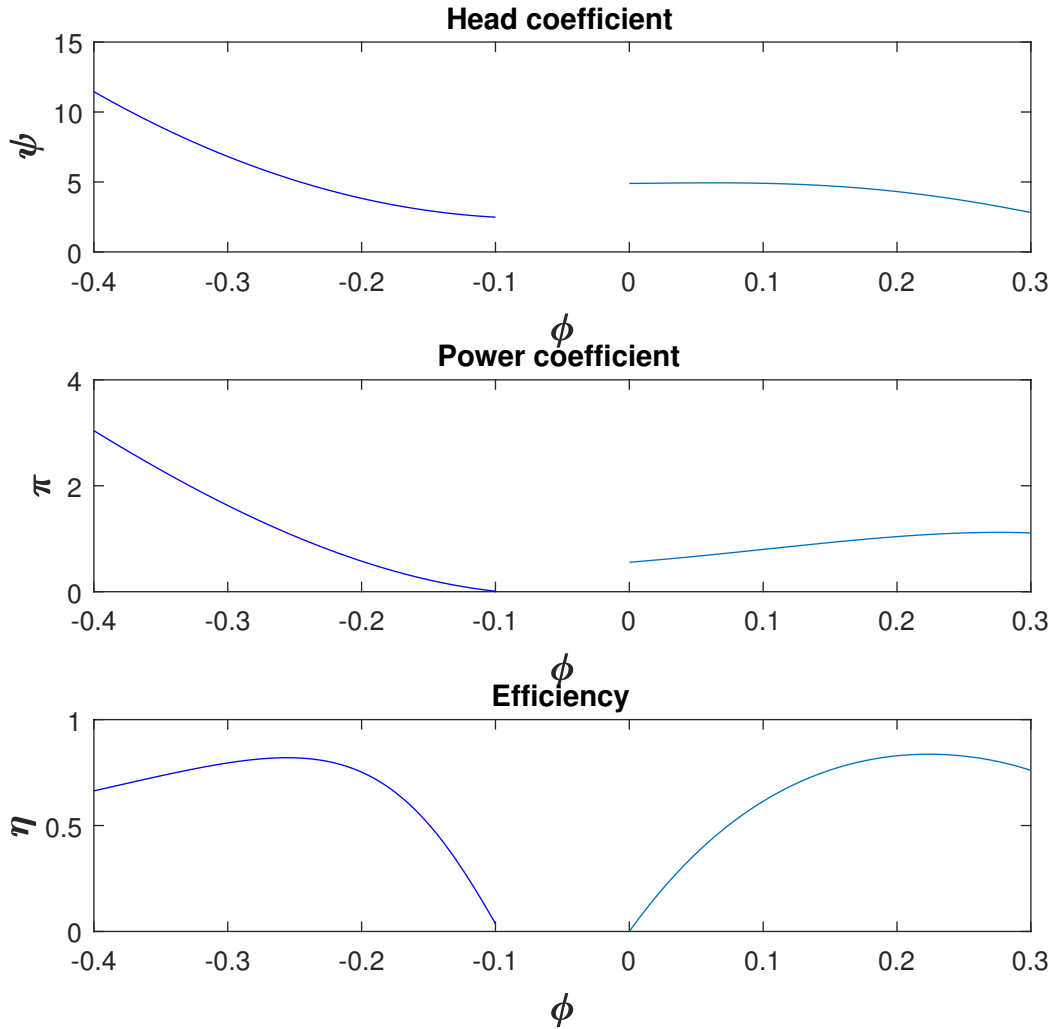


Figure 4.3: PaT dimensionless characteristics - pump [light blue] and turbine [dark blue].

We notice that when the water flux approaches zero in turbine mode, the machine becomes unable to operate properly.

Power-to-Speed relation in turbine mode

For a fixed head, the turbinning characteristic curves allow to compute the steady-state rotational speed of the machine rotor shaft as a function of the hydro-mechanical power produced. This relation is crucial to the active power controller algorithm developed in Section 3.1, where the equation system defining this relation is presented. Indeed, the direct power control implemented for the pump mode presents instability issues when applied to the turbinning conditions. Hence, the overall power regulating loop includes a speed controller whose input directly relies on the desired active power. Figure 4.4 shows the power-to-speed curves for the maximum and minimum available head H_{res} of the installation.

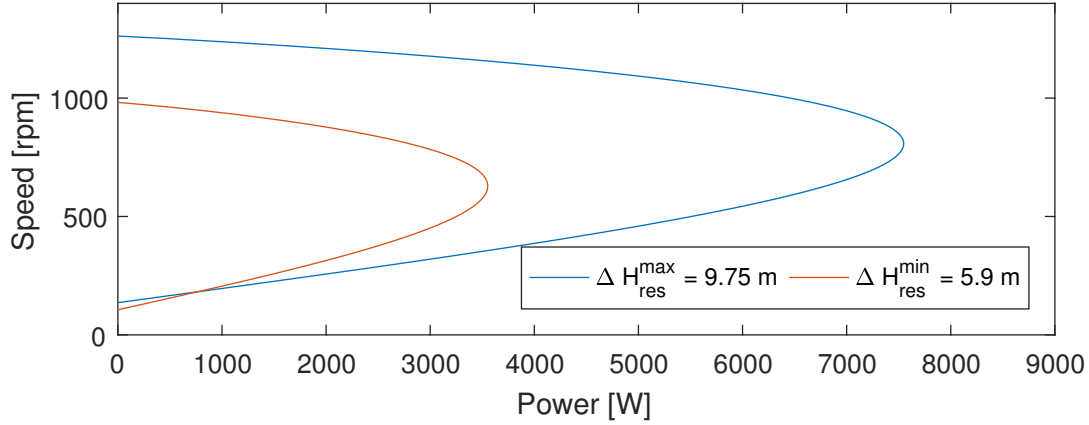


Figure 4.4: Power-to-Speed relation - turbine mode.

We observe in Figure 4.4 that there exists two distinct operating points for producing a certain power. We however only consider the upper part of the curve, which corresponds to the higher speeds. In fact, the lower part of the curve fits high values of the water flow rate in absolute terms, with typically $\phi_t < -0.4$. The characteristic curves are not valid in this range anymore. Moreover, the turbinning BEP is located at $\phi_t = -0.26$, corresponding to the upper part.

The second observation concerns the maximum power output of the PAT. The power varies from $3.5kW$ in the worst case of ΔH_{res}^{min} to $7.6kW$ with the maximum ΔH_{res}^{max} . This variation from simple to double of the power is due to the very low height difference between the reservoirs, by comparison with their relative depth.

4.1.2 Electrical parameters of the squirrel-cage IM

The electro-mechanical converter coupled with the centrifugal pump is a *W22-Cast Iron Frame-High Efficiency-IE2* squirrel-cage induction machine. The required information for its characterisation is also given in Appendix E.

Parameter	Name	Value	Unit
N_n	Rotational speed	985	rpm
P_n	Active power	30	kW
U_n	Line voltage	400	V
f_n	Synchronous frequency	50	Hz
J_{asynch}	Moment of inertia	0.71916	kgm^2
$2p$	Pole numbers	6	-
T_n	Rated torque	291	Nm

Table 4.4: IM nominal values.

Parameter	Value	Unit
R_s	0.202	Ω
L_{ls}	866.7	μH
R_r	0.1609	Ω
L_{lr}	757.26	μH
L_m	29.13	mH

Table 4.5: IM parameters.

The extraction of the nominal values of the IM is given in Table 4.4. The datasheet also provides the required testing information for identifying the machine equivalent circuit parameters presented in Table 4.5. These are computed using the parameter estimator of the *Asynchronous machine* block of the *Simulink/Simscape Power System* toolbox.

4.1.3 Interface with the grid

We here provide in Table 4.6 the parameters of the grid interfacing block composed of the VSI converters, the DC-link and the L-filter. The dimensioning of these components is done according to the considerations of Section 2.3

The DC-link voltage is sized using the formula linking V_{dc} to the maximal voltage accordingly applicable to the terminals of the converter, while the capacitor is a trade-off between the amount of energy it stores and the cost it represents for large capacitance values. As we do not have the datasheet of the future *Negundo* converter, the latter is sized based on the nominal conditions and a 10% margin. The rated apparent power is made of the nominal active power of the electrical machine and the 33% P_n reactive power required for the voltage primary reserves provision.

Parameter	Name	Value	Unit
V_{DC}	Nominal DC-link voltage	750	V
C	DC-link capacitance	10	mF
S_{rated}	Rated apparent power	35	kVA
L_f	Filter inductance	1	mH

Table 4.6: Grid interface block parameters.

4.1.4 PI controllers

In this section, we provide the tuning characteristics used for the proportional and integral gains of the stator-side (SSC) and grid-side controllers. The variables of the PI controllers are the cut-off frequency $f_0 = \frac{\omega_0}{2\pi}$ and the damping factor ζ . Their sizing is given in Table 4.7.

	Controller	f_0 [Hz]	ζ [-]
SSC	Stator currents - $i_{s,dq}$	50	1
	Speed - ω_m	5	0.8
GSC	Grid currents - $i_{g,dq}$	500	1
	DC-link - V_{dc}	50	0.8

Table 4.7: PI gains tuning characteristics.

In the SSC block, the fundamental controllers are the PI current regulation loops. The unitary damping is chosen to avoid overshoots and remain within the limits of the maximal current of the converter. The frequency is sized to ensure a sufficiently fast response time by comparison to the external regulation loops, typically 10 times quicker. In the turbining case, we use an external speed regulator to perform the power control. This speed regulator is determined to be able to closely follow the rate-limited speed reference returned by the *power-to-speed* function.

In the GSC block, the external regulation loops only control electrical quantities, such as the reactive power and the DC-link voltage. The electrical dynamics being fast, we size the DC-link controller with a relatively high frequency. The inner current loops are again determined in order to avoid overshoots and to be 10 times faster than the outer loops.

4.2 Simulation results

This section corresponds to the core validation of the whole model developed in the Chapters 2 and 3. The parameters used for the following simulations are the ones of the *Negundo* experimental site, which were computed in the previous section. The system is here tested to verify the validity of the model and the ability to furnish the required grid operator services established in Chapter 1.

We start by defining the *per unit* system used for the future presentation of the results. The first simulation concerns the comparison of the two hydraulic models in order to select the one to be kept for the rest of the tests. Then, the ability to control the active power produced or consumed by the stator side controller is tested and compared to the specific requirements for FCR supply. Finally, the installation is tested under varying voltage amplitude at the point of connection, to characterise its RPR capability and verify the specific algorithms for FRT.

4.2.1 Per unit system

The following simulations results are all expressed in per unit. In order to do so, we need to define the reference values relative to the presented results. These are given in Table 4.8. The references for the electrical system are the values leading to maximum power in pump mode of the asynchronous generator (Appendix B).

Concerning the hydraulic system, the references are adapted to the considered mode : pump or turbine. In pump mode, we consider the characteristic working point at the maximum available head of the installation (ΔH_{res}^{max}) and at the maximum power of the electrical machine.

In turbine mode, the reference values are taken to be the working point at the maximum available head of the installation (ΔH_{res}^{max}) and at the maximum power given by the power-to-speed relation in Figure 4.4.

Parameter	Electric system	Hydraulic system		Units
		Pump mode	Turbine mode	
N_b	-	1416	-845	rpm
P_b	30	30	-7.5	kW
T_b	207	207	84.76	Nm
H_b	-	9.75	9.75	m
Q_b	-	739.44	-359.43	m^3/h

Table 4.8: Reference values for the *per unit* system.

We remark different reference torques for both systems. This leads to different pu values even when, in steady-state, the electromagnetic torque equals the hydro-mechanical torque, to the friction losses.

4.2.2 Comparison of the elastic and inelastic models

In Section 2.1, two models of the hydraulic system are developed, with different accuracy levels. This first simulation compares the system temporal response to a reference active power step at $t = 1s$, from 0.15pu to 1pu, with either the elastic or the inelastic model of the PaT included. The results are shown in Figure 4.5. The observation of the different graphs in Figure 4.5 allows to draw some general conclusions about system behaviour in pump mode. The superposition of the elastic and inelastic models responses also lead to specific considerations concerning the hydraulic phenomena.

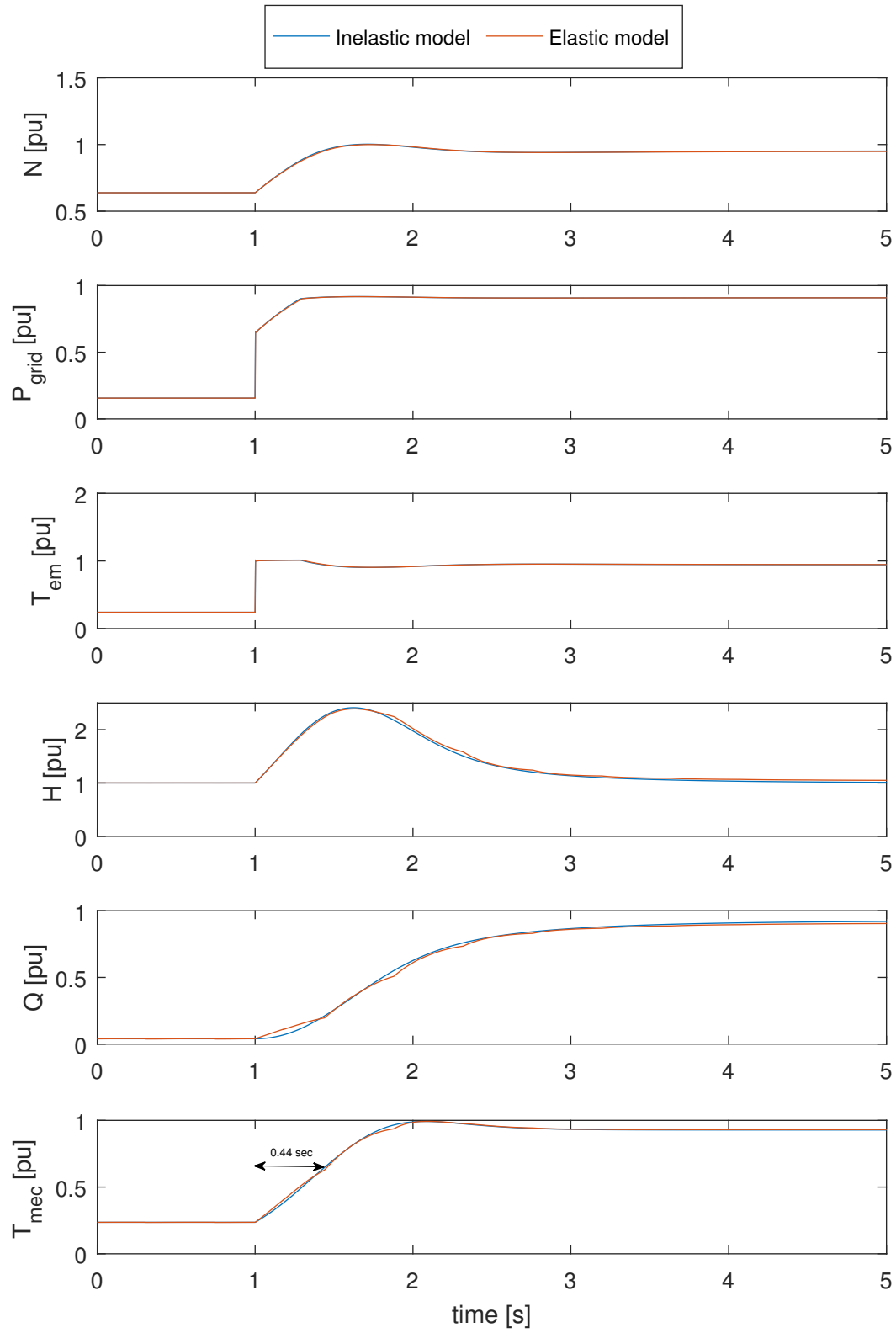


Figure 4.5: Elastic-inelastic comparison - power reference step from 0.15pu to 1pu - pump.

First of all, we observe that the fast electrical response of the current controllers of the SSC leads to an almost instantaneous rise of the electromagnetic torque T_{em} at the time of the step which sature to its maximal value of $1pu$. This enables the electrical active power P_{grid} to quickly follow its reference even if limited by electromagnetic torque during the first instant. On the other hand, the hydraulic system inertia induces a slower rise of the hydro-mechanical torque T_{mec} . This transient imbalance between the torques lead to the acceleration of the rotor shaft, with a discontinuity at $t = 1s$. The sudden acceleration increases the head pressure H at the PaT and provokes the propagation of a pressure wave along the penstock pipe, which might damage its components. This effect is called the *Water Hammer* and should be avoided. The study of the hydraulic effects on the mechanical components is out of the scope of the present document but in order to do so, the first derivative of the PaT rotational speed must be reduced. This can be done by limiting the rate of the electromagnetic torque variation.

