

A Leaf-Vein Hydrogen Spar Architecture for Cryogenic Fuel Conditioning and Distributed Waste-Heat Rejection: Toward Low Infrared Contrast in a Long-Endurance UAV

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Hydrogen's high specific energy makes it attractive for long-endurance UAV missions. This paper investigates the thermal architecture of MARID, a proton exchange membrane (PEM) fuel-cell UAV concept. Hydrogen stored in liquid form absorbs heat at the motor mount, warms to near-ambient temperature, and enters a recirculating 50 bar loop. Fuel tapped from the loop passes through a coolant-heated jacket to the anode. Leaf-inspired networks beneath the inboard wing and V-tail skins, together with a fuselage network, reject heat over 6.86 m^2 of modeled active skin. At 3,000 m, a steady-state para-hydrogen model predicts 4.195 kW of loop rejection, about 86% of modeled waste heat, with 77 W pump input and a 9.96 K area-mean active-skin temperature rise above ambient. The 10 m planform gives $L/D = 27.64$ at 28.73 m s^{-1} equivalent airspeed under an assumed drag build-up with tripped cooled surfaces. For the 48-hour design mission, mass convergence predicts 200.4 kg gross, including 11.50 kg of loaded hydrogen with its unusable ullage residual and three vacuum-jacketed spherical tanks totaling 17.89 kg dry. Results depend on assumed inventory, skin conductance, and component capacities.

Nomenclature

Symbols

A	Surface area, m^2 .	$A_{f,i}$	Area of face f of surface i , m^2 .
$A_{\text{proj},i}$	Projected sunlit area, m^2 .	c_p	Isobaric specific heat, $\text{J kg}^{-1} \text{K}^{-1}$.
D	Tube inner diameter, m.	G	Solar flux on the projected area, W m^{-2} .
$h(T, P)$	Hydrogen specific enthalpy, J kg^{-1} ; bare h : convective coefficient where stated.	h_c	Fuel enthalpy, cold-plate outlet, J kg^{-1} .
h_{pump}	Fuel enthalpy, feed-pump outlet, J kg^{-1} .	h_{ref}	Reference enthalpy $h(T_{\text{amb}}, P_a)$, J kg^{-1} .
h_{ext}	External convection coefficient, $\text{W m}^{-2} \text{K}^{-1}$.	h_r	Radiation secant coefficient, $\text{W m}^{-2} \text{K}^{-1}$.
k	Thermal conductivity, $\text{W m}^{-1} \text{K}^{-1}$.	t	Skin thickness, m.
kt	In-plane skin conductance, W K^{-1} .	L	Length, m.
m	Fin parameter, m^{-1} ; mass where stated, kg.	$\dot{m}$	Mass flow rate, kg s^{-1} .
$\dot{m}_f$	Fuel consumption rate, kg s^{-1} .	M_b	Wing-root bending moment, N m.
N	Number of secondaries.	n	Number of parallel rejection surfaces; load factor in the root-moment and gust relations.
Nu, Re	Nusselt and Reynolds numbers.	P	Pressure, Pa.
P_1	Feed-line pressure (100 bar).	P_2	Rejection-loop pressure (50 bar).
P_a	Anode supply pressure (3 bar).	p	Tube pitch, m.

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ℓ	Strip half-pitch $p/2$, m.	Δp	Pressure drop, Pa.
Q	Heat-transfer rate, W.	Q_{motor}	Motor and inverter waste heat, W.
Q_{plate}	Motor heat to fuel at the cold plate, W.	Q_{pre}	Loop heat to fuel before the tap, W.
Q_{jacket}	Coolant heat to tapped fuel, W.	$Q_{\text{fuel,net}}$	Net fuel heat sink from h_{pump} to h_{ref} , W.
Q_{sens}	Fuel sensible-enthalpy inflow relative to h_{ref} , W.	Q_{stack}	PEM waste heat, lower-heating-value basis, W.
Q_{HX}	Stack-to-hydrogen exchanger duty, W.	$Q_{\text{exhaust,stack}}$	Stack heat in the cathode exhaust, W.
$Q_{\text{band},i}$	Signed conduction to unnetworked skin, positive outward, W.	q_s	Absorbed solar flux, upper face only in the reference case, W m^{-2} .
r	Aerodynamic recovery factor.	$R_{\text{int},i}$	Effective internal-film and tube-wall resistance of surface i 's branches, excluding skin spreading, K W^{-1} .
T	Temperature.	ΔT	Temperature difference.
T_{amb}	Ambient temperature, K.	T_{aw}	Adiabatic wall temperature, the convection reference, K.
T_{stack}	Stack/anode-inlet temperature, K.	T_{tap}	Hydrogen temperature at the tap, K.
T_{loop}	Mean loop hydrogen temperature, K.	$T_b, T_{b,i}$	Common tube-contact skin temperature for both faces, K.
$T(x)$	Strip temperature at x , K.	$\bar{T}, \bar{T}_{f,i}$	Strip mean; mean of face f on surface i , K.
T_e	Strip-linearization equilibrium; not necessarily the separately solved passive-skin temperature, K.	$T_{\text{bg},f}$	Radiative background of face f (sky or terrain), K.
UA	Conductance, W K^{-1} , as identified in context.	V	Airspeed, true airspeed in heat transfer, m s^{-1} .
x	Strip coordinate, 0 to ℓ , m; or streamwise distance.	α	Solar absorptance.
ε	Thermal emissivity.	η	Fin efficiency; component efficiency where stated.
μ	Dynamic viscosity, Pa s.	ρ	Density, kg m^{-3} .
σ	Stefan–Boltzmann constant, $\text{W m}^{-2}\text{K}^{-4}$; or stress, Pa.	σ_θ	Hoop stress, Pa.
σ_r	Radial normal stress, Pa.	σ_{vm}	Von Mises equivalent stress, Pa.
τ	Shear stress, Pa.		
<i>Subscripts and indices</i>			
i	Surface or circuit index.	f	Face index; consumed fuel in $\dot{m}_f$.
$\uparrow, \downarrow$	Upper and lower face.	amb, s , stack	Ambient, skin and fuel-cell stack.

Conventions. Operating temperatures are reported in $^{\circ}\text{C}$, while temperature differences, cryogenic temperatures and all thermodynamic and radiation calculations use K. Heat leaving a skin face by convection or net radiation is positive, so radiation can change sign against a warmer background.

