DOI: 10.13009/EUCASS2023-500
Aerospace Europe Conference 2023 – 10TH EUCASS – 9TH CEAS

Fundamentals Of Aircraft Hydrogen Tank Design With System Simulation

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Abstract

As the number of researched propulsion concepts relying on hydrogen as energy storage increases drastically, both for hybrid electric and purely hydrogen-electric designs, the storage tank design becomes critical. It must be understood whether high-level assumptions made during early concept investigation are plausible, for instance on gravimetric efficiency. For this, the corresponding trade-spaces must be explored, which is usually not practical with overly detailed and computationally expensive field simulations such as Computational Fluid Dynamics or the Finite Element Method. Instead, a system simulation or analytical modeling approach is required, which is efficient and accurate. Such approaches have been discussed in the literature individually and often without much classification or context. The objective of this paper is twofold, first to describe the types of system simulation methods for aircraft hydrogen tank design including sample literature references, and second, to investigate interrelations and consistencies between select references. It was found that methods range from steady-state sizing as well as assessment using dynamic simulation to sizing using dynamic optimization. Interrelations between types of designs, for instance tank models with boil-off and tank models with finite dormancy, and different system simulation methods are reported on a point design and a limited sample trade study. For the point design, predictions of a system simulation method are compared to those of detailed field simulations with Computational Fluid Dynamics and the Finite Element Method, and results were found to compare favorably.

1. Introduction

Conventional fuels used nowadays for aircraft propulsion release carbon emissions – which are contributors to global warming – into the atmosphere during combustion. Engineers are trying to develop technologies that will enable more sustainable aviation in a timely manner. So far, the aviation industry has achieved remarkable fuel burn reductions over the last few decades, only to see them being outpaced by the demand for air travel [1]. Short term, several promising technologies are infused into the so-called aero engine, the propulsion, and the power sub-system of the aircraft. They include ultra-high bypass ratio geared turbofan, unducted fans, Sustainable Aviation Fuel (SAF), and partially electric ("hybrid") propulsion concepts. While the two first ones correspond to more or less improvements of existing technologies, SAF would compensate for the carbon dioxide (CO_2) emissions – at the cost of higher on-ground energy consumption. The hybrid-propulsion concepts look promising for small distances and seem unapplicable to longer journeys, as the specific energy of the batteries is roughly 50 times lower than that of the conventional jet fuels – the trade-off is between batteries and aircraft payload.

Hydrogen (H_2), an element that cannot emit CO_2 while flying as its molecule does not contain carbon, could be used for disruptive aircraft propulsion – either to power a fuel cell to generate electricity to power a propeller or burned in a modified jet engine. Both technologies represent their share of challenges:

- Fuel cell efficiencies are quite low (60% approximately) and require the addition of substantial Thermal Management Systems (TMS) for the case of low temperature Proton Exchange Membrane cells. Therefore, the aviation industry is heavily investigating high temperature PEM [2] – which could reduce or even suppress the need for additional TMS.
- H_2-burning jet engines might seem less disruptive and would still require redesign of the burner and could operate at different temperature ranges. The higher challenge might be however to control the emission of Nitrogen Oxides (NO_x) that could be even worse contributors to the global warming than the CO_2 [3].

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Independently from the technology chosen to extract power out of the hydrogen, one common challenge is its storage. Indeed, one attractive property of the hydrogen is its energy density, which is approximately three times higher than that of conventional jet fuels – around 120 MJ/kg for hydrogen compared to around 42.8 MJ/kg for Jet A. However, for embedded power systems, this benefit is hidden by the low density of the hydrogen, which is, at ambient conditions, approximately 9 350 times lower than that of conventional jet fuels – around 0.09 kg/m^3 for H2 compared to 840 kg/m^3 for Jet A, or said otherwise 10.8 kJ/L for H2 compared to 36 MJ/L for aviation kerosene (a factor of almost 3 500). In practice, this means that, while the same amount of energy could be carried for a third of the weight, it would require a prohibitive amount of space at ambient conditions. Therefore, hydrogen storage technologies rely on deviation from ambient conditions:

- High pressures lead to a density increase of the Gaseous Hydrogen (GH_2). Typically, the hydrogen at 700 Bar and 15°C has a density of approximately 42 kg/m^3. This is more than 450 times that at ambient conditions. Still the energy per unit volume of hydrogen at 700 Bar (5.04 MJ/L) remains 7.15 times lower than Jet A.
- Very low temperatures enable storing hydrogen mainly in its liquid form (LH_2). At 21 K and atmospheric pressure, the hydrogen density is about 71 kg/m^3, bringing his energy per unit volume to 8.5 MJ/L and thus reducing the ratio to 4.25 lower than Jet A.
- To further increase the density, hydrogen can be gelled or slush [4] [5]. The former consists of adding a fraction of gellant into the liquid hydrogen, while the latter consists of a mix of solid and liquid hydrogen. These would lead to a further increase of density of about 10% and 15-20% respectively compared to liquid hydrogen [6].

There seems to be an alignment in the aviation industry to prefer GH_2 solutions for small aircrafts and short range and LH_2 for larger aircrafts and range. The 70% added energy per unit volume the LH_2 solution provides compared to GH_2 cannot be disregarded. However, the infrastructure for GH_2 being more developed – partially thanks to the automotive industry – associated with the simplest architecture of the hydrogen distribution system makes it the preferred solution for the first prototypes flying today. Despite their benefits, the authors of this paper could not find any current application using either gelled or slush hydrogen for aviation. The reasons could be the cost of production or the lack of further technology exploration [7].

Both GH_2 and LH_2 lead to significant design constraints on their storage tank design. While GH_2 implies withstanding high pressures, LH_2 requires thermal isolation to avoid heat ingress from the environment to the tank. These constraints, and more like tank filling duration, make the storage of hydrogen in the wings – as for most of the conventional fuel – a very challenging option. Still the option of integral tank as a section of the fuselage seems to be preferred by the literature – e.g. [8] and [9] – , based on the LH2 storage capacity for a fuselage section. Indeed, a non-integral tank would add its envelope layers within the fuselage, including some gaps for its insertion and removal. While the theoretical storage capacity of integral tanks might be 8.5% higher, non-integral tanks seems to be preferred by the aircraft manufacturers as the exchange of LH2 tank seems to be preferred to the on-board refilling – as presented by the Airbus Pod concept, Universal Hydrogen or ZeroAvia.

To be aligned with the current trends in aeronautics, this paper focuses on LH2 non-integral tanks. The next sections go through the challenges of designing liquid hydrogen cryogenic storage tanks – e.g. hydrogen boil-off, dormancy with or without venting, thermal phenomena during hydrogen extraction – and how system simulation is used to support these studies. The authors mostly found that sizing was performed with steady-state analysis and will be detailed in section 2. The performance assessments, however, are mostly carried out with dynamic simulation and will be discussed in section 3. To support the conclusions, section 4 later illustrates these types of simulation with a case study consisting of a typical mission profile.

2. Steady-state cryogenic tank sizing

2.1 What is steady-state simulation?

As the name indicates, steady-state simulation is the study of a system where the states are steady – at equilibrium. It is relevant to highlight that steady does not necessarily mean that nothing is moving. Steady means that the behavior is fixed. Typically, a steady mass flow rate could flow through a pipe and generate a steady pressure loss. There would be no variation of pressure due to storage in a steady-state analysis. Thus, each variable of the model would have a single value – its equilibrium value for the given boundary conditions.