Concerning the wave propagation highlighted by the elastic model, we notice that the first derivatives discontinuities in the head H and the flow rate Q happen every $0.44s$ after the step time. These are due to the discontinuity in the speed N at $t = 1s$. The $0.44s$ time period of the discontinuities is the propagation time of the pressure wave along the pipe. Indeed, the considered length of the pipe is $L = 80m$ and the wave velocity computed with the *Negundo* parameters injected into Equation 2.4 gives $a = 364.18m/s$. Hence the period is equal to $\frac{2L}{a} = 0.44s$, which corresponds to the simulation result.

The next remark concerns the new steady-state operating conditions of the machine after the step. In Figure 4.5, we notice a difference between the head H and the flow rate Q of the elastic and inelastic models at $t = 5s$. This difference in working points is uniquely due to the friction losses which are considered in the elastic case but neglected in the simplified inelastic model. Indeed, friction causes greater head losses at higher flow rates. Hence, the elastic model stabilises at a higher H and a lower Q at the PaT.

We conclude this part by observing that only the head and the flow rate at the PaT are significantly different from one model to the other. The electrical quantities are however very similar. As this study focuses on the provision of electrical power reserves, the variable of interest is here the power produced by the installation P_{grid} , which is exactly the same with both hydraulic models. Therefore, the following simulations only consider the hydraulic inelastic case, as it allows considerable gains in matters of computational costs.

4.2.3 Primary frequency control (FCR)

FCR contracting with the grid operator requires the system to be able to automatically adapt its active power production proportionally to the measured frequency difference with the nominal grid value of $50Hz$. In order to be eligible, the power adaptation must verify the constraints developed in Section 1.1. To sum up, the maximum power capacity must be reachable within $30s$, the half of it within $15s$ and ensure at least a linear behaviour between the two. We here test the capacity of the installation to adapt the production to a new setpoint within these limits.

Please notice that the requirement of operating during at least $15min$ in a row at the maximum capacity is inherently verified by the volumes of the reservoirs given in Table 4.1 and the maximal working conditions presented in Table 4.8, whatever the contracted capacity band. Taking the smallest reservoir volume $V_u^{low} = 625[m^3]$ and the pumping maximum flow rate $Q_{pb} = 739.44[m^3/h]$, this indeed allows to compute a minimum operating time of about $51min$.

Pump mode

In Section 2.3, two models of the variable-frequency converter are developed, with different accuracy levels. This second simulation compares the system temporal response to a reference active power step at $t = 1\text{s}$, from 0.95pu to 0.2pu , with either the averaged or the full model of the converter included. The results are shown in Figures 4.6 and 4.7.

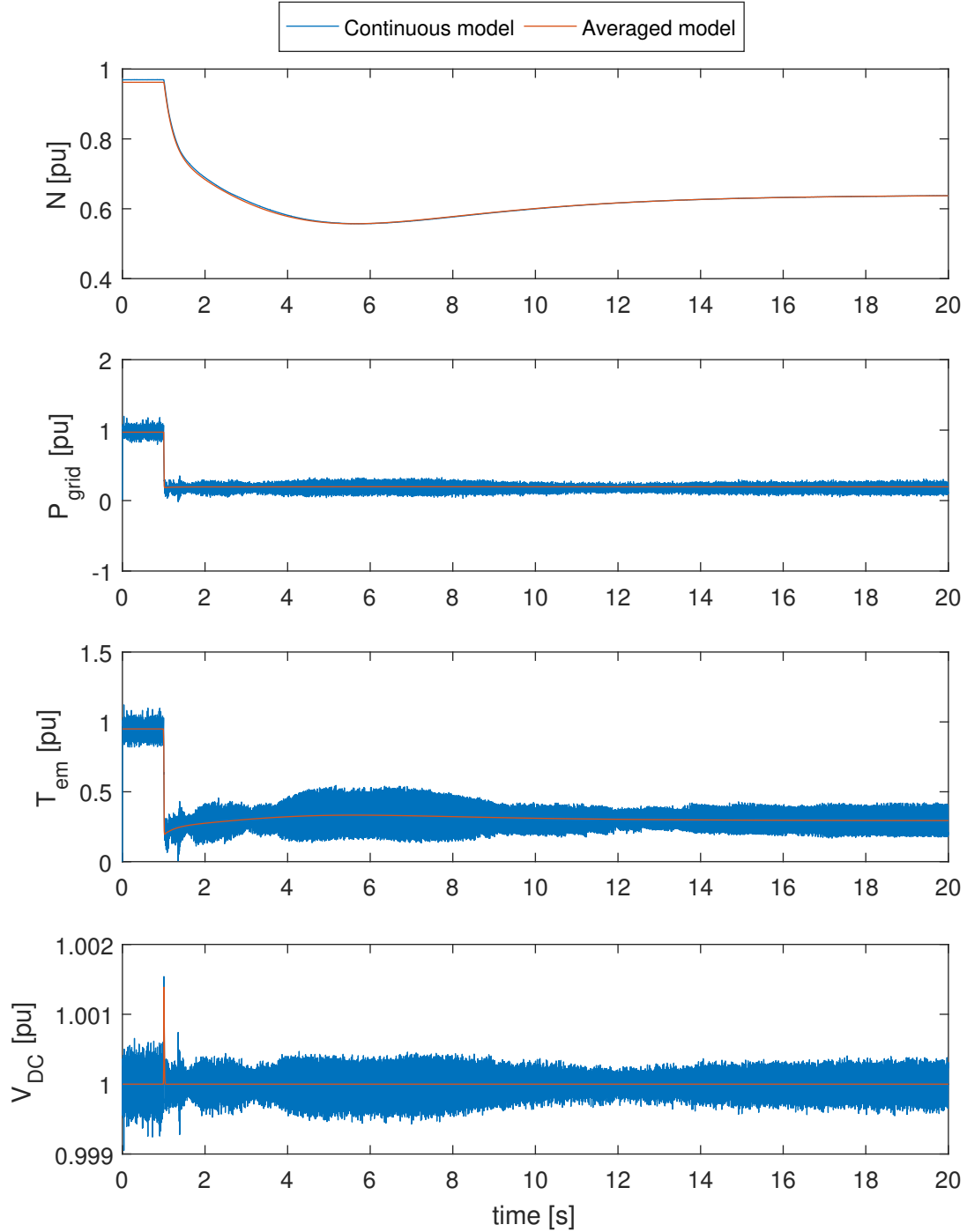


Figure 4.6: Full-Averaged comparison - power reference step from 0.95pu to 0.2pu - pump (I).

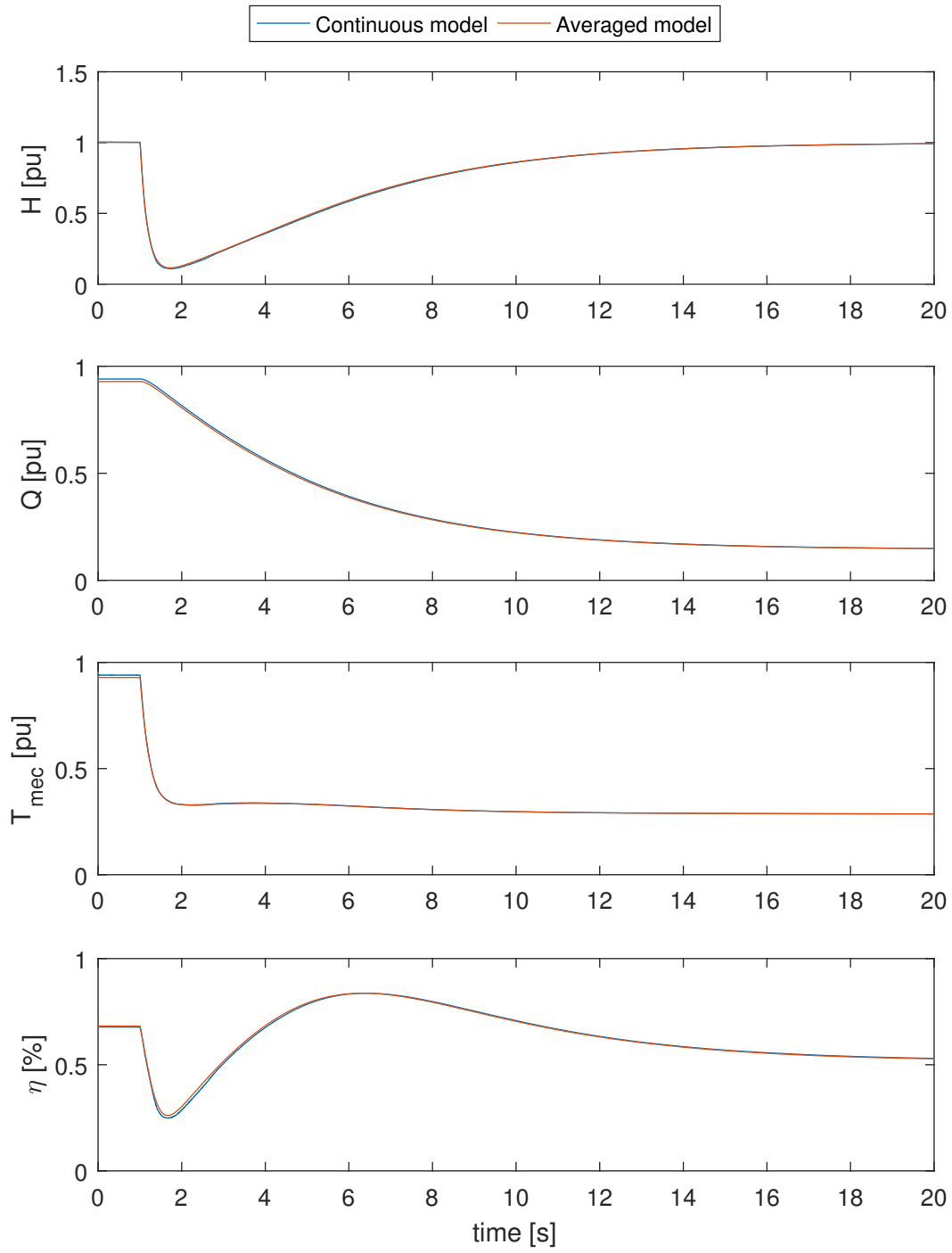


Figure 4.7: Full-Averaged comparison - power reference step from 0.95pu to 0.2pu - pump (II).

The analysis of the results given in Figures 4.6 and 4.7 highlights some important facts about the primary frequency control in pump mode, the differences between the system behaviour under the influence of the full back-to-back converter or its average model, as well as additional considerations concerning the hydraulics and the power transmission through the converter.

We start by comparing both models. We observe that the model used for the converter does not influence the mechanical characteristics of the system. Only the electrical variables in Figure 4.6 are affected with a high frequency content in case of the full back-to-back converter model being used. On the other hand, the averaged model curves follow exactly the temporal average of their full model equivalents. This provides the required validity to the averaged model, which is used for the rest of the simulations, as it allows considerable gains in terms of computational costs. Indeed, the study of the harmonic and inter-harmonic content of the power transmitted through the converter is out of the scope of this study. We here simply notice that the fluctuations in the power P_{grid} , electromagnetic torque T_{em} and DC voltage V_{dc} are due to the commutations of the converter switches. The amplitude of these harmonics is then function of three main variables :

- The DC-link voltage amplitude.
- The PWM carrier frequency.
- The inductance at the grid-side converter terminal.

The main observations concern the ability of the installation to control the active power transmitted to the grid. In pump mode, all the grid operator requirements covering the provision of FCR are respected. As noticed in the previous simulation, the electromagnetic torque T_{em} is adapted almost instantly due to the reactivity of the current controllers, providing the required electrical active power within the second. With a natural frequency $f_0 = 50[Hz]$ and a damping of $\zeta = 1$, the theoretical 98% response time of the system at is $0.0184s$. This is also verified by the simulation. Indeed, in Figure 4.8 showing a zoom on the active power P_{grid} , we observe a response time of about $0.02s$.

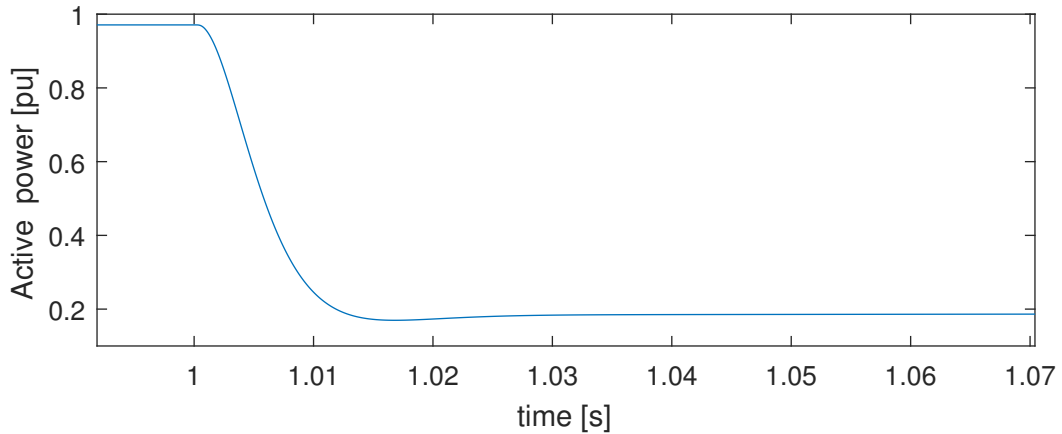


Figure 4.8: Averaged model - active power reference step response - pump.

On the other side of the rotor shaft, the inertia of the mechanical system, especially of the water column leads to much slower hydraulic transients. As the hydro-mechanical torque takes longer to decrease than its electromagnetic equivalent, the imbalance causes the rotational speed to decline, lowering the net head H at the PaT during the transient. A too low value of H is likely to provoke cavitation in the pump. In Appendix E, the datasheet specifies a minimum head value of $1.97m$, corresponding here to $0.2pu$. The head curve in Figure 4.7 falls below this threshold during a short time. The present study does not take the operational range

of the machine into account, but in order to avoid this cavitation risk, we should either reduce the furnished power capacity band, or limit the transition rates of the electromagnetic torque.

Then, the hydraulic efficiency η_{hyd} temporal evolution during the simulation is also plotted in Figure 4.7. We notice that the second working point is characterised by a lower η_{hyd} . This is due to the absence of a guide vane, as opposed to *Francis* or *Pelton* turbines, leaving the electromagnetic torque as the only control variable of the system. Therefore, doing primary frequency control involves operating in conditions which are different from the BEP and can consistently degrade the efficiency of the hydraulic machine.

The last remark concerns the DC-link voltage, which is constant throughout the simulation, apart from the little peak value at $t = 1s$. Being a reflection of the power transit through the converter, the peak occurs at the moment of the power setpoint change. In this case, the instantaneous imbalance between P_{grid} and P_{stator} causes V_{dc} to rise. The fast reaction time of the controller however limits the peak to an acceptable amplitude of less than 0.002pu.

Turbine mode

From now on, all the remaining simulations make use of the single system implementing the equivalent inelastic water column, as well as the averaged model of the back-to-back VSI converter. The previous results effectively proved the validity of these simplified versions of the components regarding the production and transmission of power to the grid.

This third and fourth simulations analyse the system temporal response to a reference active power step at $t = 1s$, from 0.93pu to 0.1pu. The initial setpoint is determined using the following reasoning. The maximum hydro-mechanical power of the PaT is 1.0pu. As the efficiency of the electrical installation is about 0.93%, the maximum reachable electrical active power reference is $P_{max}^* = 0.93pu$.

Before presenting the step response of the system, we remind the reader that a substantial change in the control algorithm is applied in case of the pump being used as a turbine. Appendix A proving the instability of the direct power control algorithm in this case, the adaptation of the active power production requires a speed controller based on the mapping of the steady-state power to the corresponding rotational speed.

In the previous pumping case, the direct power control adapts the electromagnetic torque, whose difference with the hydro-mechanical torque causes the speed of the machine to change and the hydro-mechanical torque to slowly follow its electromagnetic equivalent. In the current turbinning case implementing a speed controller, it is the opposite behaviour. The electromagnetic torque now needs to adapt to the hydro-mechanical torque. Due to the current controllers high reactivity, two implementation cases are here considered.

The first case studies the raw application of the speed reference to the controller. The second case consists in passing this signal through a filter, to reduce the strong electrical transient behaviour due to the fast adaptation of the speed by the variation of T_{em} .

- **Without the speed reference filter :** Figures 4.9 and 4.10 show the simulation results in case of the decrease of the power reference, without any filter on the deduced speed reference.