I. Introduction

I.A. Motivation

Inspired by nature, humans have created innovations over the years to meet different needs, even machines shaped like us—what we currently call humanoid robots. We can always learn from nature, and I approached it from an aerospace perspective. Drones inspired by nature's birds and leaves would be similar to robots shaped like us. Birds do run on stored chemical energy, and their blood carries energy and heat around the body. They also do not have separate aircraft-style ailerons and flaps, and leaf veins both carry fluid and help support the leaf's shape. The inspiration led me to a hydrogen PEM fuel-cell concept, where

hydrogen is the circulating fluid and fuel that spreads life through the drone, flowing through venation inside its fuselage, wings and V-tail. The main vein, the wing spar, carries the wing's loads, and the branches are intended to help support the wing structure. The circulating hydrogen provides thermal management. Hydrogen PEM fuel-cell systems can offer higher system-level specific energy than lithium-ion batteries.¹ An appealing engineering approach would be to use PEM fuel cells and design UAVs for long-endurance flights—beyond 24 hours; MARID's design mission is 48 hours.

Like birds, which regulate their body temperature, PEM fuel-cell systems require thermal management. Bird body temperatures are often around 39–43 °C during normal activity and vary with species and activity level,² while PEM stacks operate near 60–90 °C.³ Hydrogen, which is stored at about 20 K, must enter the anode at the selected target temperature of 77.5 °C. That makes conditioning the fuel to reach an operating window especially important. For the design point adopted in this paper, the gross mass is 200.4 kg; the fuel absorbs 325 W along its path, of which only 246 W is a net heat sink relative to the ambient-temperature and anode-supply-pressure enthalpy reference. A second system requirement is waste heat management, a recognized challenge for fuel-cell UAVs.⁴ Dedicated radiators add dry mass and can concentrate substantial heat loads into a small surface running well above ambient.⁵ At this design point, the PEM stack produces 3.919 kW of waste heat. The fuel's total heat uptake is small compared with the stack's waste-heat output; hence, disposing of the excess heat is essential for a properly functioning UAV system. Despite a temperature rise of about 330 K, the low fuel mass flow limits heat uptake. At nominal cruise, the motor and inverter together dissipate about as much heat as the fuel absorbs along its complete conditioning path. Therefore, the main challenge for this architecture is, in principle, dissipating the excess heat generated in the process. For a long-endurance hydrogen-powered UAV, I wanted to distribute waste heat across the airframe while limiting cooling-system mass and concentrated hot surfaces.

This asymmetry, a small fuel heat sink against a large waste-heat output, is what the nature-inspired MARID concept exploits. Cold hydrogen absorbs heat at the motor mount, whose near-ambient exterior temperature remains a sizing target. That same hydrogen performs another role in a closed loop: it collects the stack-exchanger and assigned component heat loads and carries them to the wings, V-tail and fuselage for rejection through the skin.

I.B. Proposed Approach

Hydrogen flows in one circuit with a tap. From storage near 20 K under the assumed 2 bar tank pressure, a pump draws liquid hydrogen and raises its pressure to 100 bar. The fuel then warms toward ambient temperature through a cold plate under the aft motor mount. It then enters through a regulator into the cold return of a closed, recirculating rejection loop at 50 bar. The loop circulates at a mass flow rate of 35.6 g s⁻¹ at near-ambient temperature through the motor and avionics cold plates, the stack's heat exchanger, and out to the leaf-inspired venation inside the cooled inboard wing sections, V-tail, and fuselage. The loop includes 127 g of circulating hydrogen, which is counted as dry mass. Immediately downstream of the stack exchanger, the warmest bulk-fluid station in the modeled sequence, a tap draws off fuel at exactly the stack's consumption rate. The tapped fuel is throttled to 3 bar, where a small jacket on the stack's own coolant supplies the remaining enthalpy lift to reach the 77.5 °C anode inlet—see Figure 1(a), step 7, and the tap annotation in panel (b). The PEM stack retains its own internal water-glycol coolant, which exchanges heat with the hydrogen through the stack exchanger and fuel jacket. Hence, the fuel rides along the rejection loop, performing dual roles at once. It carries excess heat through loop circulation while continuously supplying fuel to the operating PEM stack. Like leaf venation,⁶ the structural spar bores feed the respective wing and V-tail networks, where secondaries branch from them and feed the return tubes along the periphery of each lifting surface. During startup, heat exchange through the skin can reverse direction; the loop is intended to prewarm the hydrogen before the trim heater supplies the remaining lift (Section II.C).

At mid-chord lies the wing's main vein, feeding chordwise secondaries beneath each skin at thirty-seven stations with a modeled pitch of 34 mm. Secondaries connect through reticulate cross-links intended to provide alternative flow paths; however, the current research does not yet model blocked-path performance. The paper presents a one-dimensional manifold calculation resolving the nominal branch distribution. The skin calculation resolves an analytical strip between a tube and its mid-pitch symmetry line. Loss of feed supply and loss of circulation are different faults, investigated further in Section VI.A.

The independently rotating wings and V-tail also draw inspiration from avian adjustment of lifting-surface orientation.⁷ The proposed configuration avoids separate trailing-edge control surfaces and their dedicated servos and linkages, leaving more uninterrupted space within the wings for the hydrogen network.

In this paper, I evaluate the proposed architecture with a steady-state thermal model, sensitivity studies and coupled mass calculations. The aerodynamic and preliminary structural calculations support the heat-management study; they do not establish a validated aircraft design.

I.C. Related Work

Using a vehicle’s fuel to absorb heat is not new; in fact, it has a long engineering history. Liquid-propellant rocket engines have long used this approach: in regenerative cooling, one or both propellants flow through passages in the thrust-chamber wall before injection.⁸ Turbine aircraft also use fuel as a heat sink, transferring heat from engine lubrication, generators, and environmental-control systems into the fuel flow.⁹ Actively cooled structural panels rejecting heat to the hydrogen fuel were studied for hypersonic cruise at Mach 6, with a design target of keeping the aircraft’s surface below 394 K.¹⁰ Heat-transfer studies of supercritical hydrogen in rocket cooling channels¹¹ are relevant to MARID’s motor cold plate, where the fuel crosses its critical temperature.

Related studies have examined hydrogen as a heat sink for fuel-cell cooling³ and aircraft surfaces for rejecting waste heat.^{12,13} In this research, I bring these ideas together in MARID through what I describe as a “double duality.” First, hydrogen serves as both the fuel and the thermal-management fluid of the system: the circulating hydrogen absorbs waste heat from the system’s components and delivers it to the fuselage, wing, and V-tail skins for rejection to the surroundings. Second, the main veins are intended to serve as both structure and conduit: the spars would carry the loads of the rotating wings and V-tail while feeding their networks through their bores. MARID’s proposed contribution is the specific integration of these functions within a leaf-vein architecture.