For simple algebraic analysis, a spreadsheet could suffice to solve a steady-state problem. However, when it comes to complex multi-domain systems the solving becomes tedious to write properly and cumbersome to converge. In the case of a cryogenic tank sizing, the solving involves iteration on the tank sizing parameter and design choices – discussed below in this section: fluid properties, while aiming at some target metrics (such as gravimetric efficiency). A proper physics-based steady-state system simulation tool becomes an advantage. This will be highlighted again in section 2.6.

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2.2 Storage capacity: mass and volume

When it comes to tank sizing, it seems obvious that the main criteria would be its capacity, both in terms of mass of fuel stored and volume. The mass of fuel is defined by the fight scenario, while the tank volume is typically a constraint as the tank should fit in the aircraft fuselage exclusively – contrarily to the kerosene that can also be stored in the wings.
Colozza [4] presented a simple, yet efficient, algebraic method to compute the mass of liquid hydrogen (M_{LH} in [kg]) to be stored for a given application. It consists in defining the mass flow of hydrogen (F_{LH}) needed to provide a given power and multiply it by the operation time (t in [s]). Specifically, the equations were given for a conversion of the hydrogen energy into electrical energy with fuel cells – involving thus the cell efficiency (η_{fc} [-]), the number of plates in the fuel cell stack, the Faraday constant, the number of electrons per molecule and the molar concentration (moles per kilogram) for the hydrogen. It is worth noting that Kroo [10] estimates that, for a given mission profile, typical reserve fuel should be around 8% of the required fuel mass (M_{req} in [kg]). Based on the authors' previous analysis, the value of 2.6% is here used – corresponding to the weighting by the ratio of the lower heating values of hydrogen and jet fuel. The resulting formula, expressed in function of the desired output power (P_0 in [W]), is presented in equation (1) – as a slight update from Colozza to include u_R = 2.6%, the fuel reserve.

$$\boldsymbol{M_{LH} = (1 + u_R) * M_{req} = (1 + u_R) * P_0 t/(32.167\eta_{fc})} \quad \textbf{(1)}$$

More generically, with the amount of data recorded in flights, it would be also possible to obtain a good approximation of the mass of hydrogen required by integrating the output power over time. Note that, on top of that,
in the same paper, Colozza explains that the tank capacity shall include around 7.2% of additional volume to provide space for boil-off gas – liquid becoming gaseous – and pressure regulation – venting to release hydrogen outside of the tank to limit its pressure. This is the initial tank ullage. Huete [11] uses 5%. Winnefeld [12] explains that a fraction of 3% is kept for ensuring that gas is vented (and not liquid), while the rest is for the boil-off up to the venting pressure. He further highlights the different liquid volume fractions based on the desired venting pressure.
The volume of liquid hydrogen is easily obtained from the required mass of LH2 to store by diving by its density (ρLH in [kg/m3]) and presented in equation (2), considering the ullage for boil-off uBO:

$$\boldsymbol{V_t = (1 + u_{BO}) * M_{LH}/\rho_{LH} = M_H/\rho_H} \quad \textbf{(2)}$$

The initial ullage would be gaseous hydrogen and thus the mean hydrogen mass (M_H) and density (ρ_H) are introduced. Note that the combination of a reserve hydrogen u_R of 2.6% and an additional boil-off volume fraction u_{BO} of 7.2% leads to roughly 10% of extra volume compared to the raw sizing for the LH2 mass storage. This is practical to grasp the order of magnitude and these two "margins" should not be lumped automatically as – repeating for emphasis – one corresponds to mass of hydrogen while the other one is about ullage volume. Leaving these values as parameters, u_R and u_{BO}, allows us to come back to them later in this paper – especially when talking about dormancy on which we will later focus.

2.3 Thermal insulation and mechanical stress

The difference in temperature between the ambient and the liquid hydrogen – approximately 250 K – is so important that the thermal insulation becomes key to reduce the heat ingress. Indeed, a lack of insulation would lead to both the freezing of the external parts of the tank and its fixations, and the boiling of the liquid hydrogen – raising its pressure drastically [4]. Furthermore, the variations in internal pressures lead to stress considerations when sizing the wall thickness [12].
Brewer, Colozza, Verstraete and Winnefeld summarized well the tank sizing both from the mechanical and thermal insulation aspects. This paper reuses the same methodology. Out of the different insulation materials, it is here considered only two variants: the polyurethane foam and the stiffened panel Multi-Layer Insulation (MLI) vacuum.
It is also relevant to mention that the shape of the tank is playing a role in both the resistance to stress and insulation. It is well known that the sphere is the shape that best withstand stress and has least surface of heat exchange with ambient for a given volume. It is however inconvenient to place in an aircraft fuselage where cylinder with hemispherical or ellipsoidal ends would fit better.
A last note on this section is the importance of permeation and hydrogen embrittlement found on the literature [13]. Indeed, hydrogen being the smallest molecule, leakage will inevitably happen – especially at higher pressures. The molecule could also embrittle the tank material and influence its properties in an undesirable manner.

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2.4 Gravimetric efficiency

The quality of the hydrogen tank designs is typically measured in terms of the gravimetric efficiency (η_{grav} [-]): the mass of hydrogen stored inside the tank over the total mass of tank (M_t in [kg]) and stored hydrogen [5].

$$\boldsymbol{\eta_{grav} = M_H/(M_H + M_t)} \tag{3}$$

As hydrocarbon fuels are stored in integral tanks that add very low mass penalty (essentially, only pumps, valves as well as a special coating are required), their gravimetric efficiency is near 1. This is not the case for hydrogen. First, as mentioned previously, non-integral tanks seems to be preferred to integral tanks. Then, the lightweight of hydrogen combined with the need for high insulation tends to reduce the gravimetric efficiency.
Luckily, as it was pointed out in the introduction, the energy density of the hydrogen is 3 times higher than the one of jet fuel. This means that, for a given amount of stored energy, a hydrogen tank with gravimetric efficiency above 33% would have combined mass of fuel and storage lower than that of jet fuel. Nevertheless, it will not reach the theoretical 3x improvement of the fuel mass for the same amount of energy in comparison to jet fuel. At 33%, the combined masses will be equal.
Winnefeld et al. [12] introduced a slight variant of the gravimetric efficiency, where the numerator is the required – or useful [14] – mass of hydrogen (M_{req}). They insist on that the insulation performance has a key effect on this corrected gravimetric efficiency because it influences the tank mass and, in case of a design that relies on venting a given mass of hydrogen, it influences this amount – that can be seen as an increased value of u_R. We complement this statement by highlighting that the tank shape is another influencer of the gravimetric efficiency – as it influences the surface of heat exchange with the tank environment and thus the insulation and mass tank – and that other design choices such as the selected operating and venting pressure have a negligible influence – even if at first sight they seem to have a larger influence. This is made explicit by substituting equations (1) and (2) into equation (3):

$$\boldsymbol{\eta_{grav} = \frac{1}{(1+u_{BO})(1+u_R)\frac{\rho_H}{\rho_{LH}}+\frac{M_t}{M_{req}}} \approx \frac{1}{(1+u_R)+\frac{M_t}{M_{req}}}} \tag{4}$$

The approximation made in the second part of the equation (4) is due to neglecting the mass of gaseous hydrogen with respect to the one of liquid hydrogen. Indeed, the density of the gas is at least 70 times smaller than the liquid and the initial ullage is 8 or 9 times smaller – leading to a ratio of masses way above 500. Thus, the assumption – for the sake of understanding the impact of design parameters over the gravimetric efficiency – is relevant.