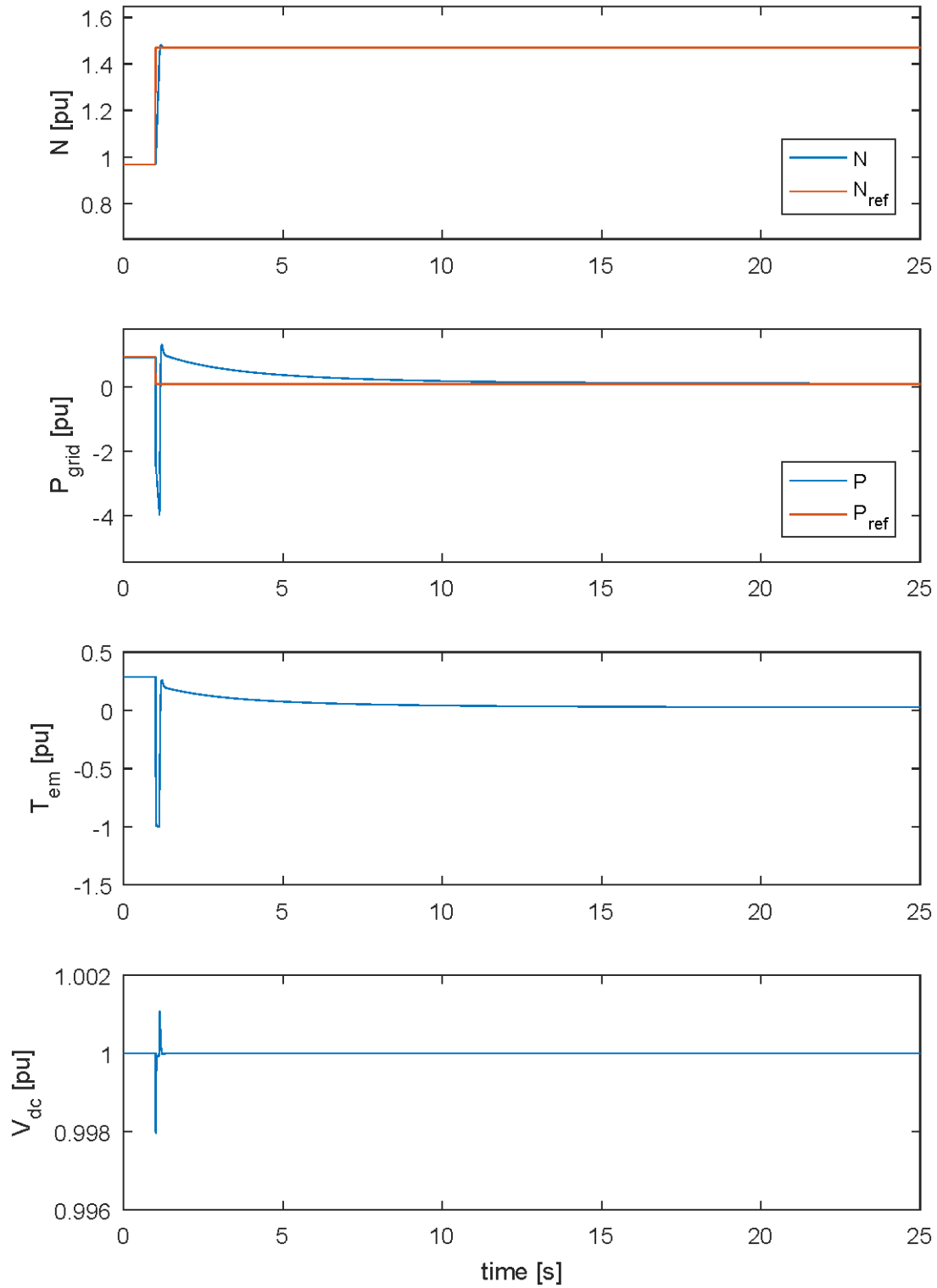


Figure 4.9: Without filter - power reference step from 0.93pu to 0.1pu - turbine (I).

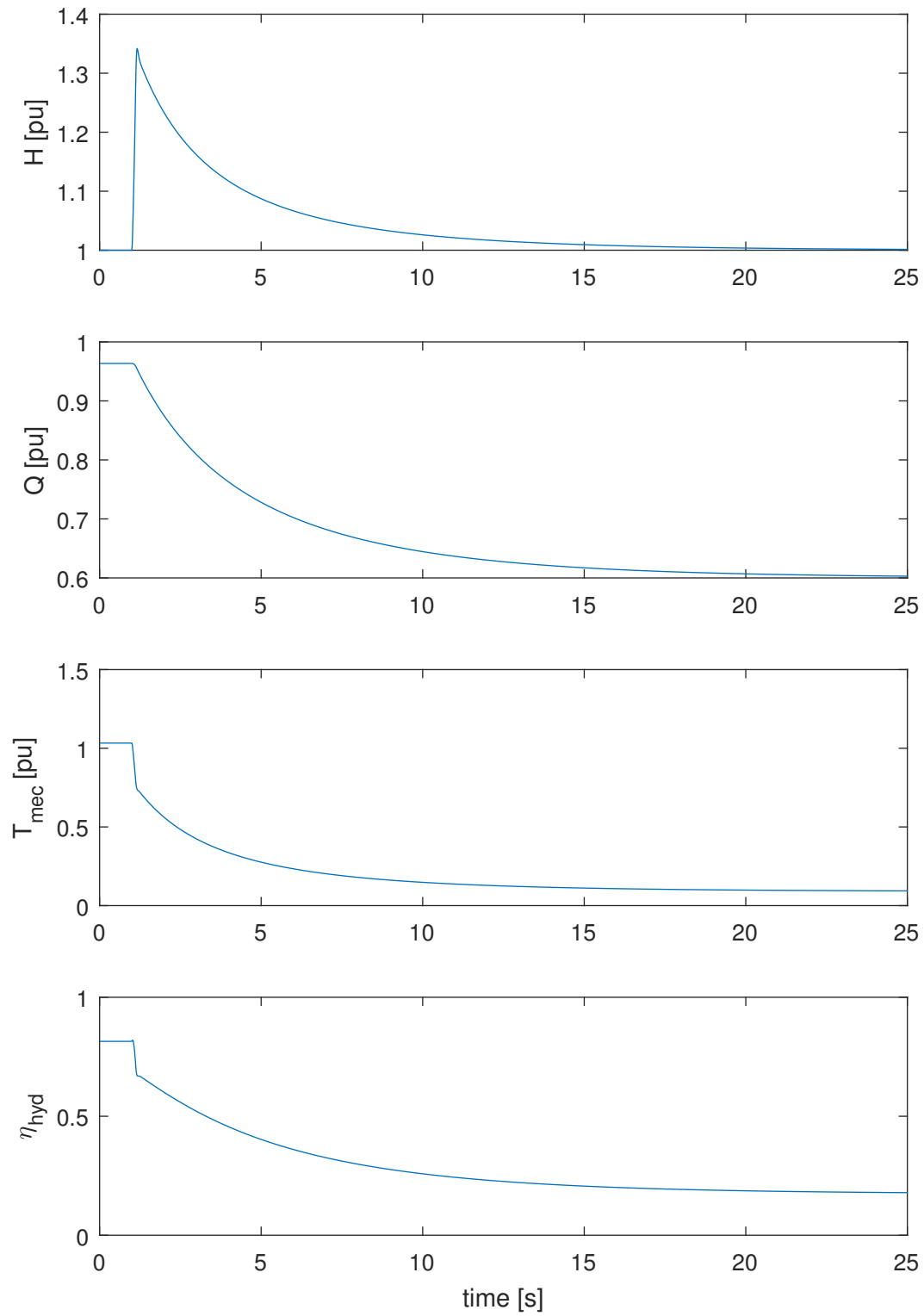


Figure 4.10: Without filter - power reference step from 0.93pu to 0.1pu - turbine (II).

In this case, the main observations in Figures 4.9 and 4.10 concern again the ability of the installation to control the active power P_{grid} produced and transmitted to the grid. As in the pump mode, the grid operator requirements covering the provision of FCR are respected. Unlike the previous simulations in pump mode, the active power here suffers a long transient behaviour inherent to the control. The control algorithm used is indeed dependent on the hydraulic transient, seeing that the power-to-speed function only works for the steady-state conditions. The response time at 98% of the active power control is thus of approximately 20s.

On top of the temporal length of the transient, it is also characterised by large parasitic amplitudes. As a matter of fact, the instantaneousness of the power-to-speed converter leads to a speed reference step. In order to quickly increase the speed, the torque order saturates to its maximum negative value and the power flow is temporarily inverted, reaching high negative values. These high transient peaks have negative impacts on the grid and must be reduced. It can be done by reducing the reference speed variation rate.

Then, we notice in Figure 4.10 that the fast acceleration of the rotor shaft causes again the water hammer effect, due to the pressure surge at the PaT. The rise of H is here faster but reaches a lower peak value than in the first simulation. This effect should still be avoided. It can also be done by reducing the reference speed variation rate.

The next remark concerns the efficiency η_{hyd} . We observe in Figure 4.10 that it undergoes a terrible drop to a value of about 20% for the second working point. As in the pumping case, the absence of a guide vane makes it impossible to control the efficiency and the system is bound to operate at other working points than the BEP.

Finally, we observe that the DC-link voltage remains quite stable, highlighting the correct transmission of the active power from the IM stator windings to the grid line. We however notice two admissible peaks due to the negative power surge at $t = 1s$ and the smaller positive power peak at the end of the rotor acceleration.

- **With the speed reference filter :** The two main drawbacks of the turbinning control observed in the results of the previous simulation are the relatively long response time of the system and its high transient peaks. The response time is fixed by the hydraulics, but the transient behaviour can be adapted by adding a first-order filter to smoothen the speed reference entering the controller. The time constant of the filter is chosen to have a response time at 98% of 10s, in order to preserve the previous response time. This filter is included for the next simulation, whose results are given in Figures 4.11 and 4.12.

Comparing these results with the ones of the previous simulation, we observe that the same working point is reached after about 20s. The response time of the system thus remains similar. However the huge transient peaks are here avoided and the power exchanged with the grid stays within an acceptable range.

Concerning the water hammer phenomenon, it is much lower due to slower rotational speed variations. The maximum value reaches by the head H is now 1.13pu, against 1.34pu without the filter.

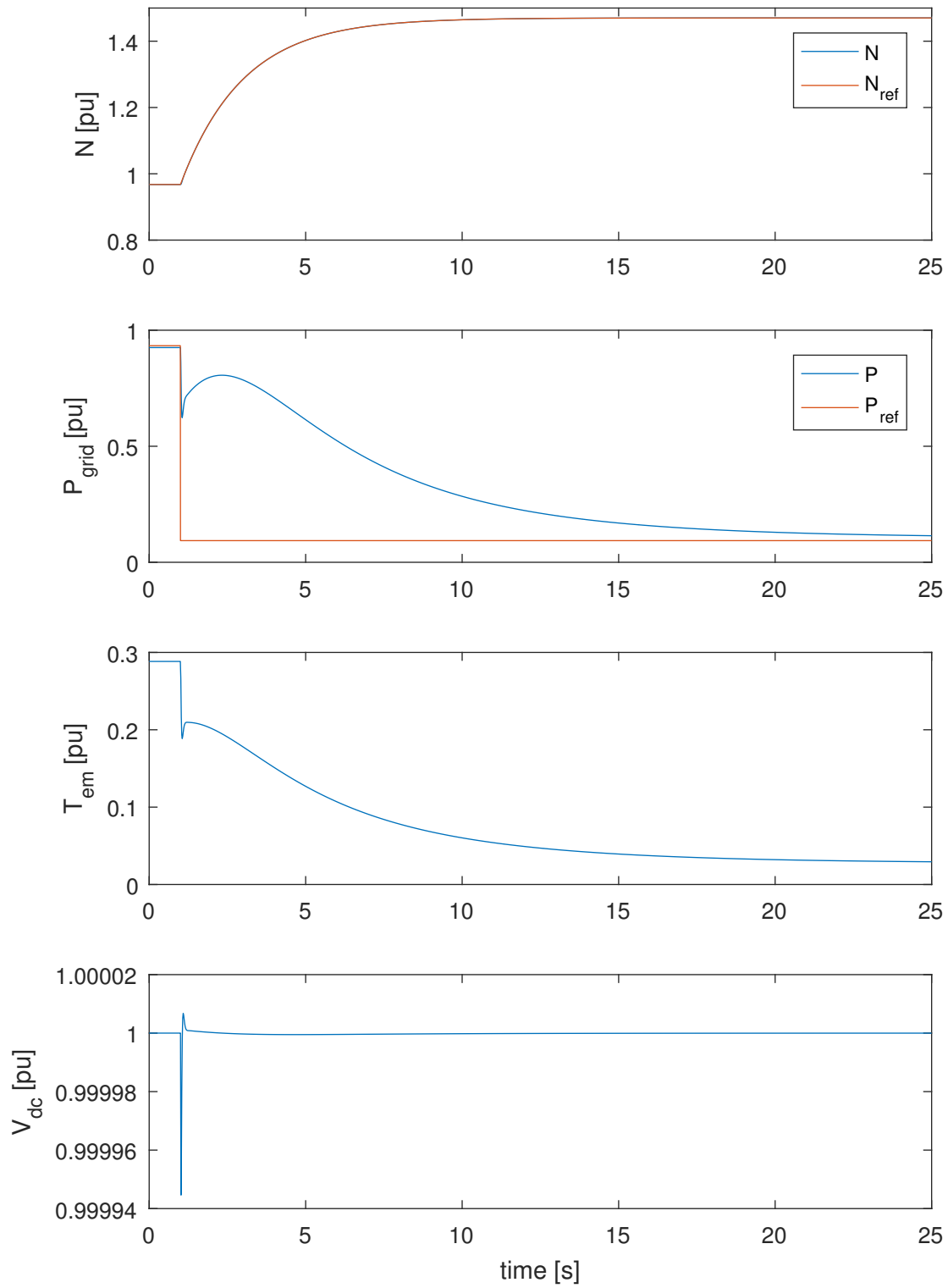


Figure 4.11: With filter - power reference step from 0.93pu to 0.1pu - turbine (I).

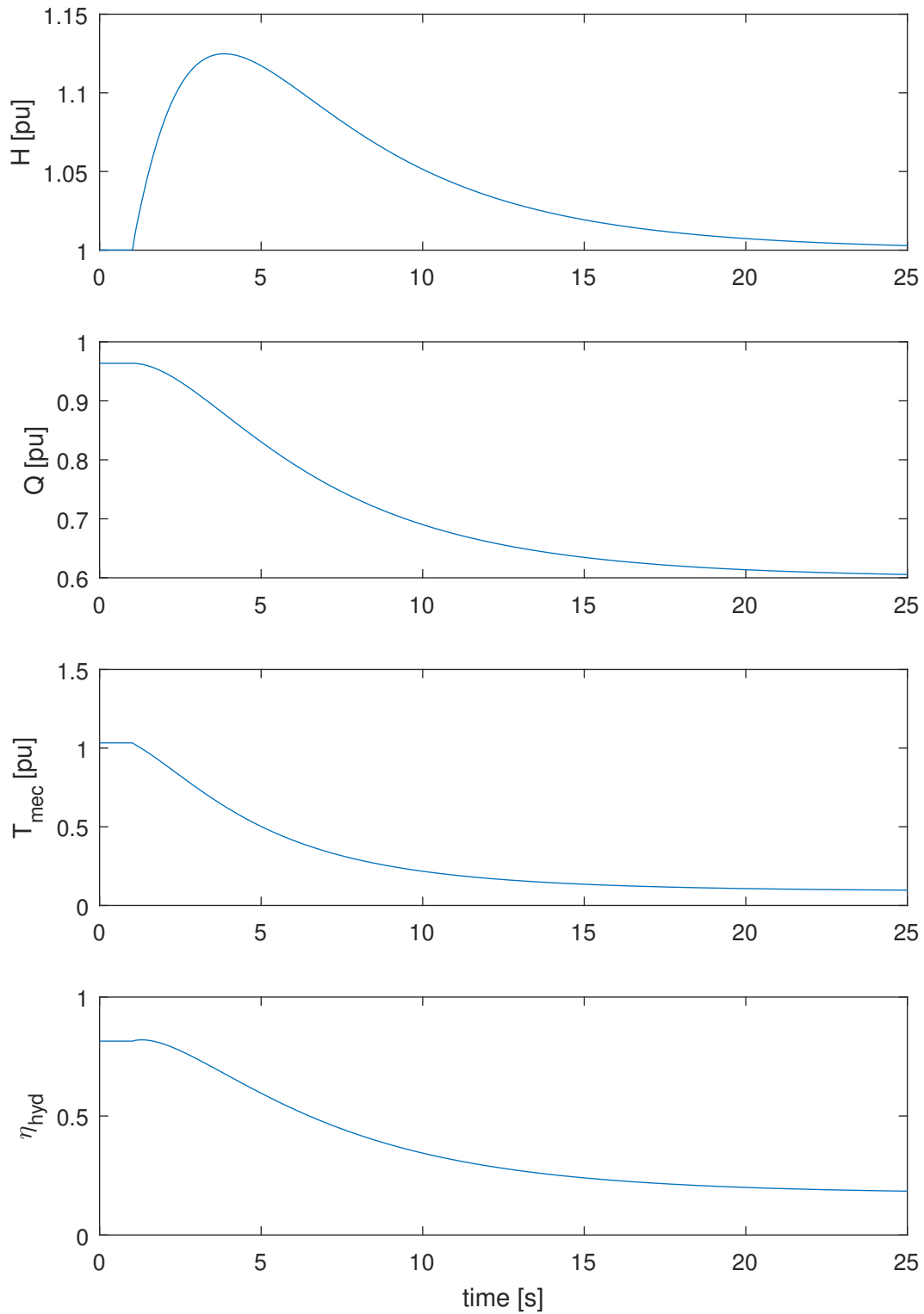


Figure 4.12: With filter - power reference step from 0.93pu to 0.1pu - turbine (II).