II. MARID Platform

II.A. Vehicle Overview

The MARID is a Multi-Axis Rotary Integrated Design quasi-fixed-wing UAV.¹⁴ Inspired by birds’ physiology and moving a step toward biomimicry, its two main wings and two V-tail surfaces rotate independently and are intended to provide roll, pitch, and yaw control; control authority is not validated here. Because each whole surface rotates, the control surfaces are large, which is intended to preserve control at slow speeds within the wing’s stall margin. I simulated three iterated preliminary CAD designs in ROS 2 with Gazebo and spent hundreds of hours flight-testing them in simulation as part of separate guidance, navigation, and control work on MARID. I have not yet completed an aerodynamic efficiency analysis or reliable CFD runs on a finalized CAD. The initial wing deflection range is set to ± 0.5 rad and may change to adapt to different drone shapes, wing sweeps, speeds, and flight regimes. The fuselage, V-tail, and joint limits are current simulation model values and are provisional. The 10 m planform consists of two 4.5 m wing panels and the 1.0 m carry-through between their roots (Section VI.C); their cooled inboard sections carry the venation of Section III.B, and their dry outer panels carry no network in this current design assumption. A further extension of the venation span is plausible and warrants future study. For the design target, I picked a 48-hour flight and solved the gross mass, sized structure, fuel and tanks together; no fuel reserve beyond the unusable ullage residual is carried. A reserve could be a mission-rules choice outside this study, potentially taking allocated payload at the same gross mass, provided larger tanks fit the fuselage.

The fuselage is a carbon-fiber composite shell carrying the aluminum network beneath its skin, as the wings do; its structure is not sized here. Key geometric and mass properties are summarized in Table 1.

Table 2 lists the unsized component inventory. The entries are estimates; each item’s basis appears in the last column. Rotation-drive reference data come from the Harmonic Drive CSG/CSF catalog and CSG-40-100-2UH-LW product entry (3.5 kg gear unit); this is not a complete installed-drive mass. A generic liquid-cooled PEM system is assumed. The air-cooled Intelligent Energy IE-SOAR 2.4 kW, 4.8 kg module provides a commercial mass reference for the provisional inventory allowance; no specific stack is selected, and the assumed electrical efficiency is specified independently. The battery basis is 48 cells near 16.2 Wh and 70 g each, with enclosure and management allowances. The exterior finish allowance is a 25 μ m epoxy primer and a 75 μ m polyurethane topcoat,^{15,16} 0.152 kg m⁻² dry over the aircraft’s 15.05 m² wetted area; the data sheets’ thickness ranges give 0.094–0.161 kg m⁻².

Table 1: Selected conceptual vehicle properties. Fixed mass is the unsized component inventory, two sized wings and the sized thermal items; fuel and tanks follow from convergence.

Parameter	Value	Basis
Gross mass	200.4 kg	Coupled 48-hour calculation, reference (MID) drag assumptions of Section VI.C and reference inventory
Reference unsized inventory	122.35 kg	Component inventory, not a weighed target
Fuselage length	3.0 m	Preliminary simulation geometry
Fuselage diameter	0.62 m	Exterior diameter; installed tank fit unresolved
Wing span, overall	10.0 m	Two one-piece 4.5 m panels and carry-through
Cooled wing section, root to boundary	1.35 m	0.795 m ² planform per wing
Wing chord, root / boundary / tip	0.649 / 0.529 / 0.25 m	Single taper
Wing root offset from CL	0.50 m	Preliminary joint position
Wing mass (each)	13.92 kg	Sized spars plus skins, ribs and splice allowances
Design loiter	3,000 m, 28.73 m s ⁻¹ EAS	33.35 m s ⁻¹ TAS; 1.3 times stall speed
Design endurance	48 h	No mission reserve beyond unusable residual
Payload and growth	33.3 kg; 200 W	Separate model inputs, mass and power counted once

Table 2: Unsized component inventory (kg). Low, reference and high values are indicative assumptions and source-informed allowances, not measured limits or targets. Shell, tail and exterior finish use an areal-mass method; drive and stack values use component reference data with installation allowances; the other allocations remain provisional. The payload/growth allocation does not demonstrate useful payload or installed mass.

Item	Low	Reference	High	Basis
Fuselage shell	14.0	20.5	28.0	Wetted-area structural allowance
V-tail structure	6.0	8.8	12.1	Area-based allowance
Exterior finish	1.4	2.3	2.4	Primer and topcoat, wetted area
Four rotation drives	14.0	18.0	22.0	CSG/CSF gear data; motor, brake and installation allowances
PEM stack	8.0	12.0	20.0	Air-cooled IE-SOAR mass reference; provisional liquid-cooled system allowance
Propulsion motor	4.0	5.0	9.0	Motor, propeller and controller allowance
Liquid-hydrogen feed pump	1.0	3.0	5.0	Un sourced installed-mass allowance
Tank mounts, lines and access	2.0	3.0	4.5	Provisional allocation
Battery and pack	4.0	4.5	6.0	48 cells plus pack allowance
Flight avionics	1.0	2.5	4.0	Provisional allocation
Landing gear	4.0	6.0	12.0	Provisional allocation
Harness	2.0	3.5	5.0	Provisional allocation
Payload and growth	33.3	33.3	33.3	Selected mass input
Total unsized inventory	94.78	122.35	163.34	Excludes sized wings, thermal items, fuel and tanks

II.B. Propulsion and Energy System

The primary energy source is a PEM hydrogen fuel cell stack supplemented by a lithium battery buffer that serves three roles: (i) a conservative safety-margin reserve against PEM underperformance or failure in flight, together with peak transient power for takeoff and for the harmonic-drive wing-rotation actuators (Section III.C) during aggressive maneuvers, neither quantified here; (ii) avionics and housekeeping power during cold start, before the fuel cell reaches operating temperature; and (iii) the startup trim heater (Section II.C), which is stack-powered on the fault route of Section VI.A. Liquid hydrogen (LH_2) is stored at 2 bar and reaches the fuel cell through the rejection loop (Section III.A). Climb power dispatch and the stack's and motor's continuous and peak ratings are not sized here; the 48-hour endurance is the steady-loiter calculation.

Table 3 lists the primary waste heat sources and their locations, as derived from the power budget in Section IV.A.

Table 3: Illustrative waste-heat multiples at the design loiter. The last column multiplies motor heat by 4.523, stack heat by 3.0 and the 500 W hotel allocation by 2.0. These independently imposed multipliers are not a balanced takeoff electrical load, takeoff/climb solution or battery-sizing requirement.