2.5 Dormancy prediction

Despite great insulation and reduced surface of exchange, heat will enter the storage tank. Liquid hydrogen will become gaseous – known as boil-off – and pressure will rise. After some time, the pressure will reach the maximum allowable pressure and, if required, some gaseous hydrogen will be vented. The tank dormancy is the period of time the tank can sustain without venting or excess structural stress. Dormancy can become pivotal for airworthiness and anticipated airport regulations [15].
Huete and Pilidis [11] investigated tank design trade-offs with explicit consideration of dormancy and proposed an algebraic dormancy estimation (and related these results to tank geometry, rate of discharge (“endurance”), maximum operating pressure and mass).
Recently, the authors studied the sizing of a tank focusing on maximizing its dormancy [15]. It was found that, based on the desired venting pressure, the maximum dormancy is conveniently expressed as a function of the hydrogen fluid density – mixture of liquid and gas. Figure 1 is an extract from [15]. As the tank is assumed to have a constant volume, and no mass is crossing the control volume boundaries, the density must be constant in a dormant tank. Therefore, for a given initial pressure and temperature of the hydrogen, the density is analogue to (one minus) the initial ullage – or equivalently the additional boil-off volume fraction u_{BO}. This allows to present here a variation of the results as the maximum dormancy as function of the initial ullage for different venting pressures:

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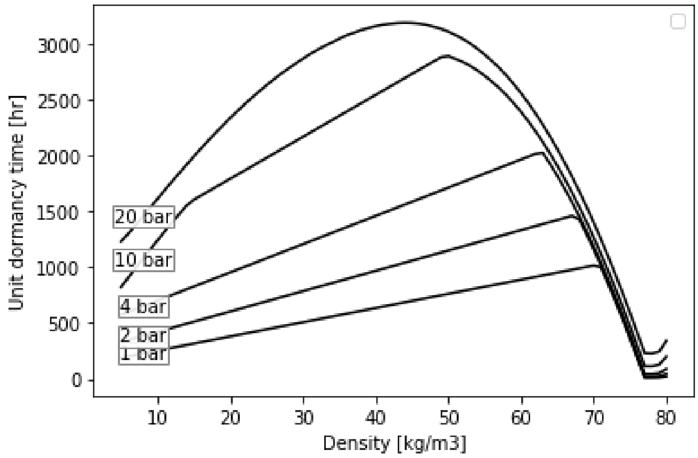

Figure 1: Unit dormancy as function of the mean hydrogen density for different venting pressures.

2.6 An iterative process

As we have summarized from the literature, in the case of a cryogenic tank sizing, the solving involves iteration on the tank sizing parameter and design choices, fluid properties, while aiming at some target metrics (such as gravimetric efficiency). Winnefeld [12] represented in the following flow chart his process.

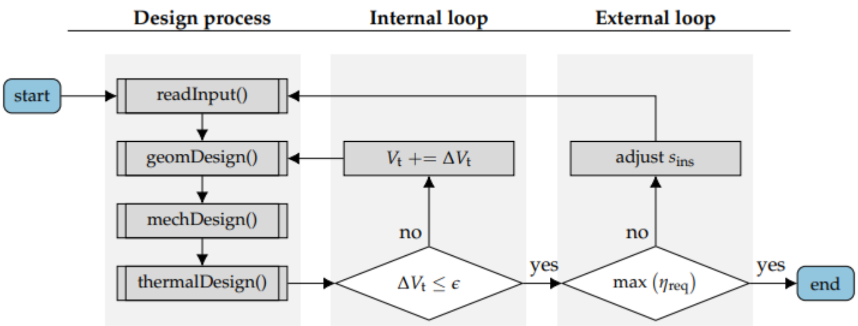

Figure 2: Flow chart of Winnefeld tank modeling where s_{ins} is the insulation thickness, ΔV_t the change in tank volume and ε the tank-volume tolerance – from [12].

Already this workflow would be inconvenient to implement in a spreadsheet and the result is often not very reusable nor robust. Additionally, this flow chart could be increased with more steps in the design process, for example by capturing the liquid and gaseous hydrogen properties used in the further computation, or by assessing the dormancy of the tank and iterating in a third loop if the result is not satisfying. At Modelon, we use Modelon Impact to size the cryogenic hydrogen storage tanks.

Modelon Impact is a next generation system modeling and simulation platform, leveraging the benefits of web and open standard technologies. With openness at its core, Modelon Impact supports standards such as Modelica, FMI, Python and REST. The user-friendly browser interface provides modeling experts the tools they need to create, simulate, and experiment. Both steady-state and dynamic solutions can be obtained from the same model, reducing effort to get an answer. Finally, the Modelon Impact API enables user-specific workflows through Python-based custom functions, and deployment of models to non-experts via targeted web applications [16] or Jupyter Notebooks. Especially for steady-state, Modelon Impact includes our Physics-Based Solving technology, that enables adding engineering insights in models so that the steady-state simulation solves even faster and in an extremely robust way. This technology proved to solve very complex systems such as aircraft jet engines multi-point design and performance analysis [17] as well as liquid cooling system sizing and behavior [18].

One additional benefit of using Modelon Impact is that it includes a wide range of physics and application libraries – often component-based – implemented in the Modelica language. This enables to easily assemble the model based on physical building blocks – exchanging energy at their interfaces – and to provide a convenient way to explore design variants – e.g. changing the material properties.

Figure 3 below shows the tank sizing model connected to the environment thermal boundary condition. The numbers on the right represent a selection of parameters or results displayed as a "sticky" for convenience. The tank itself contains many more parameters – such as the materials for each layer, maximum pressure or required dormancy.

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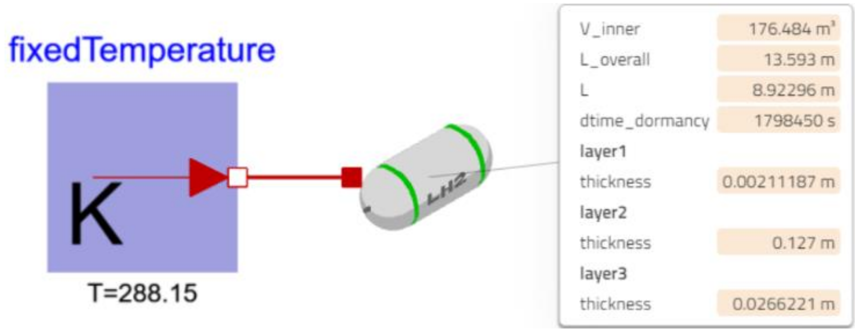

Figure 3: System view of the tank sizing model in interaction with its environment.

3. Dynamic assessment of cryogenic tank performance

3.1 What is dynamic simulation?

As opposed to steady-state simulation, in a dynamic simulation the system is not necessarily at equilibrium. It could be that the system reaches equilibrium at some point of the simulation. It could also be that it does not. Dynamic simulations look at the evolution of variables along a mission profile. The result is no more a single functioning point but a continuous trajectory in time, for each variable.

Consider the hydrogen tank during a flight scenario, the mass flow extracted is a function of the power request which varies depending on the different phases of the flight. The pressures in the tank will keep evolving as heat gets in the tank despite the insulation and, at the same time, the mass gets extracted by the hydrogen system. The fraction of liquid and gas keeps evolving along time. The solving typically involves these multi-physics couplings and can make visible some phenomena that are unobservable with steady-state simulation – e.g. the dynamic condensation blocking during cryogenic refueling.