4.2.4 Primary voltage control (RPR) and additional requirements

We still need to validate the ability to provide primary voltage control and to verify the additional grid connection requirements. As the voltage control depends on the normal operating range of the grid voltage, we here group the validation of the requirements defined in Sections 1.2 and 1.3.

In this final part, the sign convention for the presentation of the results is changed, in order to keep the sign coherence between the injection or absorption of active and reactive power. Power flows are always defined to be positive when absorbed from the grid and negative when injected into the grid. All the power curves are here normalised by the nominal power of the installation $P_n = 30kW$, the only exception being the active power in turbine mode. In this case, it is normalised by the maximum turbinning power $P_{t,max} = 7.5kW$.

Voltage tolerance

Section 1.3 defines the voltage amplitude tolerance range to be $\pm 10\%$ around the nominal voltage at the point of connection. This range corresponds to the normal operating conditions of the installation, in which case it is supposed to be able to deliver its maximum active power capacity, as well as react to a voltage difference with a droop reactive power control.

This normal behaviour of the system is tested by applying a voltage ramp at the PoC, from 0.9pu to 1.1pu, while pumping at the nominal conditions. This indeed represents the most constraining situation, as it corresponds to the maximum power flow through the converter. Figure 4.13 shows the results of this fifth simulation. We observe that all the grid requirements are here again satisfied by the installation.

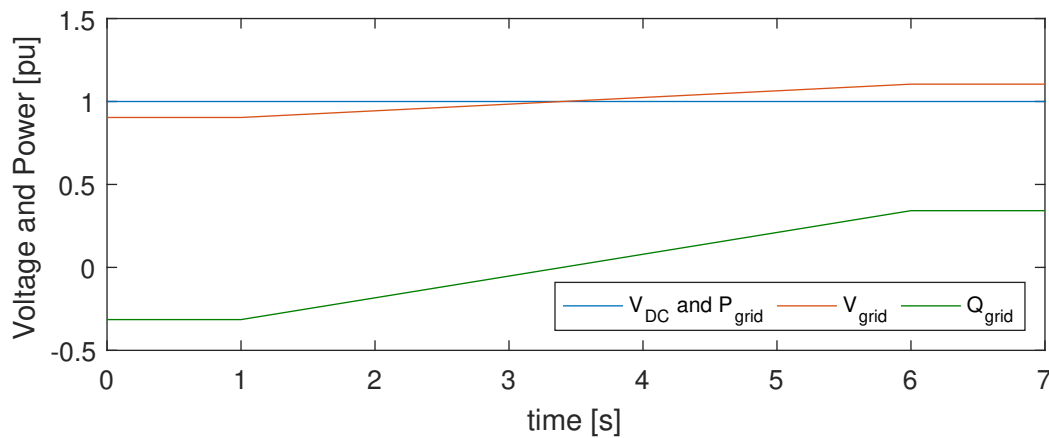


Figure 4.13: Voltage tolerance validation.

We notice in Figure 4.13 that V_{dc} and P_{grid} both remain totally steady throughout the simulation. This highlights the proper consumption of the nominal power at the machine, as well as the total transmission of this active power through the back-to-back converter.

Then, the automatic reactive power droop control implemented at the grid-side converter also shows tangible results. Its purpose is to help sustain the grid voltage in case of a dip, by injecting reactive power into the grid accordingly. This behaviour is indeed verified, as the reactive power Q_{grid} linearly varies from -0.33pu, when the PoC voltage equals 0.9pu, to +0.33pu when the grid voltage reaches 1.1pu.

Only the grid-side controller plays a role in the regulation of the installation in case of voltage variations. For this reason, a voltage variation in turbine mode gives exactly the same results.

FRT requirements

Section 1.3 defines the typical low-voltage ride-through characteristic that an installation must be able to overcome, while operating at the nominal working point. Instead of the FRT curve given in 1.3, the considered symmetrical three-phase voltage dip profile is given in Figure 4.14. This profile is tested on the system during pumping and turbining operation. It is chosen to be even worse than the ENTSO-E requirements for the sake of the installation robustness.

Please observe that only symmetrical three-phase drops are tested here. The study of unbalanced dips is out of the scope of this document and left for further research.

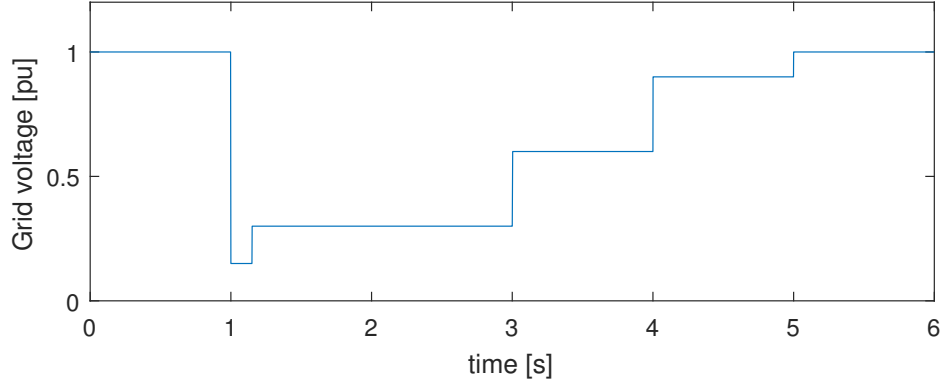


Figure 4.14: Tested voltage dip profile. Inspired from Figure 1.3.

- **Pump mode :** In pump mode, a voltage dip leads to a decrease of the DC-link voltage, due to the limitation of the power transmitted through the grid-side controller. The stability analysis of the system after the voltage recovery uses the unitary power factor nose curve shown in Figure 4.15. In the *Negundo* case, the maximum converter voltage on the nose curve is approximately 1.0pu, leading to the sufficient condition of V_{dc} being equal to 0.89pu in order to ensure stability after the recovery.

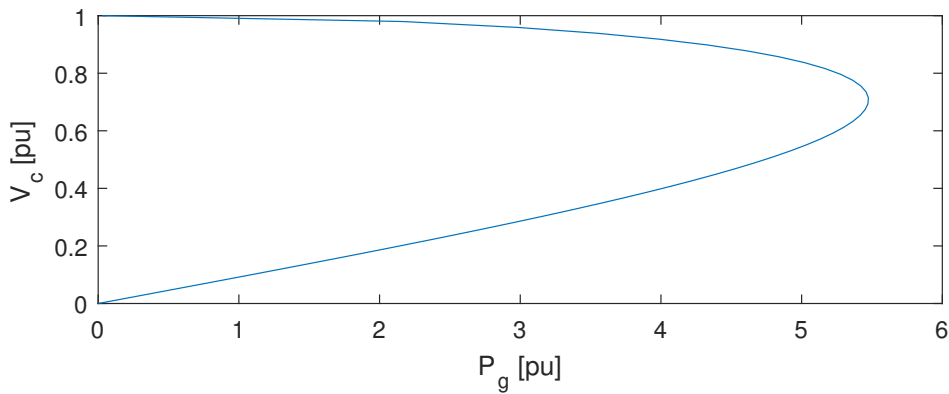


Figure 4.15: Nose curve at unitary power factor for the *Negundo* installation.

In the case of the voltage dip profile of Figure 4.14, it is impossible to maintain the DC voltage with the usual power control algorithm with. The adapted algorithm of Section 3.3 is then applied to the system under simulation of the voltage dip profile in pump mode. The results of this sixth simulation are shown in Figure 4.16.

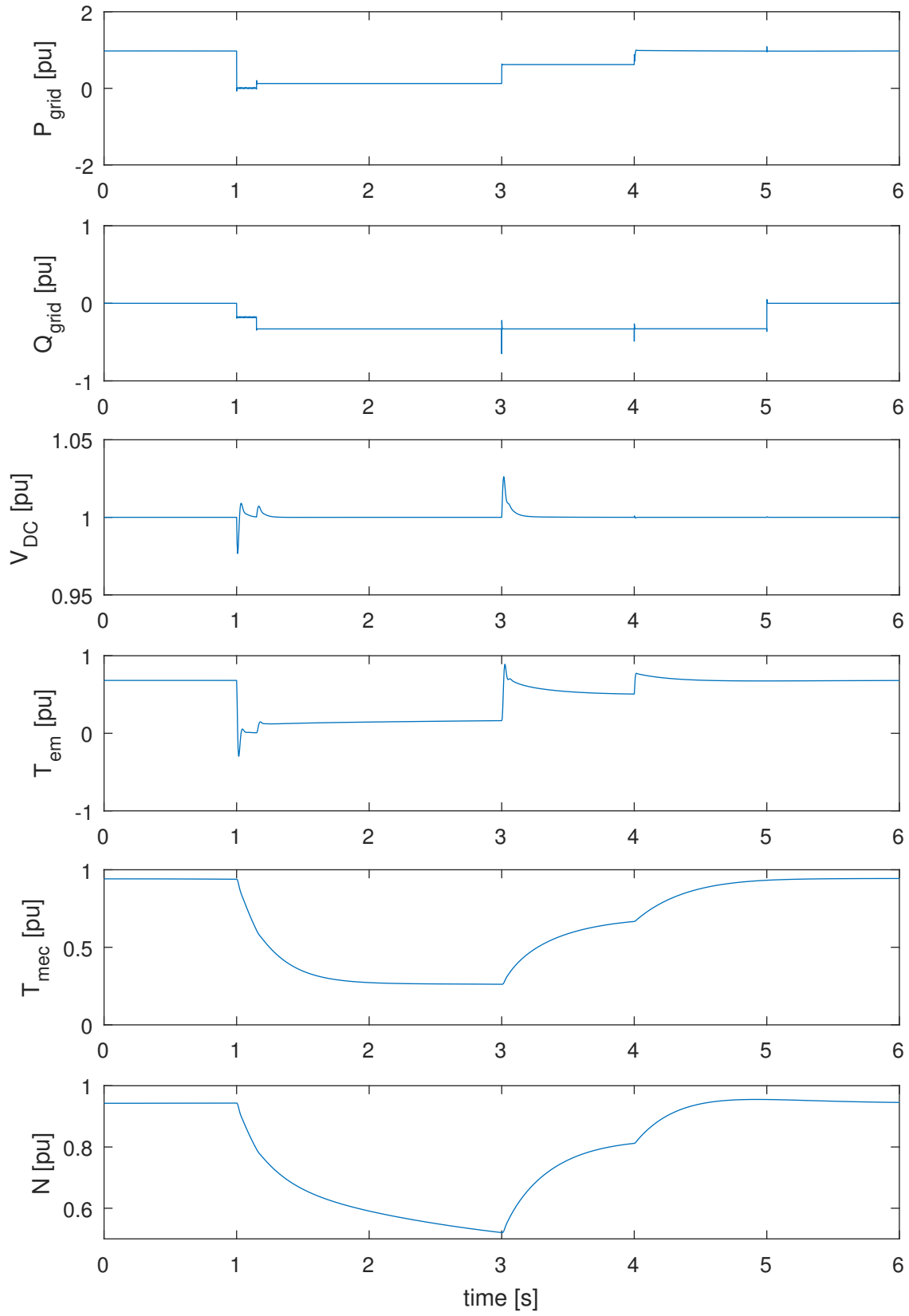


Figure 4.16: Voltage dip of Figure 4.14 - FRT capacity test - pump.

The first observations of the results presented in Figure 4.16 concern the exchanged powers with the grid P_{grid} and Q_{grid} . When the dip occurs, the power absorbed from the grid saturates at the grid-side converter. The priority is then given to reactive power. Afterwards, if the maximum value of the reactive power $Q_{max} = 0.33 \cdot P_n$ does not yet saturate the converter current, the remaining part of the current is used to absorb active power from the grid. Between $1s$ and $1.15s$, the remaining voltage is equal to $0.15pu$. The converter current limit does not allow to provide the maximum reactive power in this case. The reactive power is limited to $0.17pu$ and there is no active power exchanged. When the voltage recovers after $1.15s$, the maximum reactive power is transmitted to the grid, while more and more active power is absorbed.

Concerning the DC-link voltage, one notices that the voltage stays close to its nominal value, not exceeding 3% of variation. This is due to the control strategy used during the fault. The DC voltage is regulated by the stator-side controller in this case. Therefore, the active power developed by the pump is adapted in order to keep a constant DC voltage.

The last remark concerns the small peaks of the reactive power when the depth of the dip changes, typically at $t = 3s$ and $t = 4s$. This is due to the discontinuities in the voltage profile at the PoC and the small inertia of the current regulating loops of the grid-side controller. In reality the voltage dips profiles are not discontinuous as in Figure 4.14. Therefore, these variations leading to excessive current in the converter are not present.

- **Turbine mode :** In turbine mode, active power is produced by the installation. The saturation of the power transmission at the grid-side in case of a fault leads to an increase of the DC-link voltage. As V_{dc} cannot rise indefinitely, a braking chopper is added to the installation, allowing to sustain the voltage dip. The last simulation results are given in Figure 4.17. They correspond to the application of the FRT voltage profile in Figure 4.14 to the system in turbine mode. In this case, the results are all normalised by the turbine per unit system of Table 4.8, except for Q_{grid} and $P_{chopper}$, which are normalised by P_n . $P_{chopper}$ is the power dissipated in the chopper resistance. All the other variables are not affected by the voltage dip because of the absence of a DC voltage drop.

Observing the powers in Figure 4.17, one notices a similar behaviour of P_{grid} and Q_{grid} to the one in pump mode, except that the active power is now injected into the grid. Between $3s$ and $3.04s$, there is even more active power transmitted to the grid than in the nominal conditions, in order to recover the nominal DC-link voltage.

Regarding the DC-link, we observe that its voltage never exceeds the pre-defined value of $1.05pu$ thanks to the chopper resistance of 100Ω , which is connected in parallel with the DC capacitance when the DC link voltage crosses the $1.05pu$ threshold, and disconnected when it comes back to its nominal value. The power dissipated by the chopper resistor oscillates between 0 and $0.25pu$, depending on the DC voltage crossing the threshold or not. The braking chopper resistance is sized to dissipate the maximum power produced by the PaT in turbine mode when the DC voltage is around $1.05pu$.

$$R_{chopper} = \frac{(1.05V_{dc})^2}{P_{t,max}} = \frac{(1.05 \cdot 750)^2}{7.5e3} \simeq 100\Omega$$

We here again notice the small power peaks at the times of the voltage discontinuities, for the same reason explained for the pump mode.