Zone	Location	Loiter (W)	Illustrative multiples (W)
Aft motor mount (single)	top-mounted, tail tip	329	1,490
Avionics, payload and four actuator jackets	common plate, wing and V-tail roots	500	1,000
PEM fuel cell stack	fuselage bay, position not fixed	3,919	11,756
Total		4,748	14,246

II.C. Startup and Battery Sizing

Startup is a proposed mechanism, not a solved transient. With the motor off there is no motor-generated waste heat, although the cold plate, loop inventory and connected structure can begin with stored heat. Until the stack coolant becomes warm, the fuel jacket has no stack waste heat to supply. The first fuel follows the normal route into the rejection loop, drawing initially on that stored energy and then on the surroundings through the skin by natural convection and radiation. As the stack warms, recovered waste heat progressively takes over fuel conditioning.

Two limits distinguish this process from cruise. Heat drawn from the surroundings can bring the fuel only toward ambient, so the remaining lift to the anode setpoint must come from the electric trim heater. Ground natural convection also cannot be replaced by the cruise forced-convection coefficient when estimating replenishment. Startup flow, duration, circulation power and supplementary heating therefore remain to be quantified, together with whether any surface falls far enough below ambient for condensation or frost to matter.

The proposed startup heating process conditions the hydrogen; it does not establish a cold-start capability for the stack. Product-water icing in a cold-soaked catalyst layer is a separate concern that warm anode gas does not by itself resolve. The trim heater is also proposed for the in-flight tank-direct route of Section VI.A. Its fault calculation at the loiter condition requires 371 W at the increased fuel flow; this is not a startup or takeoff rating.

The battery-energy allowance is

$$E_{\text{bat}} \approx 777.6 \text{ Wh}, \quad m_{\text{pack,ref}} = 4.5 \text{ kg}. \quad (1)$$

At illustrative specific energies of 250 and 200 Wh kg^{-1} , dividing 777.6 Wh by those values gives energy-only masses of 3.11 and 3.89 kg, respectively. That quotient does not include a separately demonstrated enclosure, management system, wiring or installation. The inventory uses the stated 48-cell battery with pack allowances (Table 2). Neither estimate is a mission-phase energy balance or a demonstrated cold-start, maneuver or fault reserve.

The DOE automotive record lists startup status values of 20 s at -20°C and less than 10 s at $+20^{\circ}\text{C}$ to reach 50% rated power.¹⁷ These reported automotive results do not establish the startup response of the unspecified aircraft stack. Recharging from excess fuel-cell capacity and holding a reserve state of charge are proposed control functions, with no demonstrated recharge margin or selected state-of-charge schedule here.

III. Hydrogen Circuit Architecture

III.A. One Hydrogen Circuit with a Tap

Outside the stack the aircraft carries one fluid, hydrogen, and the fuel's route to the anode runs through the rejection loop. The nominal fuel path is:

1. **Tank and feed pump.** Three equal vacuum-jacketed spherical tanks supply liquid para-hydrogen near 20.2 K under the assumed 2 bar initial ullage. The pump raises it to 100 bar for 14.4 W and an outlet near 32.8 K.
2. **Motor cold plate.** The fuel absorbs 248 W of the 329 W motor-and-inverter heat, limited by its enthalpy capacity to ambient. The remaining 81 W enters the loop. A near-ambient mount is a sizing target, not a solved surface temperature.
3. **Injection and exchanger.** An isenthalpic regulator admits 0.0653 g s^{-1} of fuel into the 50 bar cold return. The 35.6 g s^{-1} circulating loop takes 3,553 W from the stack exchanger; other modeled additions precede it, so its outlet is the warmest bulk station in this assumed sequence.
4. **Tap and anode jacket.** In Figure 1, the fuel is tapped near 12.9°C and throttled to a 3 bar supply; downstream anode regulation must maintain the membrane's allowable pressure differential from the cathode. Its uptake from loop mixing is 16 W; the jacket adds 61 W to reach 77.5°C . Purge loss, anode recirculation power, stack coolant pumping power, and humidification and water management are not independently resolved.

The fuel takes 325 W in total from the plate, loop and jacket. Its net sink is 246 W, defined relative to hydrogen at ambient temperature and the 3 bar anode supply pressure. At the stack inlet the fuel carries 79 W of sensible enthalpy above that same ambient-temperature, 3 bar reference; this energy enters the stack heat balance. The jacket and mixing uptake are therefore not independent disposal terms. The loop's 127 g charge turns over in about 3.6 s. Only the fuel-consumption stream leaves through the tap; the remainder continues to the skins, returns to the cold side and recirculates. Usable inventory buffering depends on allowable pressure and inventory drawdown while maintaining fuel delivery and cooling.

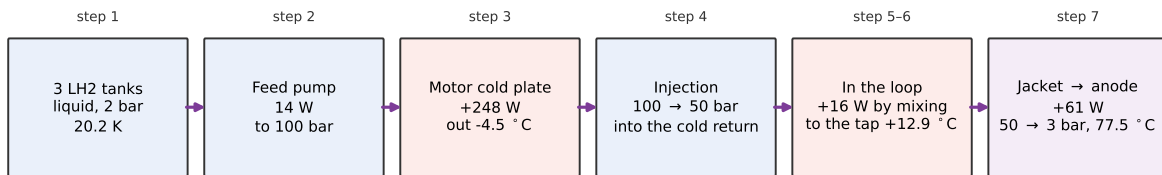
The rejection loop carries 4,195 W to 6.86 m^2 of active skin. The bulk hydrogen temperature difference between the warm supply and cold return is 7.79 K, while the area-weighted mean active-skin temperature is 9.96 K above ambient. The nominal flow is approximately 512 L min^{-1} at the solved state, and the 77 W pump input includes the common series path and the parallel route with the highest pressure drop. The stack exchanger requires $UA \approx 52.35\text{ W K}^{-1}$ under prescribed 80-to- 75°C coolant endpoints, a conductance requirement. The common avionics/payload plate receives 400 W, including the 200 W payload; two wing jackets receive 70 W and two V-tail jackets 30 W, so all 500 W of bus-side hotel electricity is counted once. Each jacket has a 0.15 kg mass allowance and each active route a 500 Pa loss allowance; four jackets do not constitute four series losses.

One hermetic, oil-free circulator is proposed for the common route and the highest-head surface path, with balancing valves holding the split and a standby pump against its loss (Section VI.A). In the model, a flow-control rule keeps total flow at or above the nominal minimum and raises it when required by the branch Reynolds-number criterion, evaluated using properties at the loop mean temperature; the loop temperature rise varies below the 20 K ceiling.