The rest of this section goes through a typical selection of physical phenomena to consider when sizing a tank that are better studied with dynamic simulation. The first two phenomena are focusing on the tank filling phase while the next two are more on the tank usage phases.

3.2 Cryogenic tank chill down

Hu [19] explains that "chill down (quenching) is the process of keeping the system adjusted to the low temperature scale which is usually several hundred degrees below room temperature. The chill down or quenching process is complicated, involving unsteady two-phase heat and mass transfer, and has not been fully understood".

Indeed, consider the scenario in which a tank is currently at ambient temperature and pressure and has to be filled with liquid hydrogen. For the sake of simplifying the case, let's consider it is filled uniquely with gaseous hydrogen. When the liquid hydrogen starts flowing in the tank, there are two antagonist reaction happening:

1. The tank walls and gaseous hydrogen start to cool down (chill down).
2. The liquid hydrogen heats up and boils.

The heat transfer between cryogenic hydrogen and both the wall and gas is not monotone with respect to the temperature difference. At low temperature differences, below approximately 40°C, there is nucleate boiling happening while after a transition period, film boiling occurs. This leads to very different heat transfers – see Figure 4.

In practice, this implies that a fast filling of a tank – originally at room temperature – might lead to two different masses (the liquid hydrogen and the wall) with a high temperature differences, which consequently will induce a low heat flux. On the contrary, a slow filling might initially bring the liquid in to its gaseous phase and will chill down the tank much faster.

This means that a chill down simulation that do not include the boiling curve for heat transfer might be useless, as these different dynamics would not be captured. It is also relevant to highlight that the tank walls will not homogeneously cool down at the same pace: the part in contact with the liquid hydrogen should cool down much faster than the part in contact with the gaseous hydrogen. Accordingly, discretizing the tank walls into different parts that have their own thermal states and conduction in between would lead to a much better understanding of the chill down scenario. Figure 5 shows how the detailed cryogenic tank is implemented in Modelon Impact where both the liquid and the gas phases thermal ports are connected with discretized heat transfer correlations – that cover convection and the boiling curve – connected to the discretized walls. Figure 6 shows the same tank as an icon (with the bubble representing the boiling capability) being filled system and connected thermally to the environment and to a venting valve. The graph on the left present the temperature of the wall based on the height in the tank – for a given time stamp.

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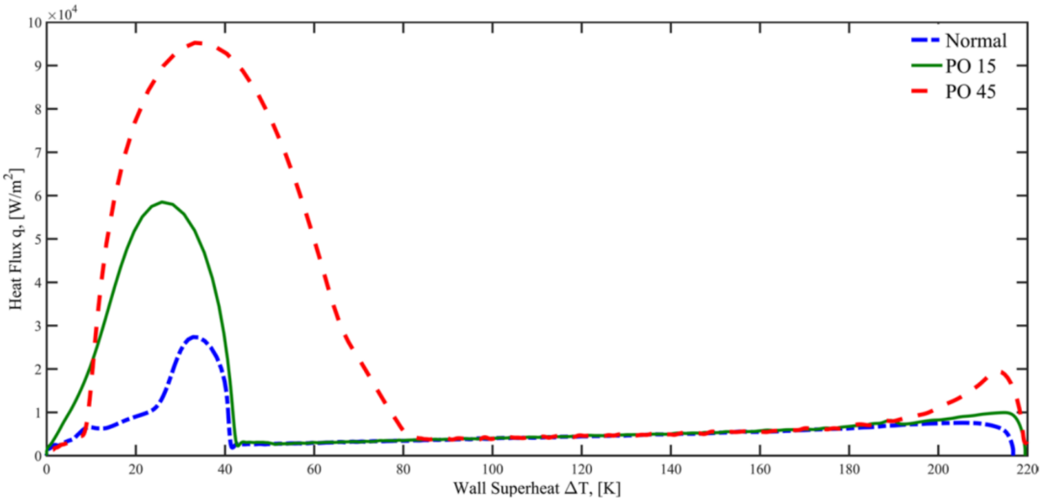

Figure 4: Boiling curves for the three surfaces during quenching experiments – from [19].

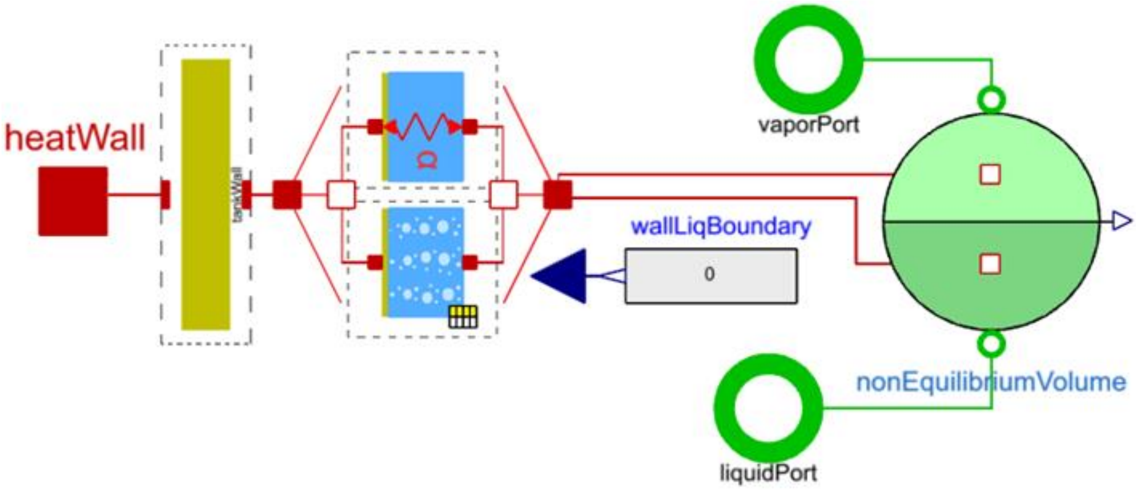

Figure 5: Inner view of the Modelon Impact cryogenic tank with boiling and quenching capabilities.

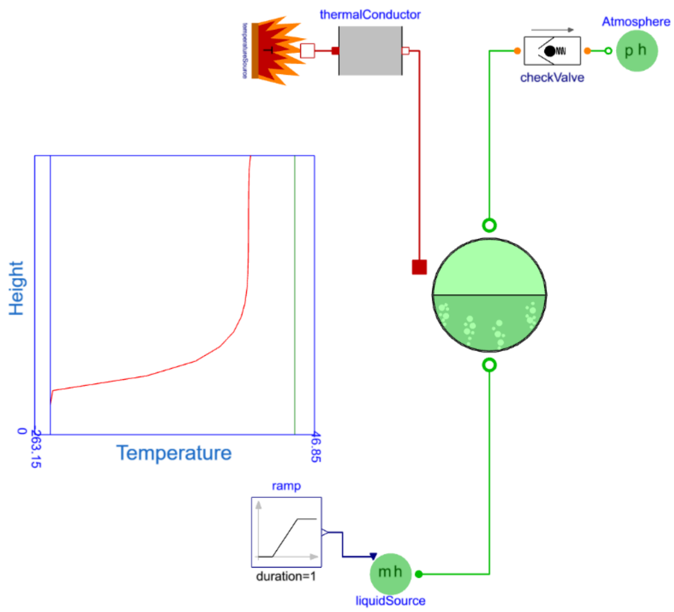

Figure 6: Wall temperature as function of the wall height for a filling scenario.