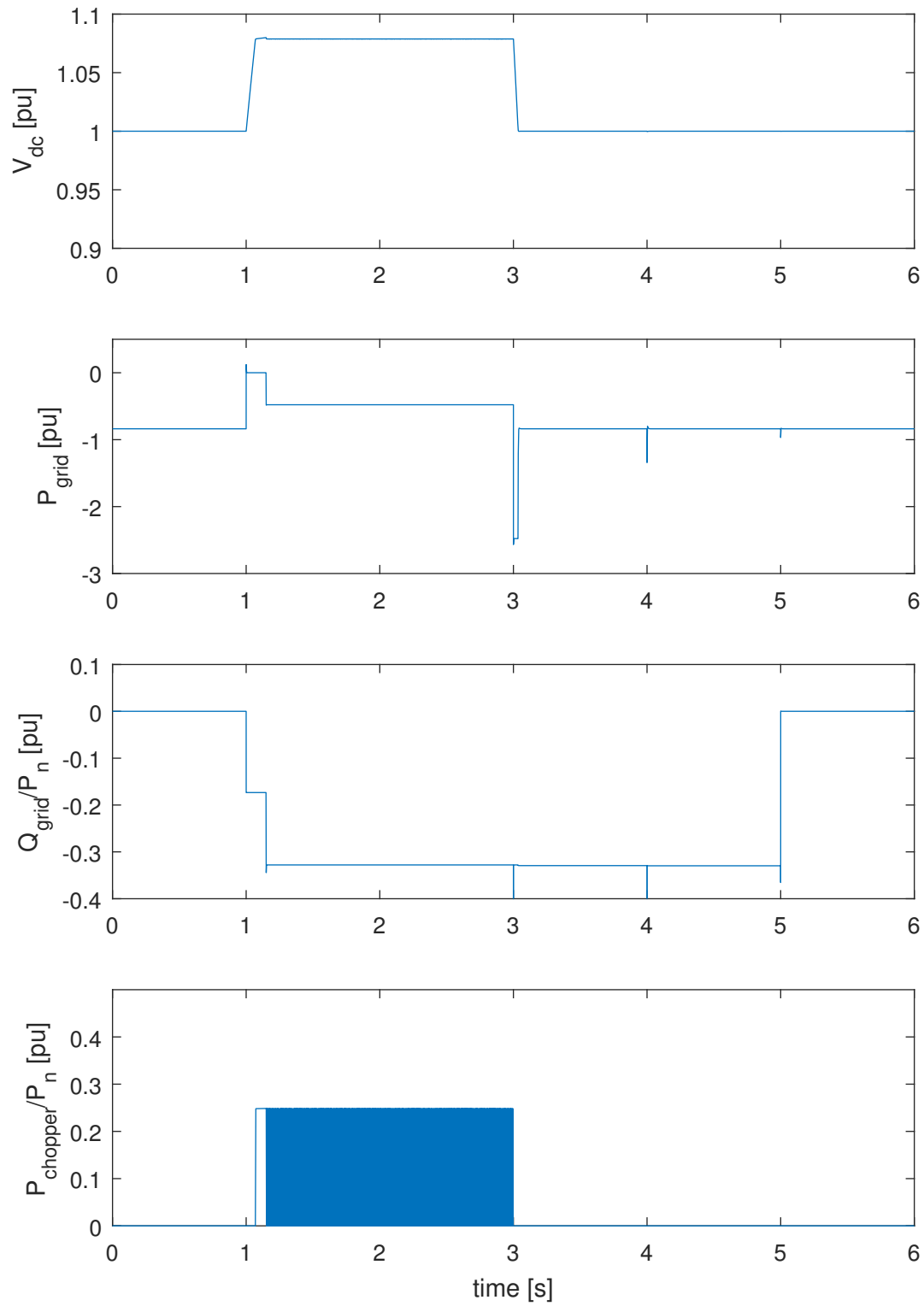


Figure 4.17: Voltage dip of Figure 4.14 - FRT capacity test - turbine.

Conclusion

Local variable-speed pumped-hydroelectricity energy storage proves to be an appropriate solution to the additional needs of reserves to ensure the grid stability and security of supply due to the rising influence of intermittent and decentralised renewable energy sources. In the present document, we provided a *Matlab/Simulink* model of such a PHES unit, using a PaT as hydraulic machine and a squirrel cage induction machine as electro-mechanical converter. Two models of the water column were established : a simple inelastic implementation and its more accurate elastic equivalent. This system is then connected to the LV grid via a back-to-back power converter enabling the variable-speed of the machine. An accurate and flexible control algorithm was designed for ensuring the regulation of the active and reactive power flows. This manoeuvrability of the installation was meant for the provision of frequency containment and voltage control reserves to the grid operators. The transient simulation of the particular *Negundo* study case gave the following results.

First of all, the comparison of the elastic and inelastic hydraulic models under the application of a power reference step in pumping conditions lead to the conclusion that the relatively smooth rotational speed profile due to the rotor inertia induces a negligible behaviour of the pressure wave propagation on the electrical characteristics. Hence, despite its relevant application to the dimensioning of the penstock components, the elastic model does not provide any advantage over its inelastic equivalent in the study of ancillary services supply.

Secondly, the validity of the averaged model replacing the full back-to-back converter was proven by comparing the averaged electrical characteristics of the machine with the ones integrating the accurate PWM control of the converter switches. We observed that the averaged model indeed lead to electrical signals being the average of their equivalents with the converter implemented and thus simply neglects the harmonic content of the signals. We therefore assume the averaged equivalent to be sufficient for the scope of this study. The rest of the results characterising the ability of the installation to verify the grid operator requirements determined in Section 1 thus makes use of the inelastic model of the water column and of the converter averaged model.

The first simulations relating to ancillary services consisted in the testing of the capacity band allocated for FCR supply in both pump and turbine modes. Considering the case of a 100% allocation of the active power range to FCR, we tested the ability to follow a power step from 0.95pu to 0.2pu in pumping conditions and observed a response time of 0.02 seconds. In pump mode, the direct power control algorithm indeed allows to adapt the consumed electrical power almost instantly. Similarly, the turbinning conditions were tested by applying a step from 0.93pu to 0.1pu. In this case, the response time is closer to 20 seconds. This huge difference between the pumping and turbinning response times is due to the different control algorithms used. Indeed, the direct power control via the electromagnetic torque is unstable in turbine mode, requiring the adding of a speed controller. We conclude that the *Negundo* site is then well suited for the provision of FCR according to the requirements defined in Section 1.1.

Afterwards, the linear time variation of the voltage amplitude at the PoC between 0.9pu and 1.1pu while pumping at the nominal power highlighted the steadiness of the active power transmitted across the converter throughout this voltage range. Moreover, this simulation validated the reactivity of the voltage droop control method for providing RPR and sustain the voltage grid. The reactive power control is therefore well designed to verify the requirements of Section 1.2.

Finally, the system was analysed during the occurrence of deep three-phase balanced voltage dips. In this case the main control strategy needed to be adapted in order to overcome the fault and verify the expected FRT behaviour, whose curve is given in Section 1.3. In pump mode, the reduction of the active power reference controlling the stator-side controller to match the maximum transmissible power through the converter makes it possible to avoid the tripping of the installation. While turbinning, the adding of a chopper resistor in parallel with the DC-link capacitor enables the surplus amount of power produced at the machine-side to be evacuated as heat and to overcome the fault. During the fault, priority is given to the reactive droop control in order to sustain the grid voltage. The transmitted active power uses the remaining capacity, which increases proportionally to the voltage recovery.

At the end of this study, one might be willing to pursue the analysis of variable-speed PHES using PaTs. In this sense, the authors provide the following **recommendations for further research** :

- The first proposition concerns the actual experimental *Negundo* site. When fully operational and enabling to recover experimental data, the comparison of this information with our simulations will allow to validate the model used for the hydraulic pump and especially the expected behaviour in turbinning conditions. Then, the control algorithm implemented in *Matlab/Simulink* could be tested via *hardware-in-the-Loop* using the real converter. The operating range of the PaT could also be obtained experimentally and compared to a prediction model.
- The second guideline involves the testing of the FRT capability of the installation. Only balanced three-phase faults were tested on the model. The application to imbalanced faults would require to adapt the control algorithm to enable the installation to sustain the voltage against imbalances. The present control indeed only provides symmetrical responses and would end up injecting time-varying power into the grid.
- Last but not least, our system presents valuable assets for its applications to islanded operations of microgrids. The characterisation of its operation with sole RES production is at stake for the future. One of the main forecasted challenges in this case will be the definition of a voltage reference somewhere in the microgrid. The presence of the variable-frequency drive indeed decouples the machine from the grid and greatly reduces the equivalent rotating mass inertia stabilizing the grid frequency.

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Appendix A

Power control instability in turbine mode

This appendix explains the reasons why the direct power control of a PaT in turbine mode is impossible. The concept of a direct power control is to adapt the electromagnetic torque of the electrical machine in order to maintain the active power produced by the pump equal to a reference power. Two main simplifications are done in order to demonstrate the instability:

- **Constant flow rate** : the flow rate changing very slowly compared to the speed during the transient due to the slow water column dynamics, the variations of the flow rate are neglected.
- **Instantaneous electrical control** : the electrical characteristics being much faster than the mechanical system, we consider that the electromagnetic torque is always equal to its reference value defined by the controller.

Using these two main simplifications, the system can be represented by the block diagram in Figure A.1. The transfer function $G(s)$ links the rotor speed to the hydro-mechanical and electromagnetic torques. J and F are respectively the rotor inertia coefficient and its friction factor. $H(s)$ is the PAT transfer function, which depends on the PaT characteristic curve $\pi(\phi)$. The system being nonlinear, the classical stability analysis cannot be performed.

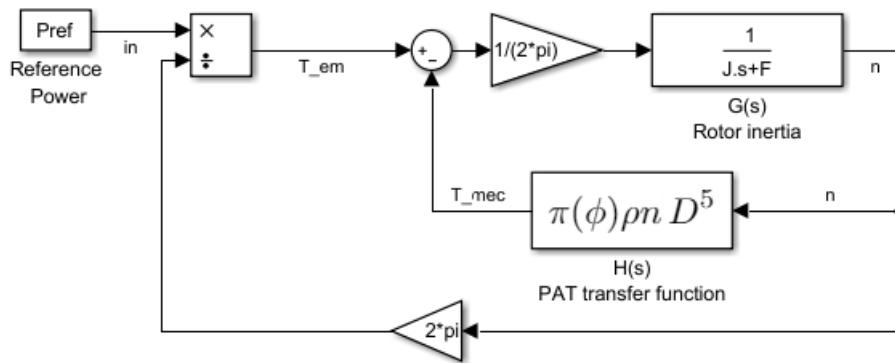


Figure A.1: System block diagram for performing the stability analysis.

The purpose of a direct power control is to keep the electrical active power P_e constant and equal to a setpoint. Neglecting the electromagnetic losses in the generator, we derive the following active power expression, central to the notion of direct power control.

$$P_e = 2\pi T_{em} n \quad (\text{A.1})$$

A steady-state working point is characterised by a state where the electromagnetic torque is equal to the hydro-mechanical one, to the friction losses. When this condition is fulfilled, the speed becomes constant according to equation A.2 and a steady state working point is reached.

$$(2\pi)J\frac{dn}{dt} = T_{mec} - T_{em} - 2\pi nF \quad (\text{A.2})$$

This working point is considered stable if a small variation of the electromagnetic or mechanical torque leads to stability. It means that a small initial perturbation do not end up stopping or accelerating the machine to infinity, bringing instability. Hence, we must find expressions of the electromagnetic T_{em} and mechanical T_{mec} torques as functions of the rotational speed n .

Equation A.1 provides the basis to recover the differential relation between T_{em} and n .

$$dP_e = 2\pi(ndT_{em} + T_{em}dn) = 0 \quad \Leftrightarrow \quad \frac{dT_{em}}{T_{em}} = -\frac{dn}{n} \quad (\text{A.3})$$

Equation A.3 thus gives an expression of the electromagnetic torque variation as a function of the speed. The expression of the mechanical torque variation must now be obtained, as a function of the speed and of the PaT characteristics.

$$T_{mec}(\phi, n) = \left(\frac{1}{2\pi}\right) \pi(\phi) \rho n^2 D^5 \quad (\text{A.4})$$

where

- $\pi(\phi)$ is the power adimensional turbine characteristic.
- $\phi = \frac{Q}{nD^3}$ and $\frac{d\phi}{dn} = \frac{-Q}{n^2D^3} = -\frac{\phi}{n}$, as we consider Q constant because of its slow dynamics.
- $\left(\frac{1}{2\pi}\right)$ is the constant used to transform the speed from rps to rad/s. Please do not mistake the π constant with the PaT curve $\pi(\phi)$.

Taking the speed derivative of Equation A.4, we get

$$\frac{dT_{mec}}{dn} = \frac{\rho D^5}{(2\pi)} \left(\frac{d\pi}{d\phi} \frac{d\phi}{dn} n^2 + 2\pi n \right) \quad \Leftrightarrow \quad \frac{dT_{mec}}{T_{mec}} = -\frac{dn}{n} \left(\frac{d\pi}{d\phi} \frac{\phi}{\pi} - 2 \right) \quad (\text{A.5})$$

Injecting the electromagnetic torque expression of equation A.3 into the torque expression equation A.5, we get equation A.6 which gives a speed independent relation between the mechanical and the electromagnetic torque.

$$\frac{dT_{mec}}{T_{mec}} = \frac{dT_{em}}{T_{em}} \left(\frac{d\pi}{d\phi} \frac{\phi}{\pi} - 2 \right) \quad (\text{A.6})$$

In order to have a stable system, a small electromagnetic torque variation must lead to a greater variation, in the same direction, of the mechanical torque. Therefore, the stability criterion is the following.

$$\boxed{\left(\frac{d\pi}{d\phi} \frac{\phi}{\pi} - 2 \right) > 1} \quad (\text{A.7})$$

Figure A.2 shows a plot of the left part of Inequality A for the hydraulic characteristic of the *Negundo* installation. One can note that the stability criterion is fulfilled only for values of ϕ close to zero. Indeed, we see in Figure A.2 that $\phi_{min} = -0.19$. Considering Q as constant, this means that only the higher speeds are stable.

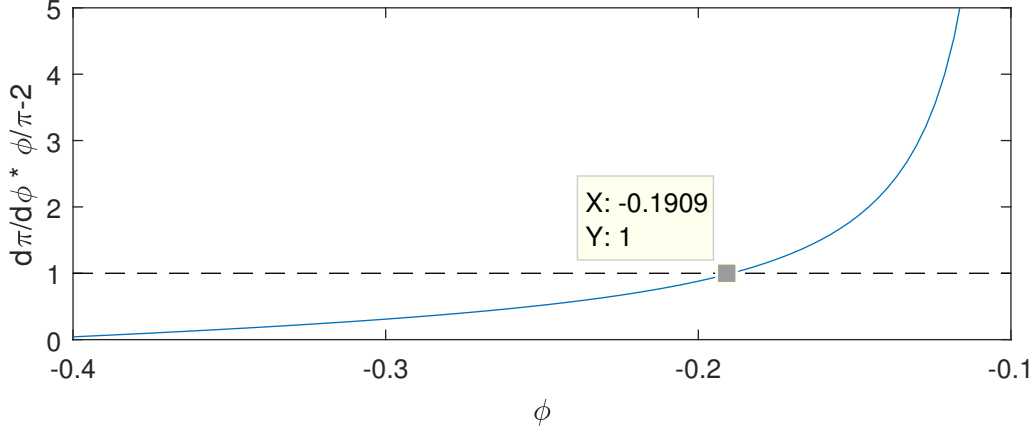


Figure A.2: Direct power control stability criterion - turbine mode.

This stability condition is confirmed using the block diagram implementation of Figure A.1. In order to do so, we observe the temporal evolution of the speed and the flow rate variable, under a deviation of 1% of the electromagnetic torque imposed at $t = 1s$, for different initial working points around the stability limit. Figure A.3 presents the results confirming the stability criterion expressed by Equation . We indeed observe that $\phi_{min} = -0.19$ is a critical point, as the higher values of ϕ in absolute terms lead to the stopping of the machine.

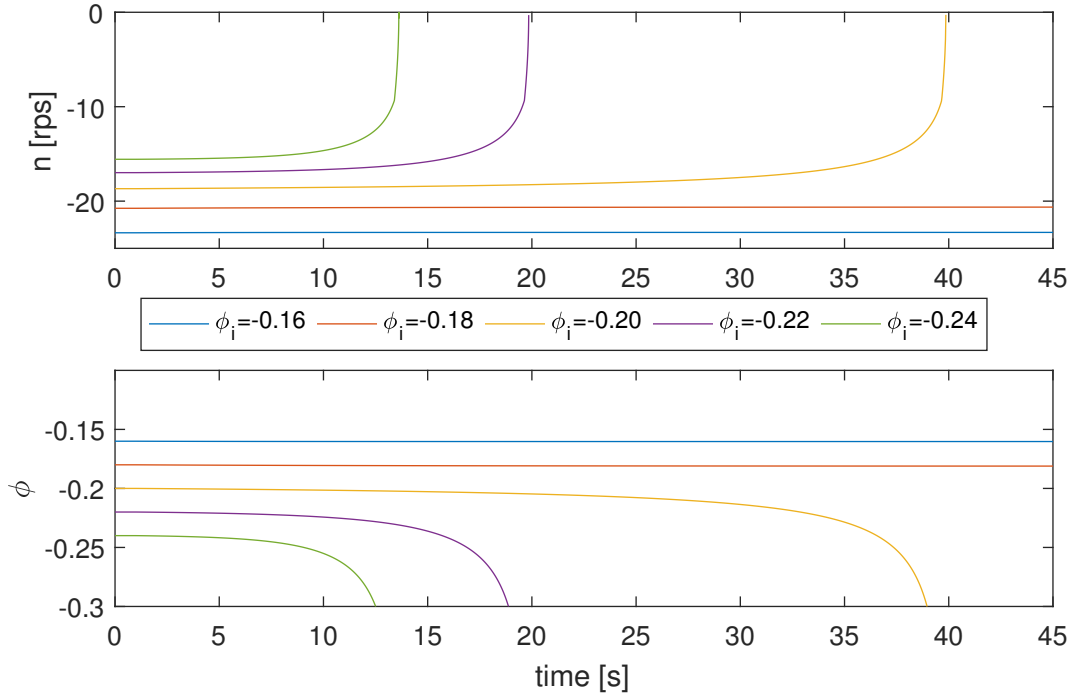


Figure A.3: Stability analysis for a deviation $\Delta T_{em} = 1\%$ at $t = 1s$, for different initial working points.