The makeup regulator and tap are the two connections between the fuel line and the recirculating loop. Pressure regulation allows the loop to operate below the feed pump's 100 bar, reducing pressure demands on the network and rotating unions while keeping hydrogen dense enough for the assumed header sizes. The loop pressure of 50 bar exceeds hydrogen's approximately 13 bar critical pressure, and its nominal temperature is far above the approximately 33 K critical temperature, so hydrogen remains single-phase at the nominal states. The only normal fuel-path crossing of the critical temperature against a heated wall is on the cold plate, at the higher pressure chosen for that purpose.

The stack's internal coolant is the small water-glycol loop assumed in this design's bipolar plates. It transfers heat to two external components: the heat exchanger to the rejection loop and the fuel jacket. Its mass is carried as an allowance in the thermal-component inventory. It is not replaced by the hydrogen loop for a physical reason as much as a practical one: at 50 bar and 15 °C hydrogen holds $62 \text{ kJ m}^{-3} \text{ K}^{-1}$ against about 3,500 for a water-glycol mixture, so cooling the cells directly with hydrogen would need some 57 times the volumetric flow through the plates' coolant passages for the same heat duty and bulk temperature rise, with the pressure drop that implies and 50 bar hydrogen sealed against the cathode inside the stack. The stack keeps the coolant it was built for; the hydrogen takes over at its exchanger.

(a) the fuel's route, tank to anode: three pressures and the three places it takes heat

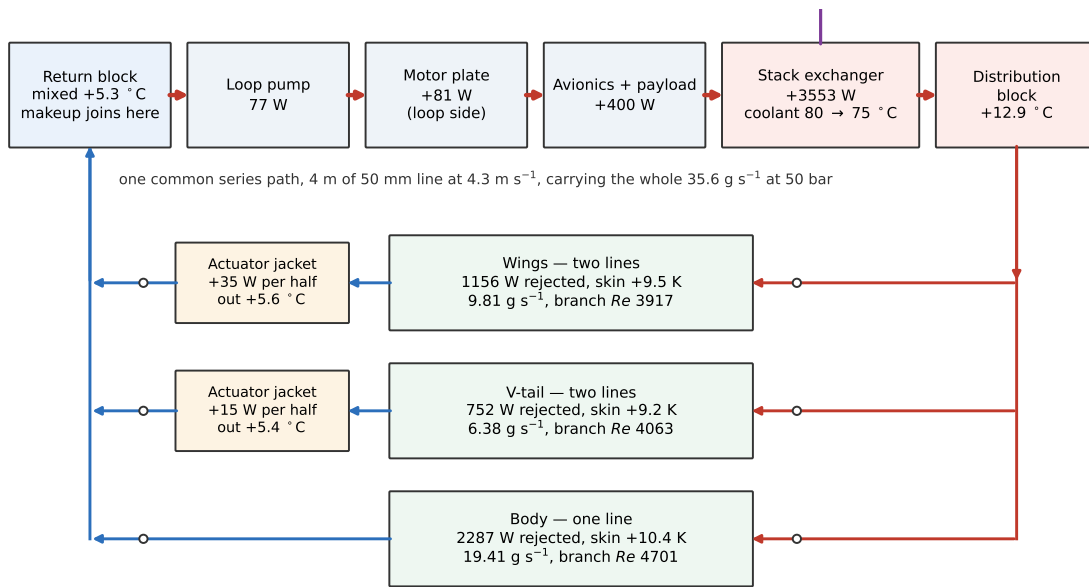


0.0653 g s⁻¹ of fuel; it takes 325 W in three places; its net sink is 246 W, the cold plate's 248 W less 2 W it still holds above ambient at anode pressure

the middle stretch is loop gas: the fuel is injected into the loop's return and drawn off at the tap

tap: 0.0653 g s⁻¹ to the jacket and anode

(b) the rejection loop: one common path, one distribution block, three surface circuits



ten line shut-offs; modeled allowance: two balancing valves and three circuit flow sensors. The five separate returns meet in the return-block gallery; installation interfaces are not itemized.

red: feed at +12.9 °C blue: surface return at +5.1 °C purple: the fuel ○: shut-off

Jacket outlets and return before fresh fuel mixing: common surface-return temperature assumed; constant hydrogen heat capacity; local jacket temperatures are not independently resolved.

Figure 1: **The circuit, drawn from the model at the design point.** (a) The fuel route from tank to anode. (b) The rejection loop: a common series path feeds five parallel lines, each with a shut-off at both ends; wing and V-tail actuator jackets lie on their respective return lines. Station order is the lumped model's assumed sequence. Fluid temperatures are bulk values; skin rises are surface averages.

Water-glycol is a credible alternative for distributed skin cooling. Hydrogen is investigated here for its low inventory mass within the structural passages, its direct integration with the consumed fuel stream

and its freedom from freezing at the atmospheric temperatures considered, despite its large volumetric flow. A water-glycol network would require concentration selection and assessment of cold viscosity, stagnant-branch freezing and restart; the retained stack circuit still needs its own freeze protection. Filling the modeled passage volume of 30.6 L with a liquid would weigh tens of kilograms, but a liquid system would use different passages and components; with no matched liquid-coolant system sized, comparative mass and pumping benefits remain unestablished.

Sharing the fuel and rejection fluid also couples cleanliness and pressure-boundary requirements. Braze residue, seal outgassing and corrosion products could reach the anode, so the proposed tap path includes particulate filtration and a getter ahead of the jacket, not yet sized. The pressure boundary must prevent air ingress during filling, shutdown and maintenance as well as leakage in operation.

Figure 2 shows the wing network. The spar's bore is a spanwise primary vein feeding chordwise secondaries under the skin, so the spar carries aerodynamic bending and torsion while its bore and the skin serve as fluid conduit and heat exchanger. At the root, the concentric union of Section III.C passes the feed into the bore and collects the return from the leading- and trailing-edge headers; nothing at 100 bar and no cryogen crosses the joint.