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3.3 Dynamic condensation blocking during cryogenic refueling

To the authors' knowledge, it was first detailed by Osipov and Muratov [20] that, when filling a cryogenic hydrogen tank, a so-called condensation blocking effect could happen. The study, carried over on a no-vent insulated tank, showed that, if the filling is too fast, evaporation of the liquid hydrogen can happen at the same rate that the condensation of the gaseous hydrogen, leading in practice to a constant mass of gaseous hydrogen that can only build up pressure to absorb the added liquid hydrogen from the filling. Note that filling of LH2 tanks is typically ensured by pressure difference and the gas pressure increase leads to filling mass flow reduction – and ultimately to blocking. Accurate representation of this phenomenon requires characterization of the heat transfer between the liquid – vapor interface, and the vapor and liquid bulks respectively. As vapor condenses at the liquid surface, it is heated up and the increased surface temperature reduces the condensation rate. The liquid surface at the same time is cooled down by colder liquid at lower level, and this heat transfer rate thereby limits the condensation rate. While a more detailed model of the vapor-liquid heat and mass transfer is still under development in our cryogenic tank model, at the time of the writing, we present in Figure 7 some first results. These are based on simulation where heat and mass transfer are proportional to the temperature difference between vapor and liquid bulk temperatures.
It is observed that, in the first scenario with a higher mass transfer coefficient (corresponding to efficient cooling of the liquid surface from the liquid bulk), the tank fills up quickly and the pressure raises to approximately 2.6 Bar. In the second scenario, a lower mass transfer coefficient is used, reducing the vapor condensation rate (corresponding to poor cooling of the liquid surface) and the pressure builds up quickly to near 3 Bar, which is the pressure of the source, slowing down the filling of the tank by several order of magnitude.
In their first dynamic simulation, Osipov and Muratov showed that the blocking was happening after approximately 4min corresponding to 50% of the tank filling and it took about 35 additional hours to bring the filling up to 95%. Slowing down the filling before the blocking happens can lead to drastic reduction of filling time – which might be counter-intuitive if one is not aware of this effect.
In a follow-up paper [21], Osipov et al. studied the refueling profile considering both the chill down and condensation blocking effects. Clearly, the study of the optimum tank filling profile to minimize its duration is a dynamic problem that can be solved by experimenting on the filling system parameters.

Figure 7:Filling with and without (low fidelity) blocking.

3.4 Venting

We discussed, in section 2.5, the tank dormancy of the tank prediction. While the tank can be designed to be dormant, there are several reasons to still integrate a venting valve – one of them being the partial loss of insulation, for which the tank sizing cannot account for. This scenario can easily be studied by adding a heat ingress, e.g., directly to the liquid hydrogen (in effect, by-passing the insulation) – as shown in Figure 8.

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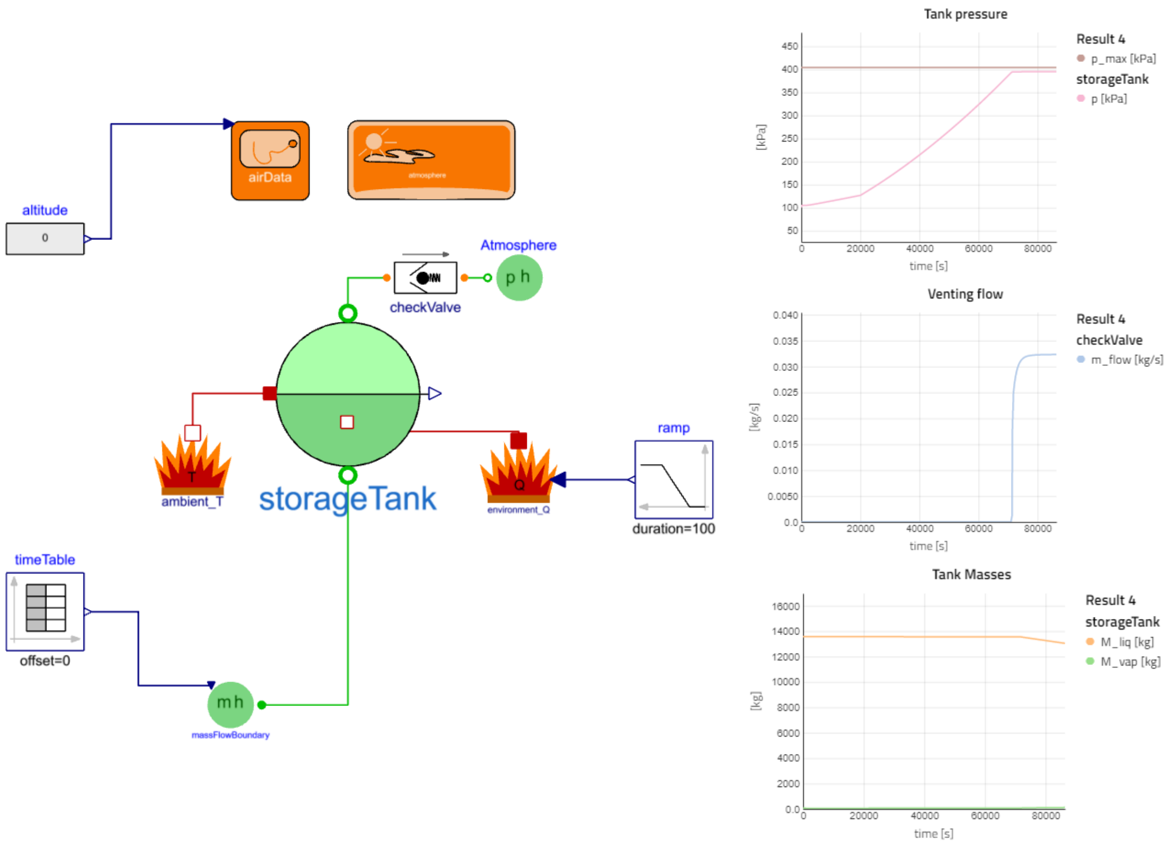

Figure 8: Venting scenario due to additional heat ingress.

Looking at the results, the additional heat ingress causes the pressure and temperature to rise faster than with just the ambient heat ingress. At time = 70,500 s the pressure reaches the point where the vent valve opens to allow vapor to vent off and avoid reaching the maximum operating pressure. With the valve open, the pressure settles at 395 kPa and the venting rate settles around 0.03 kg/s.

While this simulation is conveniently made in dynamic mode, we could argue that such a study could be decomposed into two steady-state part:

1. the time before venting (dormancy) – that we saw we can get from a steady-state analysis – and
2. the steady venting flow – which might be easily obtained from the pressure-flow correlation of the venting valve and from which we can compute the mass of hydrogen vented.

However, as soon as the system is more active than this one – e.g. active heating regulation – then the study becomes more complex and the benefits of dynamics simulation clearer.

3.5 Increasing fidelity by discretizing the fluid layers

In reach of a further increase in accuracy, some authors – Daigle [22] and Zandler [23] for example – discuss the discretization of either or both the liquid and gaseous part of the tank. During mass extraction, the temperature variations in the tank due to heat transfer and gas volume expansion bring new insights on the temperature distribution along the height of hydrogen. The results are not included here though as some display layers of gaseous hydrogen at lower temperatures than the saturated liquid hydrogen – which seems unphysical. The approach remains however valid and it might only be a mistake in code implementation or a lack of representativity of some physical effects not detailed in their paper.