If the system is stable for some ϕ value, one solution to overcome the instability is to reduce the working range of the PaT to the stable range only. In this case, a direct power control with the electromagnetic torque T_{em} can be operate without stability issues. This solution is however not convenient for the following two main reasons :

- The efficiency is very low and decreases quickly for low ϕ values (in absolute terms). Moreover, the BEP corresponds to an unstable working point. Figure A.4 shows the turbinning efficiency as a function of ϕ .
- The second reason is linked to the first. As the efficiency is very low for values of ϕ close to zero, the maximum power point P_{max} at fixed available head H_{res} is not reached for the considered ϕ . The maximum power working point is indeed reached for ϕ values higher than the ϕ_{BEP} in absolute terms. Therefore, limiting the working range reduces significantly the power range in turbine mode.

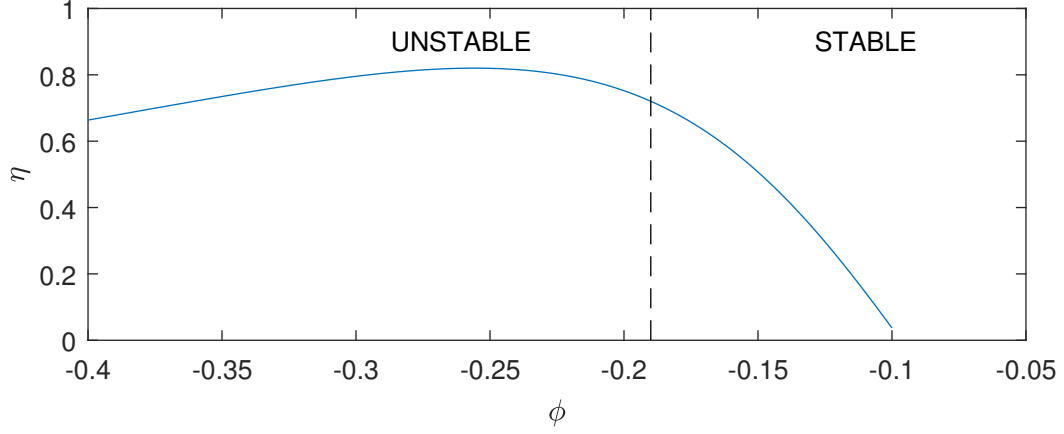


Figure A.4: Turbine efficiency and stability as a function of ϕ .

Appendix B

Magnetising the IM : choice of i_{sd}^*

The d stator current i_{sd} permits to magnetise the induction machine. In this appendix we determine the value of i_{sd} allowing to develop a maximum power. The following development is here directly adapted to the *Negundo* case. It can however be easily changed in order to fit any other pump and/or induction machine.

There are two limitations on the induction machine. The rated stator current I_{rated} and the nominal stator voltage $V_{rated} = V_n$. Equation B.1 highlights the first condition. The factor multiplying I_{rated} is due to the fact that the latter is a *RMS* value and that the *dq transform* used does not conserve the amplitude. We notice that it corresponds to a circle equation in the $\{i_{sd}, i_{sq}\}$ plane, centred on the origin and having a radius of $\sqrt{3}I_{rated}$.

$$i_{sd}^2 + i_{sq}^2 \leq (\sqrt{3}I_{rated})^2 \quad (\text{B.1})$$

In order to express the maximum voltage condition in steady-state, the simplification of the stator voltage equations developed in Section 2.2 is needed. These equations are retrieved hereunder.

$$\begin{cases} u_{sd} = R_s i_{sd} + \sigma L_{cs} \frac{di_{sd}}{dt} + \frac{L_m}{L_{cr}} \frac{d\phi_{rd}}{dt} - \dot{\xi}_s \sigma L_{cs} i_{sq} \\ u_{sq} = R_s i_{sq} + \sigma L_{cs} \frac{di_{sq}}{dt} + \dot{\xi}_s (\sigma L_{cs} i_{sd} + \frac{L_m}{L_{cr}} \phi_{rd}) \end{cases}$$

Equation B.2 is also needed to link the d-current to the rotor flux.

$$\phi_{rd} = -\frac{L_{cr}}{R_r} \frac{d\phi_{rd}}{dt} + L_m i_{sd} \quad (\text{B.2})$$

Defining the constant value σ , remembering that the steady-state conditions involve that all the time derivatives can be removed from the previous equations and injecting the steady-state relation between ϕ_{rd} and i_{sd} , we obtain

$$\begin{cases} u_{sd} = R_s i_{sd} - \dot{\xi}_s \sigma L_{cs} i_{sq} \\ u_{sq} = R_s i_{sq} + \dot{\xi}_s L_{cs} i_{sd} \end{cases} \quad (\text{B.3})$$

Equations B.3 thus give a relation between the steady-state stator voltages and currents. The maximum stator voltage condition is expressed in Equation B.4. The same normalising factor $\sqrt{3}$ is used for the same reason as in the current case.

$$u_{sd}^2 + u_{sq}^2 \leq (\sqrt{3}V_{rated})^2 \quad (\text{B.4})$$

Injecting the voltage expressions of Equation B.3 into Inequality B.4, gives the following condition B.6 in terms of the stator currents i_{sd} and i_{sq} . One can notice that its an ellipse equation centred on the origin in the $\{i_{sd}, i_{sq}\}$ plane. This ellipse depends of stator reference speed $\dot{\xi}_s$.

$$(R_s i_{sd} - \dot{\xi}_s \sigma L_{cs} i_{sq})^2 + (R_s i_{sq} + \dot{\xi}_s L_{cs} i_{sd})^2 \leq (\sqrt{3} V_{rated})^2 \quad (B.5)$$

$$(R_s^2 + \dot{\xi}_s^2 L_{cs}^2) i_{sd}^2 + (R_s^2 + \dot{\xi}_s^2 L_{cs}^2 \sigma^2) i_{sq}^2 + 2 R_s \dot{\xi}_s L_{cs} (1 - \sigma) i_{sd} i_{sq} \leq (\sqrt{3} V_{rated})^2 \quad (B.6)$$

Neglecting the slip, we consider that the stator reference speed $\dot{\xi}_s$ is linked to the mechanical speed n by the number of pole pairs p .

$$\dot{\xi}_s \simeq p \cdot 2\pi n \text{ [rad/s]} \quad (B.7)$$

We are looking for the maximum power conditions, meaning the point (i_{sd}, i_{sq}) which maximises the power under the two constraints given by Inequalities B.1 and B.6. Using the PaT characteristics and a similar procedure as the one explained in Section 3.1 for obtaining the turbining power-to-speed relation, the pumping speed-to-torque steady-state characteristic is recovered and shown in Figure B.1.

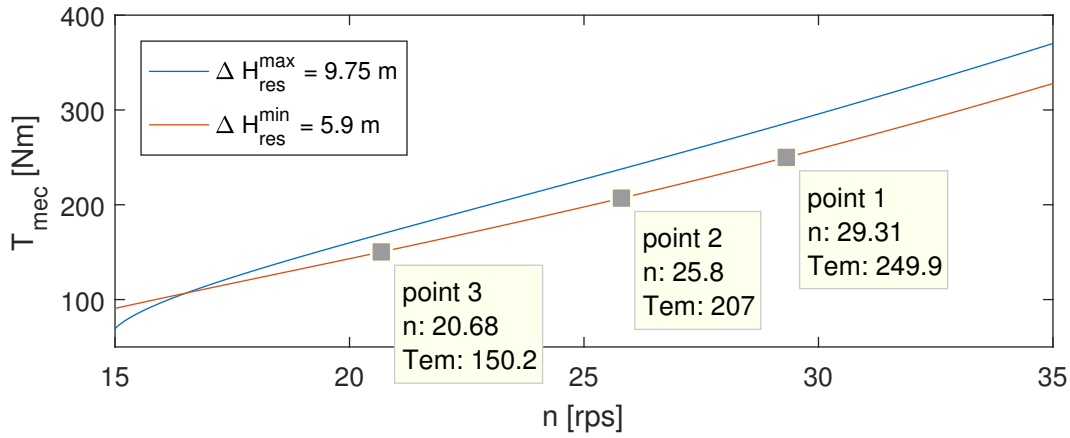


Figure B.1: PaT torque-to-speed relation - pump mode.

Figure B.1 shows that the torque always increases with the speed. Therefore, maximising the power is equivalent to maximising the hydro-mechanical torque T_{mec} . In steady-state, neglecting the friction losses, we assume the following relation.

$$T_{mec} = T_{em} = p \frac{L_m^2}{L_{cr}} i_{sd} i_{sq} \quad (B.8)$$

Seeing the steady-state expression of the electromagnetic torque, we observe that the iso-torque lines present a hyperbole shape in the $\{i_{sd}, i_{sq}\}$ plane. These are represented in Figure B.2 for several values of the torque, along with the circle representing Inequality B.1 and the ellipses formed by Inequality B.6 for several values of the speed. In this figure, the operating range of the machine in terms of d and q stator currents at a certain rotational speed corresponds to the intersection of the surfaces formed by the maximal current circle and the maximal voltage ellipse.

In Figure B.1, the torque-to-speed pump characteristic is shown for both the minimum and maximum steady state heads H_{res} of the *Negundo* installation. The minimum steady-state head case is the most limiting factor due to the fact that it then requires higher speeds for the same torque. A higher speed for the same torque indeed gives a higher constraint on by the maximal voltage condition, has shown in Figure B.2. Please notice that we choose the pump mode characteristic here, because it is the most constraining case. Indeed, the maximum power in turbine mode is lower than its pumping equivalent.

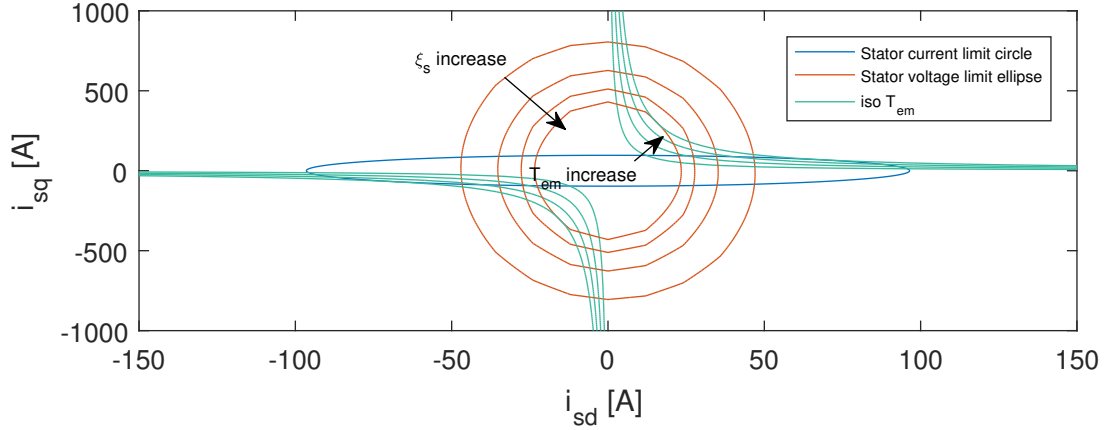


Figure B.2: Current, voltage and iso-torque relations in the $\{i_{sd}, i_{sq}\}$ plane.¹

The previous considerations allow to obtain the maximal power operating point graphically. From the observation of Figure B.2, we conclude that the maximal power operation point is obtained when the three curves intersect in a single point of intersection. Figures B.3, B.4 and B.5 show these 3 curves for 3 different hydro-mechanical torques T_m , corresponding to the desired mechanical power. These three points are also reported in Figure B.1.

For the first point, the iso-torque curve never respects both conditions simultaneously. Hence, this working point is unreachable by the machine.

In the case of the second torque, we observe that there is only one point of the iso-torque respecting both condition. This torque corresponds to the maximal admissible power of the machine. In order to reach this point, the direct stator current component i_{sd} has to be set at 26.74 A.

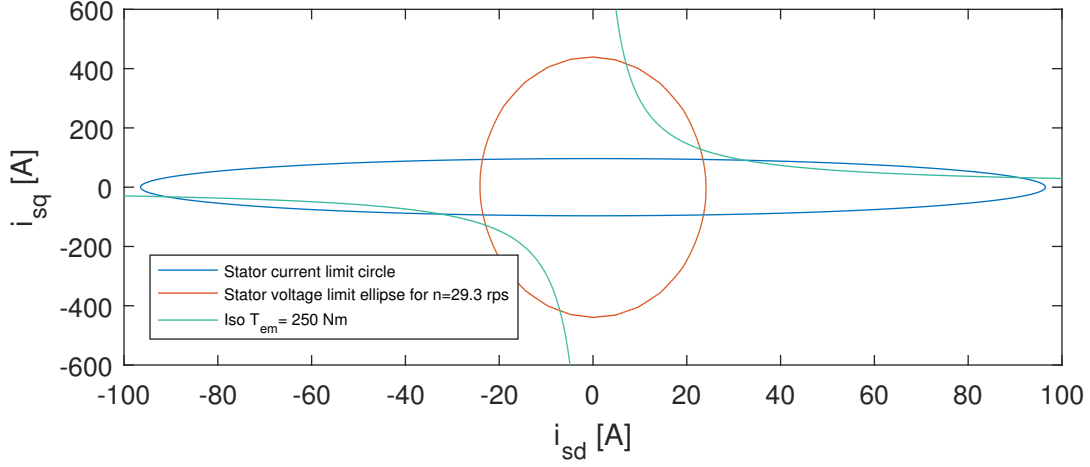
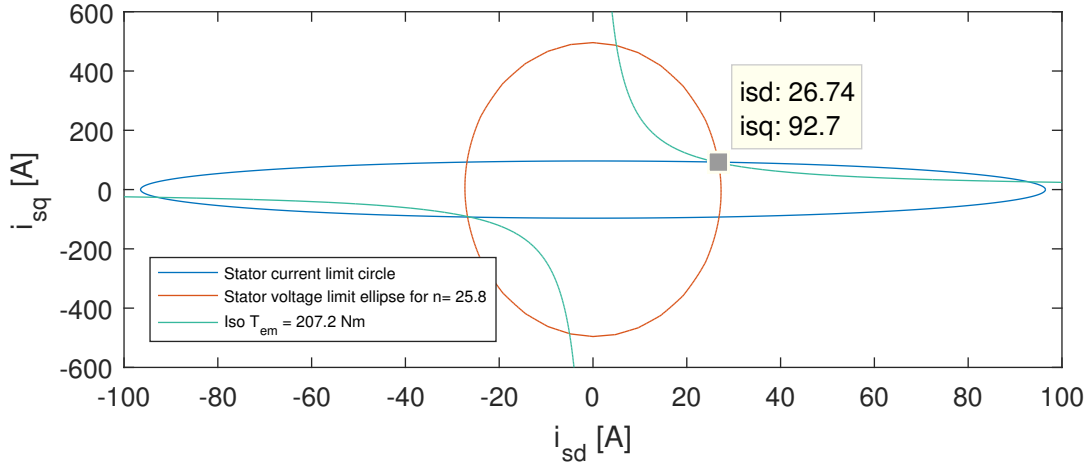
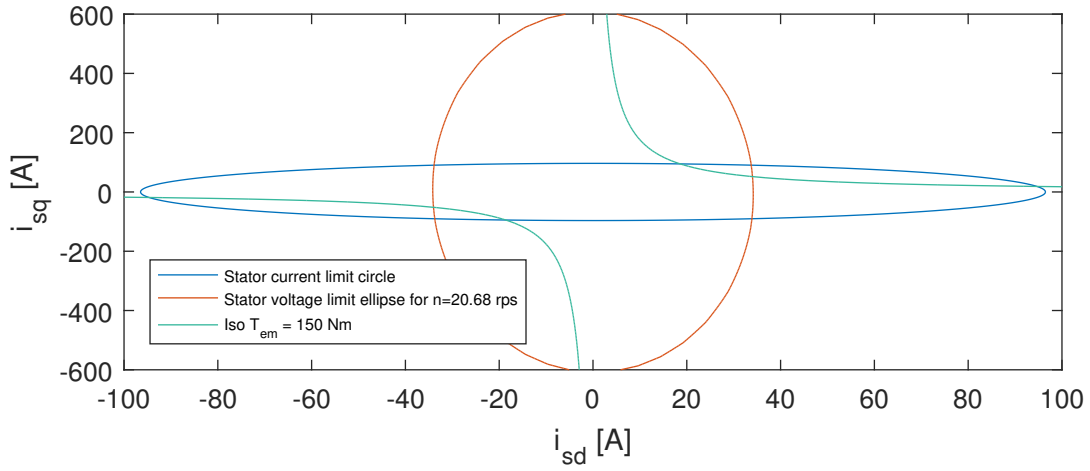
In the last case of a smaller torque than the maximum, we observe that there exists a small intersection region between the three surfaces, corresponding to several operating points. The previously obtained $i_{sd} = 26.74$ belongs to this region. We then conclude that all the points below the maximum torque are reachable using this value of i_{sd} . Indeed, the reduction of the torque leads to lower speeds, thus lower voltage constraints as shown in Figure B.2.