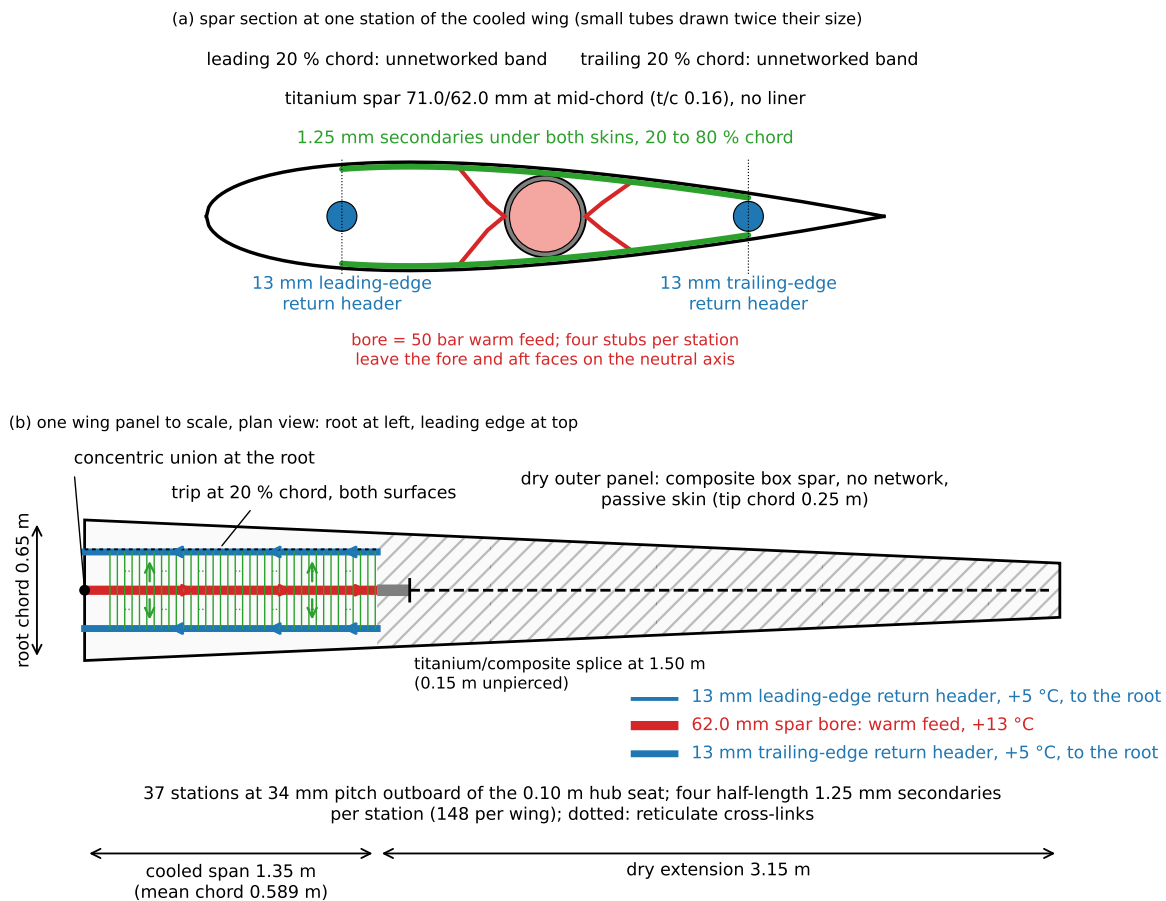


Figure 2: **The leaf-vein circuit in the wing, drawn from the model's dimensions.** (a) Spar section at one cooled station: the titanium spar at mid-chord carries the warm loop feed in its bore; secondaries leave through stubs on its fore and aft faces and run under both skins to the leading- and trailing-edge return headers. (b) One 4.5 m wing panel to scale: the secondaries lie chordwise, with reticulate cross-links between neighbors, over the cooled 1.35 m inboard section, and the headers return to the root union; the titanium spar ends at the 1.50 m splice, and the dry outer panel carries the composite box spar and no network. Red is the feed, blue the cooled return, green the secondaries; the small tubes in (a) are drawn at twice their size. Section III.B gives the dimensions.

III.B. Venation Geometry

Murray’s law ($D_{\text{parent}}^3 = \sum_i D_{\text{child},i}^3$) balances pumping work against a fluid-volume or maintenance cost for a branching tree. This network is instead a manifold: a header feeds dozens of identical branches in parallel, and near-uniform branch flow depends on small variation in the supply-to-return pressure difference relative to branch loss. The headers are therefore sized for distribution and are larger than Murray’s bore; the wing’s 62 mm spar bore, for example, compares with 6.6 mm from Murray’s law (Section V).

The wing spar is the mid-chord pivot; the network is arranged symmetrically about it. At each of thirty-seven stations per wing on an assumed 34 mm pitch, outboard of the 0.10 m hub seat, four half-length secondaries leave the spar through small stubs welded into its fore and aft faces, on the neutral axis where the bending stress is zero. They run forward and aft under the upper and lower skins, each over half of the modeled 60% of the local chord, into leading- and trailing-edge return headers that flow back inboard, the U arrangement of a leaf’s marginal veins. No tube runs inside the spar, and no detachable fitting exists between spar and secondary: each branch is permanently joined through a welded titanium stub and a titanium-to-aluminum transition, and reticulate cross-links give every branch an alternative path. The 177 mm secondary is a modeled length, 30% of the mean chord; its curved route to the skin and header is provisional, and its hydraulic and bonded lengths have not been established separately.

The provisional V-tail repeats the pattern at 60% of the wing’s networked area, on an assumed 0.45 m chord and a derived 1.08 m span per surface, fed through its own spar bore. The fuselage carries 122 circumferential half-hoops of 5 mm bore between two 30 mm longitudinal headers over the assumed networked run from 0.48 to 2.55 m, with the unnetworked nose and tail regions described in Section III.G, its return leaving at the aft end beside the pump. Table 4 gives the geometry and the design-point hydraulics of Section V.

Table 4: Nominal rejection geometry and hydraulics at 3,000 m and 50 bar, with 7.79 K bulk loop rise. Bores are inner diameters. The wing and V-tail feeds are spar bores; each secondary is assumed thermally coupled to the skin over its full modeled length. Circuit pump entries are the power required by each balanced surface route; their sum excludes the common series path and the throttling needed to balance routes on one pump. The total, 77.0 W, uses the highest route pressure drop (V-tail) plus the common path. Walls and provisional component-loss allowances are stated in the text.

Surface	Modeled active area m ²	Secondaries count	Modeled length mm	Bore mm	Headers mm	Flow g s ⁻¹	<i>Re</i>	Δp kPa	Pump W
Wings (both, both skins)	1.77	296	177	1.25	62 spar + 4 × 13	9.81	3,917	1.8	7.1
V-tail	1.06	232	135	1.0	15 spar + 4 × 10	6.38	4,063	2.5	6.3
Fuselage	4.03	122	974	5.0	30 + 30	19.41	4,701	1.1	8.5
Total	6.86	650				35.60			77.0

Bores, branch count and the minimum loop-flow setting are chosen together. The bores in Table 4 are not a demonstrated geometric optimum; their coupled flow and pump-power trade is developed in Section V.B.

The 20–80% chord extent is a coverage assumption, not an optimized one. Greater coverage could add rejection area, subject to header accommodation and coupled changes in branch losses, tubing mass and external heat transfer. The leading and trailing 20% bands and the hub seat are unnetworked, the bands are not reserved for conventional flaps or ailerons, and the dry outer panels remain passive. Pitch, skin conductance and loop conditions jointly set the reported strip temperatures; no full-panel uniformity is established.