Zandler used the results from Hasan's experiments [24] to criticize his results. A comparison between some experiments and simulations are presented in figure 9 and proved a good accuracy for temperature. The pressures are not included here and proved to be less accurate, most likely due to the lack of fidelity of the interface film layer modeling and discretization of the ullage.

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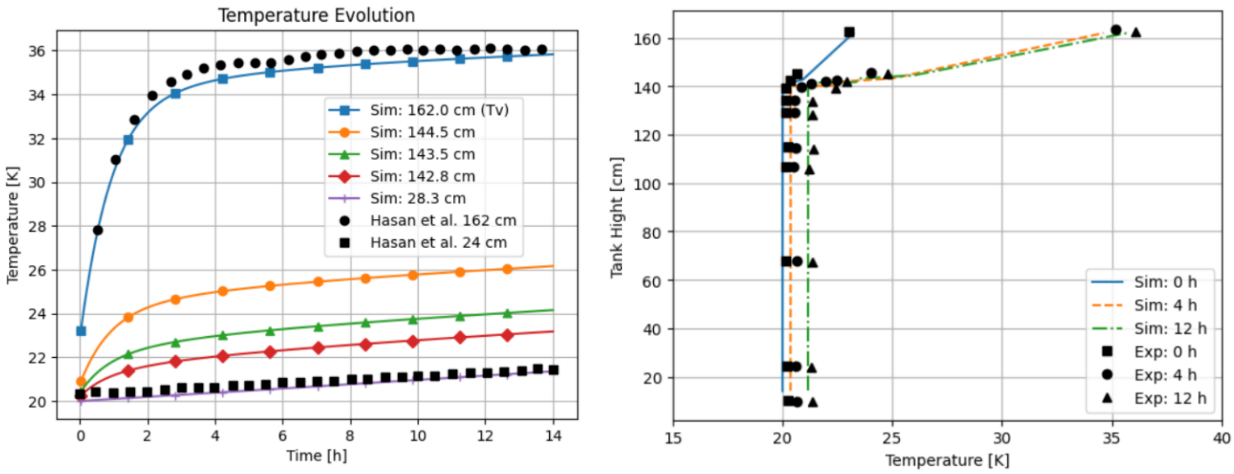

Figure 9: Temperature evolution as function of time and height for a discretized tank – from [23].

3.6 High-fidelity analyses

Sullivan et al. [25] present a range of analyses using finite element and other high fidelity methods. In the following, we provide a range of comparisons of sizing results (as described in section 2), to these more accurate predictions.
First, we compare the results for the inner tank made out of Aluminum 2014-T6. This inner tank is the innermost layer of a vacuum insulated tank and must primarily sustain the mechanical load exercised by the fluid it contains. The next layer towards the outside of the tank is a multi-layer vacuum insulation, and therefore only imposes minimal load on the surface. This tank is described in terms of geometry and sizing parameters such as safety factor in Table I of Sullivan et al.. The material properties of Aluminum 2014-T6 are available in the Modelon Impact tank sizing database, yet was replaced with the slight variation provided in Table IV of Sullivan et al. for consistency. It is noteworthy that the more detailed mechanical sizing of the inner tank considered more than a single load (the sizing approach described in section 2 uses an analytical approach and currently only considers the internal pressure). The detailed mechanical sizing additionally considers a uniform external pressure on the outside, a 3.5g vertical acceleration load, a 0.5g lateral acceleration load, and thermal loads. It is assumed that it is conservative to omit the external pressure load on the outside and effectively assume an ideal vacuum for the mechanical sizing using the analytical approach. The other loads must currently be captured by an increased safety factor. Sullivan et al. assume 1.5 as safety factor (in accordance with U.S. Federal Aviation Administration Regulations), while authors on analytical sizing methods have been proposing higher values in an attempt to make conservative assumptions despite the analytical sizing approach. Huete and Pilidis [11] for instance used a safety factor of 2.2. A second noteworthy difference of the more detailed mechanical sizing is the use of a supporting rod. This is currently not represented in the analytical sizing routine.
The following Figure 10 illustrates the results of the analytical sizing for the inner tank and the given parameters. The results are given over a range of safety factors. The lowest value is the one considered in the detailed sizing using a range of load conditions. The highest value is beyond the safety factor considered by Huete and Pilidis. We achieve a match to the reference results for a safety factor of 2.4. From this comparison, we therefore conclude that, at least for the inner tank of a metallic vacuum-insulated tank with supporting rod, a slightly increased safety factor might be sensible to account for additional load cases and approximations of the analytical sizing approach.

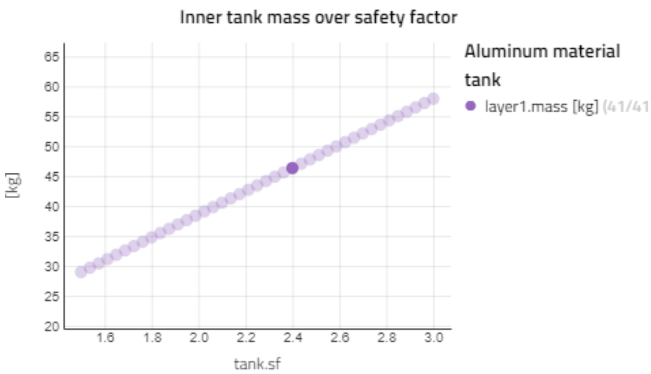

Figure 10: Inner tank mass over safety factor using Aluminum 2014-T6 properties

DOI: 10.13009/EUCASS2023-500

We now compare the results for the outer tank of the same cryogenic hydrogen vessel (outside of the vacuum chamber). It is also made out of Aluminum 2014-T6. This outer tank must primarily sustain the mechanical load exercised by ambient to avoid buckling. This outer tank is described next to Table XIV of the given reference (in particular in terms of outer diameter, 53.21 inch and ambient pressure 14.7 psi). The material properties of Aluminum 2014-T6 were again replaced with the slight variation provided by Sullivan et al., this time in Table XIII. For these results, Sullivan et al. do not impose additional load cases, i.e., only the uniform external pressure was applied.
Figure 11 shows again the variation of outer tank masses over the safety factor. In this case, a match with the analytical sizing routines is achieved for a safety factor of 1.8. From this comparison, we therefore conclude that, at least for the outer tank of a metallic vacuum-insulated tank with supporting rod, a slightly increased safety factor might be sensible to account for approximations of the analytical sizing approach. The safety factor found here is lower however than the one used by Huete and Pilidis (namely, 2.2). Therefore, their choice might be a first reasonable approximation and should be biased up (in case of dominating influence of the inner tank) or down (in case of most influence by the outer tank) for this type of tank geometry and engineering assumptions.