The present appendix shows that this method computes a value of i_{sd} that allows the reaching of the maximum output power of the system, as well as any other lower power. The maximum working points in the *Negundo* case are given in Table B.1.

Variable	Value	Unit
i_{sd}	26.74	[A]
i_{sq}^{max}	92.7	[A]
T_{em}^{max}	207	[Nm]
$n^{max}(\Delta H_{res}^{min})$	25.8	[rps]
$n^{max}(\Delta H_{res}^{max})$	23.6	[rps]
$P^{max}(\Delta H_{res}^{min})$	33.7	[kW]
$P^{max}(\Delta H_{res}^{max})$	30.8	[kW]

Table B.1: Maximum power working point.

¹Please do notice the two axes different scales, in order not to mistake the circle for an ellipse and the ellipses for circles.

Figure B.3: Current, voltage and iso-torque relations in the $\{i_{sd}, i_{sq}\}$ plane (point 1).Figure B.4: Current, voltage and iso-torque relations in the $\{i_{sd}, i_{sq}\}$ plane (point 2).Figure B.5: Current, voltage and iso-torque relations in the $\{i_{sd}, i_{sq}\}$ plane (point 3).

Appendix C

The direct-quadrature transform

We here develop the space vector model allowing us to transform the measured phase currents and voltages into their direct and quadrature components in the reference frame aligned with the rotor flux. The inverse transform is also required to switch back to the phase voltages corresponding their computed dq equivalents. The global *abc-to-dq* transform, also called the *dq* transform needs to be broken down into two separate operations.

The first step consists in finding the bi-directional space vector corresponding to the three-phase system. It is the *Concordia* transform.

The second step executes the rotation enabling the switching from the immovable reference frame to the frame aligned with the rotating flux linkage. It is the *Park* transform.

We simply focus on the direct and quadrature components, as the zero component does not influence the control of the stator side converter. This is explained in Section 2.2.

The *Concordia* transform

The *Concordia* transform is similar to the *Clarke* transform, except that the former preserves the power and not the amplitude. It consists in a projection of the three phase values onto two orthogonal directions : the α and β axes.

Both *Clarke* and *Concordia* transforms are expressed by the general expression of the space vector $\bar{\lambda}^s$ given below. This vector is a complex number split into its real and imaginary parts, respectively λ_α and λ_β .

$$\bar{\lambda}^s = \lambda_\alpha + j\lambda_\beta = K(\lambda_a + \lambda_b e^{j\frac{2\pi}{3}} + \lambda_c e^{j\frac{4\pi}{3}}) \quad (\text{C.1})$$

The greek letter λ used here is a generic term that can either be a current i or a voltage u . The superscript letter s stands for *space vector*. Please do not mix it with its subscript equivalent, indicating a *stator* value, as opposed to the *rotor* values.

In our case, the constant K is set to the value $\sqrt{\frac{2}{3}}$ in order to preserve the power. For the *Clarke* equivalent, K should be set to $\frac{2}{3}$.

Taking into considerations the fact that

$$e^{j\phi} = \cos(\phi) + j\sin(\phi) \quad (\text{C.2})$$

and that

$$\begin{cases} \cos(\frac{2\pi}{3}) = \cos(\frac{4\pi}{3}) = -\frac{1}{2} \\ \sin(\frac{2\pi}{3}) = \frac{\sqrt{3}}{2} \\ \sin(\frac{4\pi}{3}) = -\frac{\sqrt{3}}{2} \end{cases}$$

we obtain the following matrix representation of expression C.1

$$\begin{bmatrix} \lambda_\alpha \\ \lambda_\beta \end{bmatrix} = \sqrt{\frac{2}{3}} \begin{bmatrix} 1 & -\frac{1}{2} & -\frac{1}{2} \\ 0 & \frac{\sqrt{3}}{2} & -\frac{\sqrt{3}}{2} \end{bmatrix} \begin{bmatrix} \lambda_a \\ \lambda_b \\ \lambda_c \end{bmatrix} \quad (\text{C.3})$$

Figure C.1 shows the application of the space vector model to both stator and rotor windings. We hereby observe that the α -axis is well aligned with the axis of phase a and that the β -axis direction is set 90° counter-clockwise, just like the real and imaginary axes of any complex plane.

Re stands for the reference axis of the stator and rotor windings. They are both aligned with their respective phase a . The electric angle θ_{em} outlines the relative rotation of the rotor frame, by comparison to the fixed stator frame.

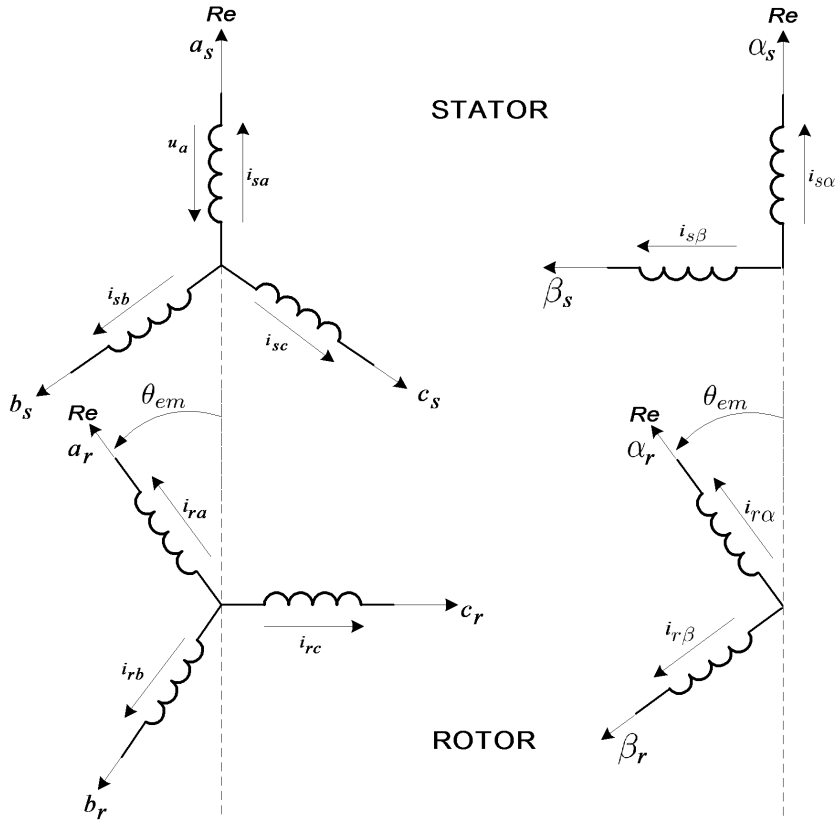


Figure C.1: Space vector model of both stator and rotor windings. Adapted from [31].

The *Park* transform

The *Park* transform consists in a change of the coordinate system. It takes any reference frame and rotates it by a given angle $-\xi(t)$, whose first two axes are called d and q for *direct* and *quadrature*.

The expression of the *Park* transform applied to the space vector of the previous section is the following.

$$\bar{\lambda} = \lambda_d + j\lambda_q = e^{-j\xi(t)} \bar{\lambda}^s$$

Using the complex exponential property C.2, the results from previous section and the multiplicative property of matrices, this expression becomes

$$\begin{bmatrix} \lambda_d \\ \lambda_q \end{bmatrix} = \sqrt{\frac{2}{3}} \begin{bmatrix} \cos(\xi) & \sin(\xi) \\ -\sin(\xi) & \cos(\xi) \end{bmatrix} \begin{bmatrix} 1 & -\frac{1}{2} & -\frac{1}{2} \\ 0 & \frac{\sqrt{3}}{2} & -\frac{\sqrt{3}}{2} \end{bmatrix} \begin{bmatrix} \lambda_a \\ \lambda_b \\ \lambda_c \end{bmatrix} \quad (\text{C.4})$$

The interest behind the application of this transform is to find the the right angle $\xi(t)$ to convert the sinusoidally time-varying values $\{\lambda_a, \lambda_b, \lambda_c\}$ into the continuous values of $\{\lambda_d, \lambda_q\}$. The continuous nature of the dq values is central in field-oriented control.

Figure C.2 superimposes both the stator and rotor space vector and *Park* coordinate systems to outline the fact that, if the *Park* transform angles are correctly chosen, the dq frames of the stator and rotor superimpose. This allows to precisely follow the rotating flux linkage and apply the control on the continuous current and voltage components in this frame.

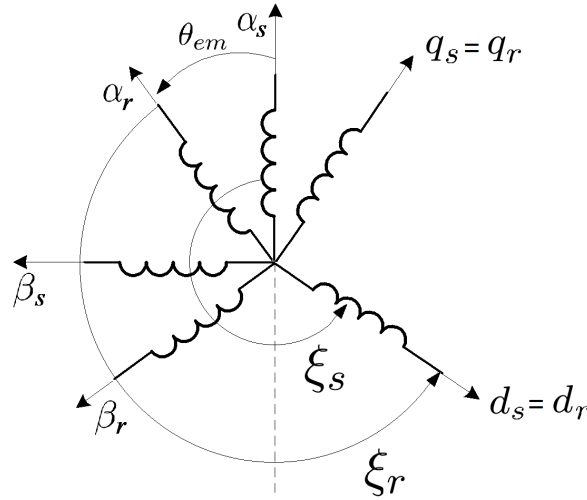


Figure C.2: Stator and rotor α - β and dq coordinate systems.

Stator side control : the dq transform

In our case, the direct dq transform is used to transform the measured stator phase currents $\{i_{sa}, i_{sb}, i_{sc}\}$ into there direct and quadrature components. We can directly apply equation C.4 using the specific stator angle ξ_s .

$$\begin{bmatrix} i_{sd} \\ i_{sq} \end{bmatrix} = \sqrt{\frac{2}{3}} \begin{bmatrix} \cos(\xi_s) & \sin(\xi_s) \\ -\sin(\xi_s) & \cos(\xi_s) \end{bmatrix} \begin{bmatrix} 1 & -\frac{1}{2} & -\frac{1}{2} \\ 0 & \frac{\sqrt{3}}{2} & -\frac{\sqrt{3}}{2} \end{bmatrix} \begin{bmatrix} i_{sa} \\ i_{sb} \\ i_{sc} \end{bmatrix} \quad (\text{C.5})$$

Once the control scheme is applied in the dq coordinate system, one needs to convert the computed voltages $\{u_{sd}, u_{sq}\}$ back to their phase values $\{u_{sa}, u_{sb}, u_{sc}\}$. This is done by inverting the global transformation matrix using the matrix inversion property

$$(A \cdot B)^{-1} = B^{-1} \cdot A^{-1}$$

This inverse transformation is also done on the stator. Hence the same stator voltage ξ_s is used. The inversion of both *Concordia* and *Park* unit matrices is quite trivial. Only the final result is given here.

$$\begin{bmatrix} u_{sa} \\ u_{sb} \\ u_{sc} \end{bmatrix} = \sqrt{\frac{3}{2}} \begin{bmatrix} \frac{2}{3} & 0 \\ -\frac{1}{3} & \frac{1}{\sqrt{3}} \\ -\frac{1}{3} & -\frac{1}{\sqrt{3}} \end{bmatrix} \begin{bmatrix} \cos(\xi_s) & -\sin(\xi_s) \\ \sin(\xi_s) & \cos(\xi_s) \end{bmatrix} \begin{bmatrix} u_{sd} \\ u_{sq} \end{bmatrix} \quad (\text{C.6})$$

Appendix D

PI tuning

This appendix presents the methodology used to tune the PI controllers of this study, using an analytical approach. Figure D.1 shows the typical closed-loop control scheme used in all cases. $C(s)$ and $G(s)$ are respectively the transfer functions of the PI controller and the model of the underlying physics.

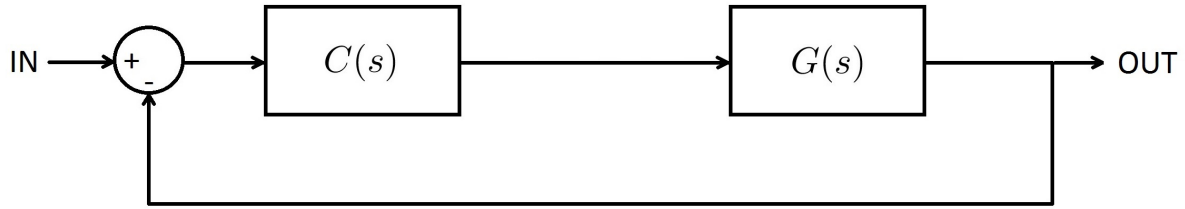


Figure D.1: General closed-loop control scheme.

Proportional and integral gains determination

The general form of the first-order transfer function $G(s)$ is given below.

$$G(s) = \frac{a}{b \cdot s + c} \quad (\text{D.1})$$

The general form for the transfer function $C(s)$ is the following, k_p being the proportional gain and k_i being the integral gain.

$$C(s) = k_p + \frac{k_i}{s} \quad (\text{D.2})$$

The feedback-loop allows to design the controller so to obtain an overall transfer function, from the input IN to the output OUT of Figure D.1, of a given type. Equation D.3 shows that we try to match the overall system with the general second-order transfer function. This leaves us with two degrees of freedom, knowing the natural frequency ω_0 and the damping factor ζ . ω_0 determines the rise time of the step-response of the system, while ζ influences the amplitude of the overshoot of the step-response.

$$\frac{C(s)G(s)}{1 + C(s)G(s)} = \frac{\omega_0^2}{s^2 + 2\zeta\omega_0 \cdot s + \omega_0^2} \quad (\text{D.3})$$

Replacing $G(s)$ and $C(s)$ by their expressions in D.1 and D.2, we obtain the following equation for the global transfer function.

$$\frac{C(s)G(s)}{1 + C(s)G(s)} = \frac{(k_p + \frac{k_i}{s})(\frac{a}{b \cdot s + c})}{1 + (k_p + \frac{k_i}{s})(\frac{a}{b \cdot s + c})}$$

Sorting the right-end term, we get

$$\frac{C(s)G(s)}{1 + C(s)G(s)} = \frac{\frac{a}{b}(k_p \cdot s + k_i)}{s^2 + \frac{(c + ak_p)}{b} \cdot s + \frac{ak_i}{b}} \quad (\text{D.4})$$

This expression has the same form as the general transfer function, allowing inspection. In order to result in the same temporal behaviour, both denominators from D.3 and D.4 need to be equal.

$$s^2 + 2\zeta\omega_0 \cdot s + \omega_0^2 = s^2 + \frac{(c + ak_p)}{b} \cdot s + \frac{ak_i}{b} \quad (\text{D.5})$$

The equality is verified identification of all three factors.

$$\begin{aligned} \omega_0^2 = \frac{ak_i}{b} &\Leftrightarrow k_i = \frac{b}{a}\omega_0^2 \\ 2\zeta\omega_0 \cdot s = \frac{(c + ak_p)}{b} \cdot s &\Leftrightarrow k_p = \frac{1}{a}(2b\zeta\omega_0 - c) \end{aligned}$$

This leads to the following expression determining both gains k_p and k_i as functions of the equivalent system parameters and the desired step-response.

$$\boxed{k_i = \frac{b}{a}\omega_0^2 \quad k_p = \frac{1}{a}(2b\zeta\omega_0 - c)} \quad (\text{D.6})$$

In practice, we coded these results into the matlab function $[Kp, Ki] = PI_tuning(a, b, c, \zeta, \frac{\omega_0}{2\pi})$ returning the required PI gains.