Pitch also carries an unoptimized mass trade. For a folded-core composite skin exchanger, Larschow et al.¹⁸ found that a gap of one channel width raised calculated heat rejection per unit mass by about 12% relative to zero gap, with water coolant included in the mass comparison and the optimum depending on material and boundary conditions; this motivates a spacing study for the hydrogen network but does not set its 34 mm pitch.

III.C. Structural Spar Sizing

The main spar is a hollow Ti-6Al-4V shaft that rotates relative to the fuselage through cross-roller bearings and is driven by a high-torque brushless motor and harmonic drive as part of the proposed rotation-control concept; control authority remains unvalidated. It must simultaneously carry the wing root bending moment from aerodynamic lift, the motor torsion transmitted to the wing, and the 50 bar of the loop it carries in its bore, while providing adequate shaft diameter for the motor clamp interface.

At the design load factor of 3.81, the discrete-gust case at the loiter speed, which exceeds the assumed 2.5 g maneuver case at this wing loading (Section VI.C), treating the lift as an elliptical spanwise distribution split evenly across the two main wings, each wing root carries a bending moment of

$$M_b = \frac{n m_{\text{gross}} g}{2} \times \frac{4}{3\pi} b_s = \frac{3.80913 \times 200.4 \text{ kg} \times 9.81 \text{ m/s}^2}{2} \times \frac{4}{3\pi} \times 4.5 \text{ m} \approx 7,151 \text{ N}\cdot\text{m} \text{ (limit)}, \quad (2)$$

where n is the design load factor, m_{gross} is gross mass and $b_s = 4.5 \text{ m}$ is the panel semispan from the wing joint to the tip and $4/(3\pi)$ locates the elliptical distribution's resultant from the root. The titanium spar runs 1.50 m from the root, the 1.35 m cooled section and a 0.15 m unpierced transition to the splice with the dry outer panel's composite spar. The titanium spar carries the root moment of the entire panel through load transfer from the composite extension. The composite outer spar is sized separately, with a provisional splice allowance (Section VI.C). The torsion the wing-rotation actuator transmits into the spar is taken as 700 N·m, a representative rather than derived figure, since no hinge-moment model exists for the rotating-wing configuration (Section VI.E).

Because the root stub is itself the pressure boundary of the loop, the wall is checked against the full combined stress state via the von Mises criterion. For a tube of outer and inner radius r_o , r_i under bending moment M_b , torque T and internal pressure P , the bending stress is $\sigma_b = M_b r/I$, the torsional shear $\tau = Tr/J$, and the Lamé pressure stresses are

$$\sigma_\theta(r) = \frac{Pr_i^2}{r_o^2 - r_i^2} \left(1 + \frac{r_o^2}{r^2} \right), \quad \sigma_r(r) = \frac{Pr_i^2}{r_o^2 - r_i^2} \left(1 - \frac{r_o^2}{r^2} \right), \quad (3)$$

plus the closed-end axial pressure stress $\sigma_{axial,P} = Pr_i^2/(r_o^2 - r_i^2)$, which adds to σ_b . At the outer surface the von Mises equivalent stress is

$$\sigma_{vm} = \sqrt{\sigma_x^2 + \sigma_\theta^2 - \sigma_x \sigma_\theta + 3\tau^2}, \quad \sigma_x = \sigma_b + \sigma_{axial,P}; \quad (4)$$

at the inner surface the equivalent stress is computed from the triaxial state with $\sigma_r = -P$.

For the selected $D_o = 71.0 \text{ mm}$, $D_i = 62.0 \text{ mm}$ tube, $I = 5.22 \times 10^{-7} \text{ m}^4$ and $J = 2I$. The outer-surface stresses are $\sigma_b = 486.3 \text{ MPa}$, $\tau = 23.8 \text{ MPa}$, $\sigma_\theta = 32.1 \text{ MPa}$ and $\sigma_{axial,P} = 16.1 \text{ MPa}$. Von Mises stress is 488.8 MPa outside and 427.7 MPa inside. A typical yield of 880 MPa divided by 1.5 gives a 20.02% margin; the assumed minimum 828 MPa basis gives 12.93%. These are unpierced-section static margins under the specified gust case. On the typical-strength basis, the torsion capacity is 5,553 N m, so the assumed 700 N m is 12.6% of that capacity. Pressure reduces the margin by about 0.19 percentage point. The 4.5 mm wall is an assumed manufacturing floor. The local airfoil depth permits this section under the stated skin and clearance allowances; the airfoil and junction remain undesigned. At the 1.50 m splice, a 0.516 m chord and 0.16 thickness ratio give 82.5 mm maximum depth, leaving 72.5 mm after the 10 mm skin/clearance allowance. The 71 mm spar fits only if that depth is available at the spar station and the splice fitting stays within the tube's envelope. The argument is a geometric accommodation constraint on an eventual airfoil, not evidence that an aerodynamic section and its junction already satisfy it. Carrying near-ambient loop gas in the bore also separates this structural task from the thermal-contraction problem that routing the cryogenic feed directly through the spar would introduce.

The neutral-axis stub-hole calculation is a separate idealized plate-hole check. At 0.10 m from the root, elliptic outboard loading gives a remaining shear fraction 0.971708; torsion and transverse shear contribute 23.80 and 7.74 MPa, with axial and hoop pressure stresses $\sigma_x = 16.06$ and $\sigma_y = 32.11 \text{ MPa}$. For the ideal traction-free circular hole, the maximum edge normal stress is

$$\sigma_{\text{edge,max}} = \sigma_x + \sigma_y + 2\sqrt{(\sigma_x - \sigma_y)^2 + 4\tau^2} = 178.35 \text{ MPa}, \quad (5)$$

using the summed shear and outer-surface pressure components, a 228.9% margin on the typical-strength allowable. On the neutral-axis faces bending stress vanishes while torsional and transverse shear remain; in the unpressurized plate limit, pure shear gives a maximum edge normal stress of four times the remote shear, so the retained pressure components are essential to this check. A near-root hole 45° off the neutral axis would instead see an approximate concentrated bending stress of 1,032 MPa, which motivates confining the roughly 1.3 mm bores to the fore and aft faces, beyond the hub seat. The thin-plate idealization omits local port pressure, curved thick-wall geometry, bosses and welds, so it is not a qualified hole margin: the 148 stubs per wing still need finite-element and fatigue assessment of curved-wall stress and weld heat-affected zones under control cycling.