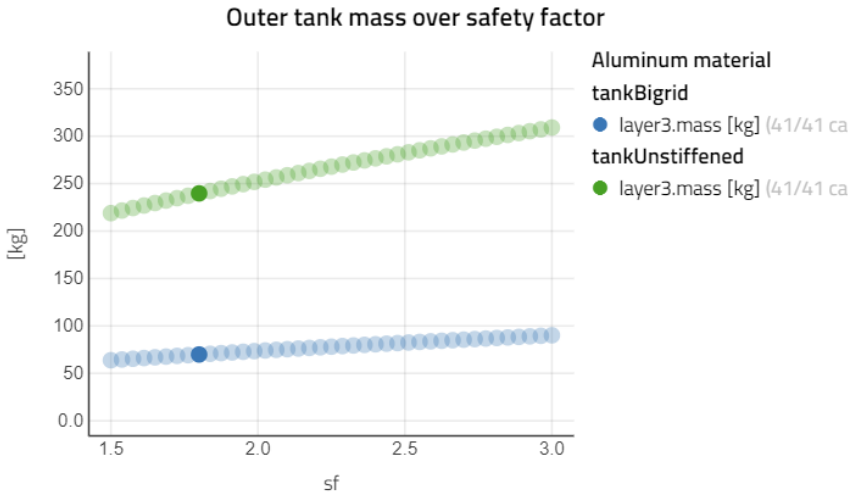

Figure 11: Outer tank mass over safety factor using Aluminum 2014-T6 properties

4. A case study – flight mission profile

4.1 Defining the scenario

This case study is complementary to the one presented by the authors in [15]. The focus here is on sizing two tanks with different insulation types and fitting in the same aircraft (geometry and MTOW constraints). The assessment consists thus of the different performance of both design in terms of internal variables – pressures and temperatures – as well as overall aircraft range.
The tank sizing models from the Modelon Vapor Cycle Library available in Modelon Impact are used. They rely on the cryogenic tank sizing methods suggested by Colozza [4] and by Huete and Pilidis [11]. Geometric parameters are determined by considering the key design factors of mechanical strength, structural design, heat transfer, insulation, dormancy and boil-off. The revised dormancy estimation methodology by Sielemann et al [15] is used.
The tank sizing is coupled with aircraft sizing, so that suitable design constraints are imposed. Therefore, the tank diameter must fit within the fuselage cross section and the overall mass of the tank including the hydrogen fuel is within limits imposed by the maximum take-off weight.
For this example, the storage tank for a two-aisle aircraft with the seating arrangement 2-3-2 (2 seats, an aisle, 3 seats, an aisle, then 2 seats) is sized for both a polyurethane foam insulated tank and a multi-layered insulation (MLI) vacuum insulated tank. The design parameters are shown in Table 1 and serve as inputs to the simulations.
It is relevant to highlight that the available hydrogen mass is not the same between both tanks. Indeed, the “mass of accessories per unit tank volume” is the main reason for this difference – as the overall mass is constrained by the maximum takeoff weight (MTOW). Furthermore, a percentage of the volume is reserved, not to be filled with liquid, and a percentage of the liquid is reserved so it’s not considered part of what is “available”. The increase in accessories mass is at the cost of volume available for hydrogen mass.

DOI: 10.13009/EUCASS2023-500

Table 1: Tank sizing design parameters for Foam and MLI vacuum storage tanks intended for 2-3-2 seating aircraft.

	Foam	**Vacuum**
Vapor volume fraction at filling [%]	13	13
Available hydrogen fuel [kg]	13620	10220
Outer diameter [m]	4.67	4.67
Filling pressure [kPa]	165.5	165.5
Maximum operating pressure [kPa]	404	210
Insulation material	Foam	MLI
Insulation coefficient [W/(m.K)]	0.015	0.00015
Insulation thickness [mm]	Unknown	127
Dormancy [h]	24	Unknown
Safety factor [-]	2.2	2.2
Mass of accessories per unit tank volume [kg/m3]	3	5
Ambient temperature [K]	288.15	288.15

Another interesting note is on the “Unknown” values:

- For the foam-insulated tank, the desired dormancy is imposed to compute the insulation thickness, while also constraining mass and diameter.
- For the vacuum-insulated tank, the dormancy will depend on the given insulation thickness and the surface area to volume ratio.

4.2 Results and analysis

Simulating the sizing of the two tanks in steady-state with Modelon Impact, we obtain the results summarized in Table 2. These serve as input to the dynamic assessments of the designs.

Table 2: Tank sizing results for Foam and MLI vacuum storage tanks intended for 2-3-2 seating aircraft.

	Foam	**Vacuum**
Inner volume [m^3]	235	176
Total length [m]	17.2	13.6
Vessel thickness [m]	0.0041	0.0021
Insulation thickness [m]	0.1289	0.127
Protection thickness [m]	0.0008	0.0266
Dry tank and accessories mass [kg]	5183	7221

DOI: 10.13009/EUCASS2023-500

The mission profile consists of an aircraft trajectory with take-off, climb, cruise, small additional climb, cruise again and descent. The mission profiles for both tanks are scaled to their capacities. Associated to the mission profile, the aircraft required power is computed, and as explained in the first section, the associated mass flow of hydrogen for a fuel cell propulsion design is estimated. Both the altitude and mass flow extractions are inputs to the model, as shown in Figure 12 and their trajectories are plotted in Figure 13. The altitude is used for an airData model to determine the corresponding ambient temperature and pressure whereas the mass flow rate is used on a boundary that extracts the liquid hydrogen from the tank. The checkValve is included to vent before the tank reaches the maximum design pressure and an optional heat source to the liquid volume is included. In this case no additional heat will be added.

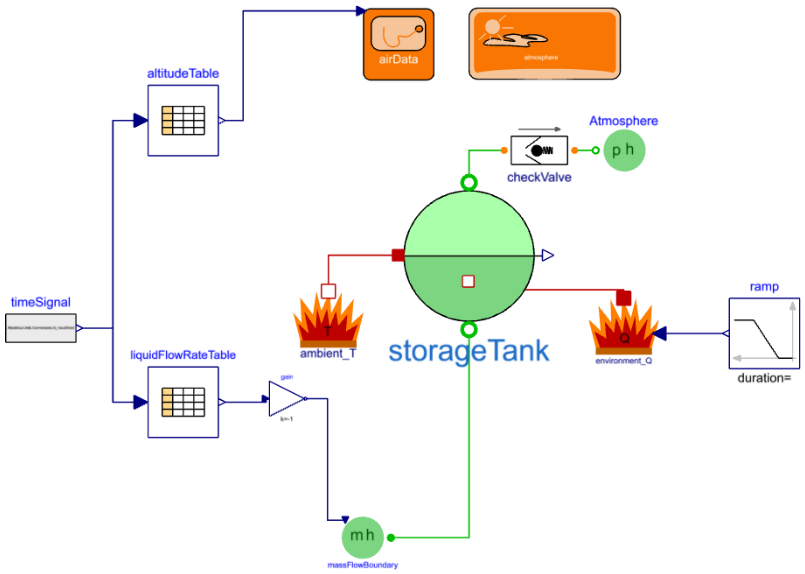

Figure 12: Model of the mission profile with altitude, mass flow rate and heat ingress as inputs.