Damping factor and natural frequency choice

Figure D.2 outlines the behaviour of a second-order system under the influence of different damping factors, for a fixed natural frequency. We observe that, for damping ratios higher than 1, there is no-overshoot but an increasing rise time. If lower than 1, the rise time becomes shorter as ζ approaches 0 but there remains some oscillations around the equilibrium. Please note that the rise time mainly depends on the cut-off frequency, to which corresponds the time constant $\tau = \frac{1}{\omega_0}$ of the system. The higher the time constant, the higher the rise time.

The tuning of a PI controller then requires a choice for both parameters ζ and ω_0 . On the one hand, ω_0 is chosen according to the time requirements of the system under control. It mainly takes the underlying physics into consideration. On the other hand, the choice for the damping ζ represents a trade-off between the increase of the final rise time and the introduction of oscillations around the equilibrium. Typical values of ζ will be between 0.8 and 1.0, accepting a small overshoot and a rather short rise time. In our case, we chose $\zeta = 1.0$ for all inner current loops to eliminate any possible overshoot. Regarding the outer loops, we rather take $\zeta = 0.8$ to enhance the rise time.

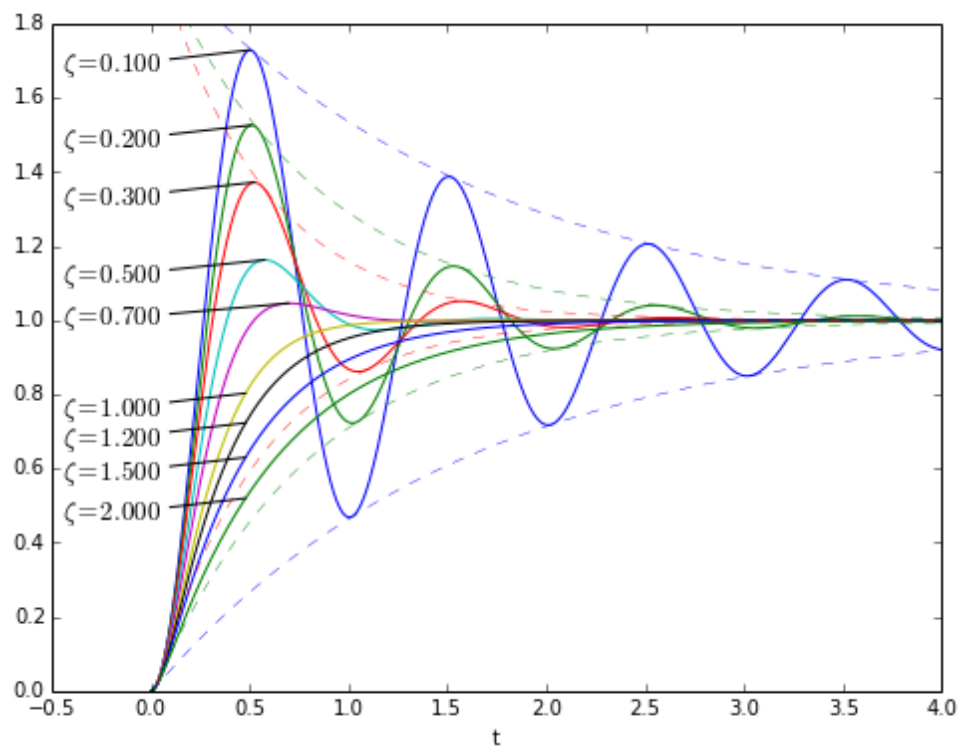


Figure D.2: Second-order system step response for cut-off frequency $\omega_0 = 2\pi$ ($f_0 = 1\text{Hz}$), for different damping factors ζ [32].

Appendix E

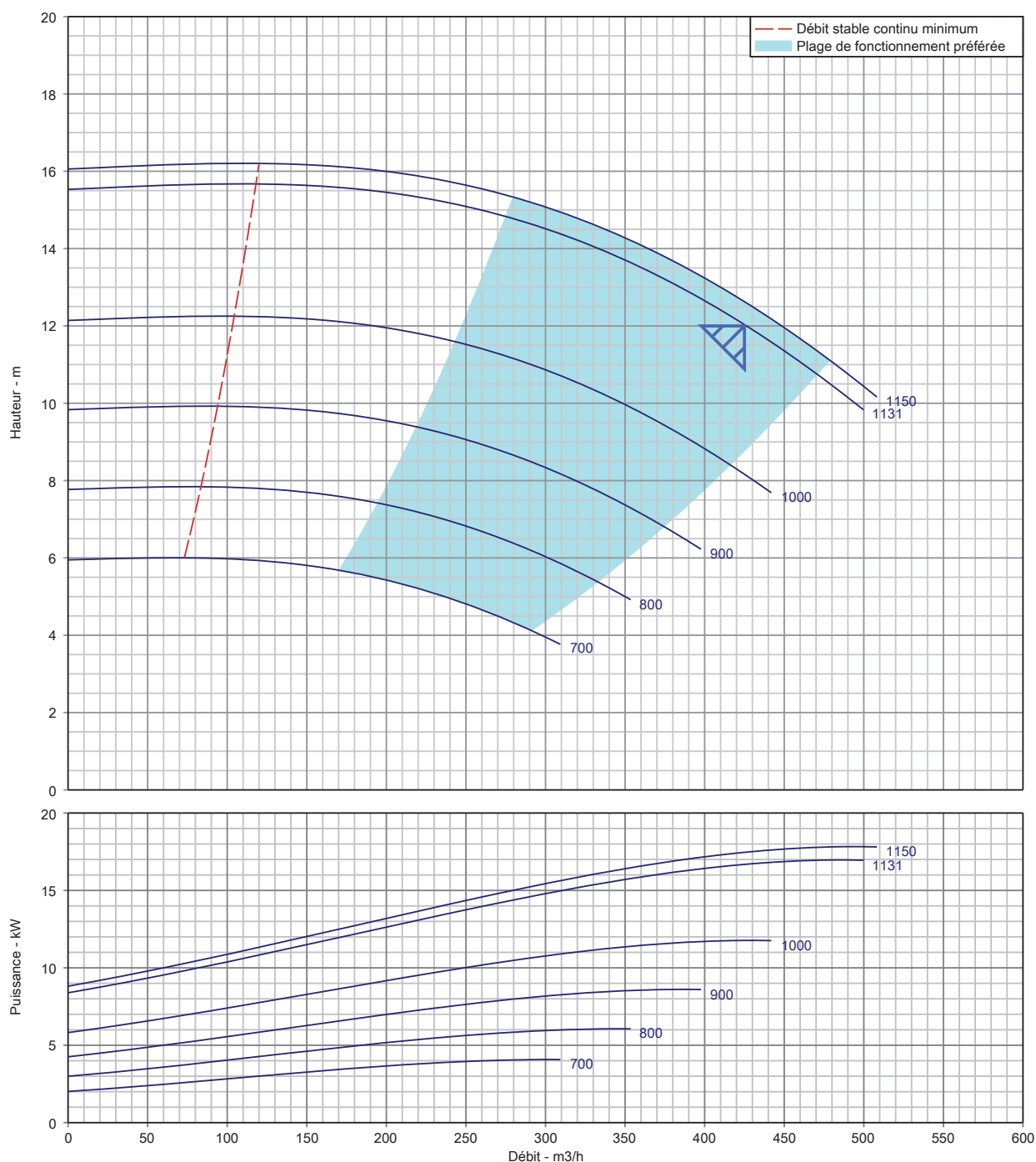
Negundo data

Pump datasheet

Overview of the *Negundo* site

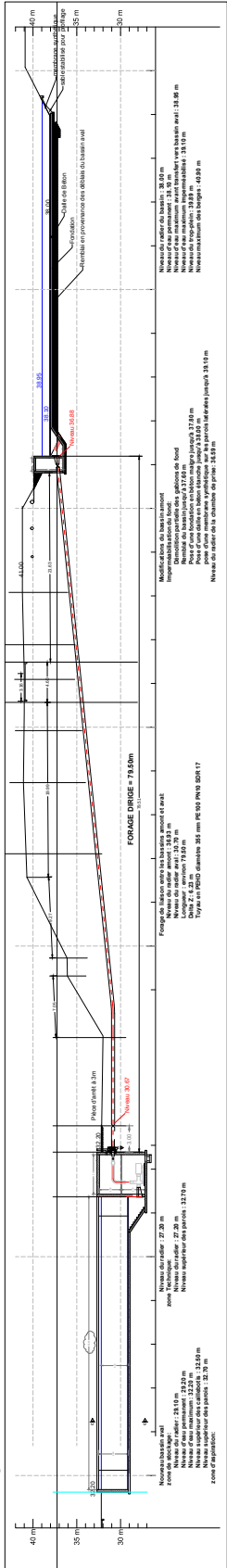
Squirrel-cage induction machine datasheet

Courbes en variation de vitesse

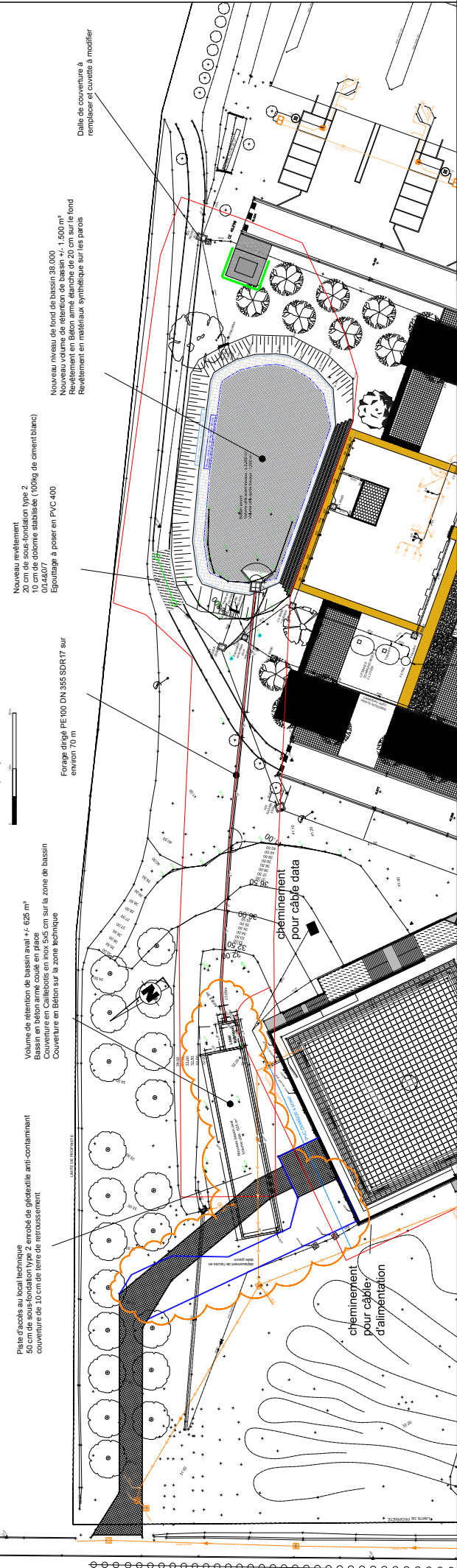


Client	: FABRICOM INDUSTRIE SUD SA	Numéro d'offre	: EMI1602096
Référence client	: Prototype PAT - FROYENNES	Taille	: ICN200-250F
Numéro du Poste	: 001	Nombre d'étages	: 1
Service	:	Suivant le numéro de courbe	: ICN200-250F_1480 Rév 19/10/15_P_MR200-250_Rev0
Quantité	: 1	Date de dernière sauvegarde	: 05 juil. 2016 09:14
Débit, nominal	: 425,0 m³/h	Puissance, nominale	: 16,68 kW
HMT / Pression différentielle, nominale	: 12,00 m	Densité du liquide, nominale / maxi	: 0,999 / 0,999 kg/dm³
Vitesse nominale	: 1131 t/min	Viscosité, nominale	: 1,00 cSt
Rendement	: 83,4 %	Cq/Ch/Ce/Cn [ANSI/HI-2010]	: 1,00 / 1,00 / 1,00 / 1,00
NPSH requis	: 1,97 m	Diamètre roue, nominal	: 296 mm

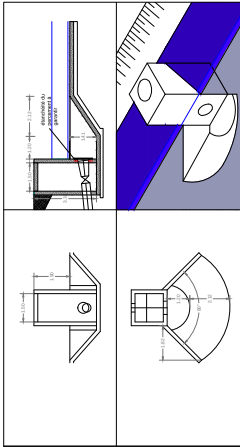
Profil en long de la liaison entre le bassin amont et le nouveau bassin aval 1/200



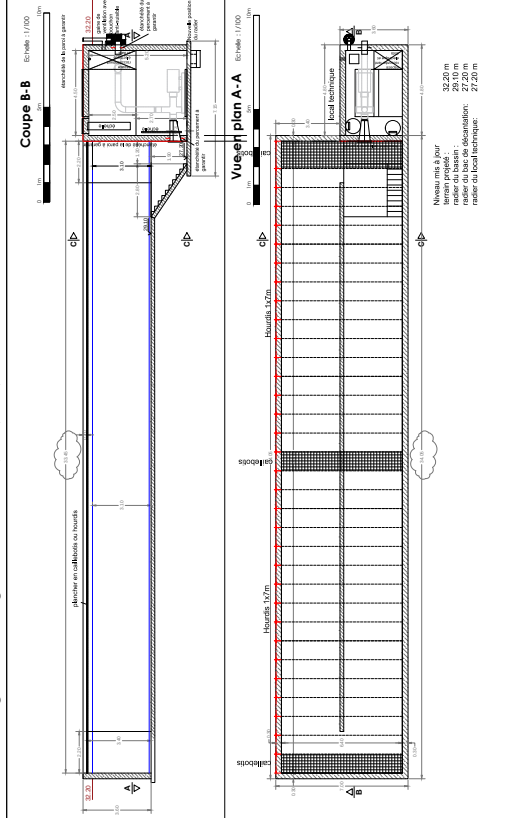
Plan de projet 1/250



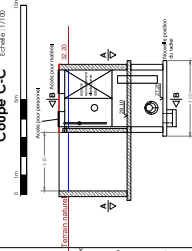
Ouvrage de captage amont 1/100



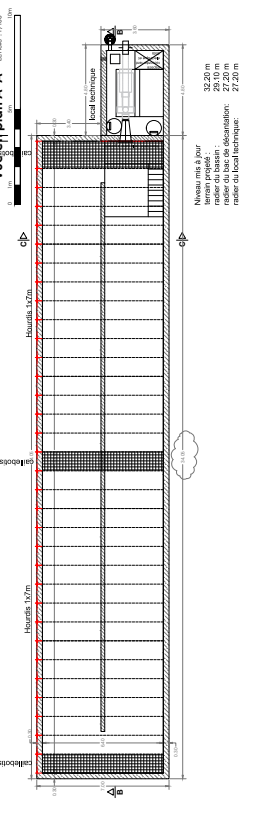
Nouvel ouvrage de stockage aval 1/100



Coupe C-C



Vue et plan A-A



SA TRBA

TRBA

MOJAKOVIC HENRI

MOJAKOVIC HENRI

MOJAKOVIC HENRI

MOJAKOVIC HENRI

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No.:

Date: 15-MAY-2017

DATA SHEET**Three-phase induction motor - Squirrel cage rotor**

Customer :
Product line : W22 - Cast Iron Frame - High Efficiency - IE2

Frame : 225S/M
Output : 30 kW
Frequency : 50 Hz
Poles : 6
Full load speed : 985 rpm
Slip : 1.50 %
Voltage : 400/690 V
Rated current : 55.7/32.3 A
Locked rotor current : 379/220 A
Locked rotor current (I_L/I_n) : 6.8
No-load current : 22.0/12.8 A
Full load torque : 291 Nm
Locked rotor torque : 210 %
Breakdown torque : 250 %
Design : ---
Insulation class : F
Temperature rise : 80 K
Locked rotor time : 12 s (hot)
Service factor : 1.00
Duty cycle : S1
Ambient temperature : -20°C - +40°C
Altitude : 1000 m
Degree of Protection : IP55
Approximate weight : 369 kg
Moment of inertia : 0.71916 kgm²
Noise level : 63 dB(A)

	D.E.	N.D.E.
Bearings	6314 C3	6314 C3
Regreasing interval	20000 h	20000 h
Grease amount	27 g	27 g

Load	Power factor	Efficiency (%)
100%	0.84	92.6
75%	0.79	92.7
50%	0.69	92.6

Notes:

Efficiencies according to the indirect method of IEC 60034-2-1:2007 with stray load losses determined from measurement.

Performed by

Checked