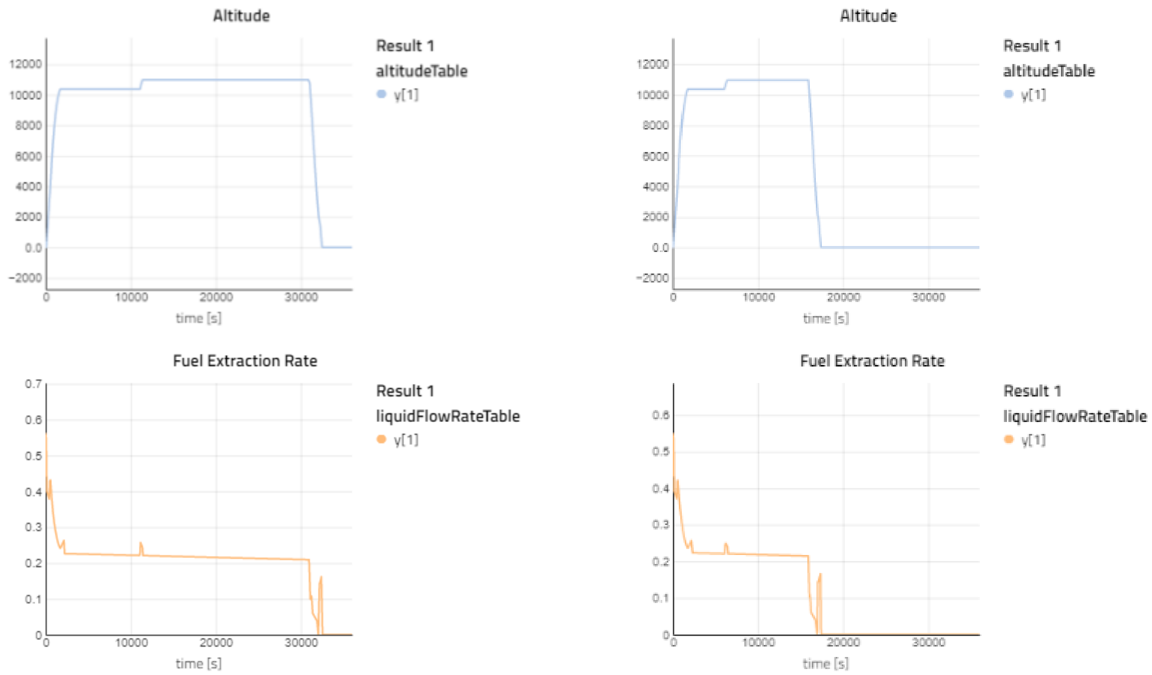

Figure 13: Mission profiles – foam on the left and vacuum on the right.

As previously mentioned, the flight durations are different due to the different available mass of hydrogen.
For the sake of conciseness, we will now look and compare only at a few variables of interests. We represent the tank pressures and gas and liquid temperatures, in Figure 14. It is observed that the temperatures in the foam-insulated tank rise due to heat ingress and in combination with the evaporation of liquid hydrogen to gaseous hydrogen, consequently the pressure rises – despite the mass extraction.

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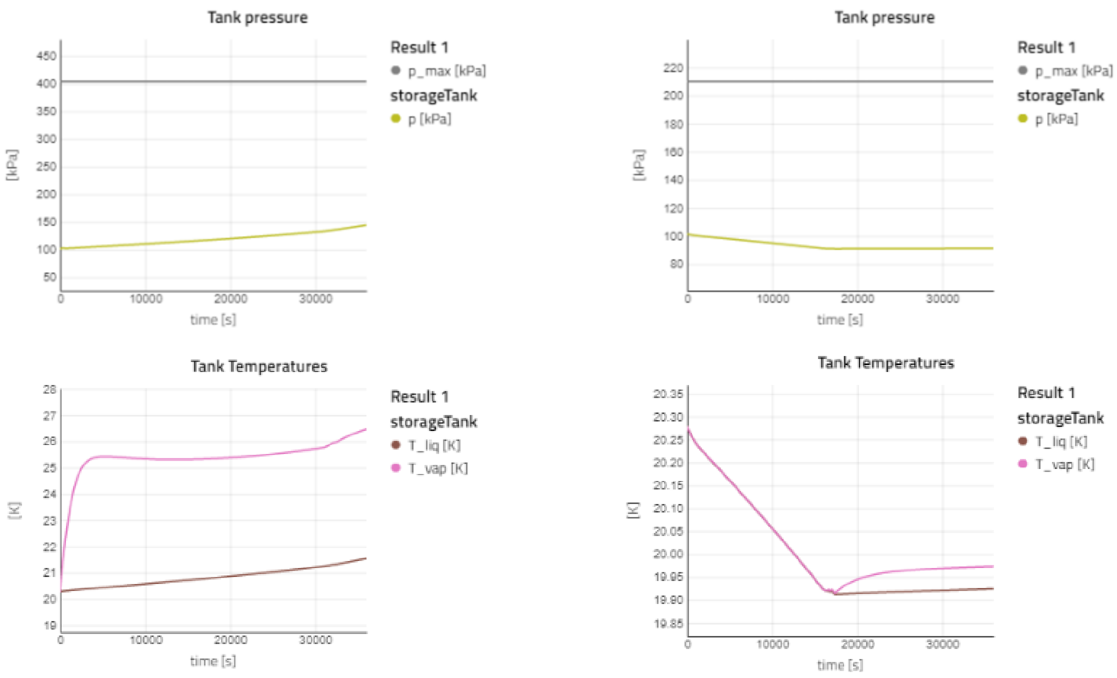

Figure 14: Pressures and temperatures – foam on the left and vacuum on the right.

On the other hand, for the vacuum-insulated tank, the temperatures decrease during flight – as the gas expands and the heat ingress is so low. The rate of evaporation is low and the expansion is almost isenthalpic. Consequently, the pressure drops below atmospheric pressure. This is in fact to be avoided as this means that air could leak into the cryogenic tank causing direct freezing and potential blocking of the extraction pipe. Therefore, it is recommended to maintain a minimum pressure of about 1.2 Bar at all times – preventing an internal leakage [5]. This can be achieved by actively heating the tank – which should be done here in the case of the vacuum-insulated tank.

4.3 Opening to dynamic optimization capabilities

As the core of this paper explained and this use case illustrates, an accurate design of a liquid hydrogen cryogenic storage tank relies on steady-state sizing combined with dynamic assessment of performance, and iterations based on combined results. The main reason is that the criteria for performance can seldomly be included in the steady-state sizing – consider the control of the active heating to prevent getting below a minimum pressure in a vacuum-insulated tank.
However, there is a technology capable of both sizing for steady-state and dynamic cases simultaneously: dynamic optimization. To the authors' knowledge, there is no publication using this technology to discuss the sizing of aircraft cryogenic storage tanks.
Modelon Impact embeds this technology and proved to be useful in several past applications, recently on sizing and simultaneously optimizing the trajectory of a drone [26] [27]. Using this technology in the context of aircraft cryogenic tanks is left out as a perspective to this paper and still worth mentioning for completeness of the overall simulation ecosystem.

5. Conclusion

This paper focused on a curated review of the literature dedicated to designing liquid hydrogen cryogenic storage tanks for aerospace application – from the main sizing equations to the performance assessment scenario and including some relevant metrics. This review was presented from the standpoint of the different technologies of simulation – ranging from steady-state to dynamics and opened the discussion to CFD and dynamic optimization. Both steady-state and dynamic simulations were used in a use case scenario to compare the impact of different materials on the tank designs and performance. It was shown that the foam insulation tank allows larger flight ranges at the cost of letting more heat ingress in the tank that could lead to venting in some scenarios. The vacuum insulation tank requires an additional mass of accessories which reduces its capacity (for a fixed MTOW) and, if not conveniently used as a heat sink, requires heating to control the pressure from dropping below the minimum acceptable – while its dormancy is much higher due to very low heat ingress.

DOI: 10.13009/EUCASS2023-500

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