Every other pressure member is checked in Section V.D, which lists each hoop stress against its allowable; the nominal hoop stresses of the brazed aluminum network, drawn trunk lines and titanium bore remain below the assumed allowables. The V-tail spar carries its own, lower aerodynamic loads and would be sized by the identical procedure once a spanwise load distribution for the V-tail exists (Section VI.E).

A concentric rotary union at the wing root passes the loop's feed in its core, straight into the spar bore, and the return in an annulus. The annulus is formed over the root stub by a hub sleeve of about 77 mm bore that is also the bearing journal: it carries the wing's bending into the cross-roller bearings, receives the two return headers through a port in the sleeve rather than through the spar, and holds 50 bar at a hoop stress of 58 MPa; the spar wall inside it sees the loop pressure on both faces. The fluid crossing the joint is near ambient, with nominal feed/return bulk temperatures of $12.9/5.1^\circ\text{C}$ against -4.5°C air; the ± 0.5 rad control motion is the fatigue case, and the return, some 8 K cooler than the feed, exchanges heat with it across the spar wall over the hub's length; that exchange is not resolved here. Concentric swivels at these pressures are standard hydraulic practice; the hub's sizing and its joint to the spar are not analyzed here (Section VI.E).

III.D. Material Selection

The titanium spar carries 50 bar hydrogen at the near-ambient loop state in its bore. The stubs are titanium, with the aluminum transitions outside the spar. Titanium is retained for its specific strength and fatigue capability in the load-bearing shaft, but those properties do not establish compatibility of the hydrogen-wetted bore or its welds: qualification must address the actual alloy condition, surface and exposure,¹⁹ and a bore barrier is a proposed fallback that would itself need continuity, durability and inspection evidence. Titanium-vessel failures have been reported in hydrogen service.¹⁹ Lee compiles a "high" embrittlement screening rating for annealed Ti-6Al-4V at 24°C and 34.5–68.9 MPa;²⁰ these data do not qualify the proposed hydrogen-wetted spar at 5 MPa.

The material trade must consider heat transport, pressure containment and the intended skin-support duty. The network uses 6061 aluminum, with an assumed as-brazed yield of 55 MPa and an allowable of about 37 MPa for brazed members; drawn, mechanically joined trunks use the T6 allowable. Brazing softens the alloy, which is why the fuselage half-hoops and headers carry thicker, as-brazed walls; every brazed member keeps an allowable-to-stress ratio of at least 1.1 (Section V.D). At equal tube geometry aluminum's $2,700\text{ kg m}^{-3}$ is lighter than titanium's $4,430\text{ kg m}^{-3}$, but the allowable determines whether that geometry can be kept. A T6-wall alternative would save about 3.7 kg of tubing only if a solution-treatment and aging route recovered the temper without damaging the completed joints, including the titanium-to-aluminum transitions; it is not the selected installation. Brazing, fatigue, permeation, hydrogen compatibility and manufacturing damage remain qualification items.

The leaf-vein branches are intended to support the airfoil skin and locally replace conventional ribs; their structural contribution is not quantified, and the mass estimate keeps its provisional skin-and-rib allowance without credit for this substitution (Section VI.E). The secondaries are bonded to the inner skin face and the return headers run along its edges. Two conditions attach to the choice. The aluminum tubes must be galvanically isolated from the carbon-fiber skin, by a glass isolation ply or a coating, which is standard practice for aluminum fittings in composite structure. And the thermal expansion mismatch, aluminum at $23 \times 10^{-6}\text{ K}^{-1}$ against carbon fiber near 2×10^{-6} in-plane, over the approximately 53 K modeled area-mean skin-temperature span from $+22.0$ to -31.2°C between sea-level noon and the tropopause, requires a compliant bond line between tube and skin; titanium at 8.6×10^{-6} provides a closer expansion match. CFRP outer skins are bonded over the metallic spar core to form the aerodynamic surface; no additional structural credit or radar-cross-section performance is established by this calculation. The CFRP layout,

resin system and the skin's in-plane conductivity that sets the strip efficiency between tubes (Section IV.B) remain assumptions pending dedicated material selection research.

III.E. Fuel Conditioning Line

The fuel line has one normal route and one final inlet-temperature control task. Along the route of Section III.A, the proposed cold plate is a serial micro-channel exchanger that gives the small once-through stream a defined outlet before injection, while the skin uses a parallel network for the circulating flow. A plate bypass must regulate its outlet toward ambient while the jacket control holds the anode setpoint. This is intended to reduce the anode's dependence on instantaneous motor heat and on which surfaces reject the load, with the loop inventory intended to buffer the tap state. Real-fluid enthalpies determine the duties.

The proposed tank-fill and pressure-build sequence defines the assumed inlet subcooling. Liquid near 20.23 K is retained as a warm-hydrogen return brings the ullage to 2 bar, about 2.58 K below saturation; the schedule then rises to 3 bar, where saturation is about 24.57 K, and the mass model reserves 0.214 kg of unusable ullage gas at an assumed 60 K mean. The pressure-build route and its mass allowance are included. At the nominal fuel flow, replacing withdrawn liquid at 2 bar requires about 2.7 g h^{-1} of pressurant, excluding the gas needed to raise pressure. The initial and final ullage inventories at the assumed 60 K imply a net gas increase averaging 4.3 g h^{-1} over 48 h. Pressurant is drawn from the loaded hydrogen; the endpoint residual is already included in the mass budget. The three spheres carry a heat-leak allowance of 1.31 W scaled with their insulation area, not a measured result; although small against propulsion power, it can still matter to the diminishing cold inventory. Condensation, stratification, pressure-control transients and pump cavitation margin decide how much subcooling survives, so the nominal margin is a boundary condition for the feed calculation, not a demonstrated tank capability.

The feed pump's 14.4 W is small on the aircraft power budget but raises the liquid to about 32.8 K. Compressing the liquid before warming avoids the work of compressing the same stream after vaporization, and at 100 bar, well above hydrogen's critical pressure, warming crosses no liquid-vapor boundary; that pressure also gives the regulator headroom into the 50 bar loop. Local heat-transfer coefficients and pressure losses inside a realizable cold plate remain to be designed.

Four thermal nodes must remain distinct: the motor winding and stator, the cryogenic plate wall, the loop-side plate and the mount's exterior. The motor interior supplies heat to the warmer loop, and the contact, spreading and insulation resistances between the nodes have not been sized, so the near-ambient mount target is not a prediction of mount-surface or winding temperature. Limiting the cold plate to warming the fuel toward ambient separates its purpose from anode-temperature regulation; avoiding a sub-ambient exposed surface would need insulation and control against condensation and frost.

At cruise the injected fuel is about 0.18% of the circulating flow and already near ambient, lowering the cold return by about 0.02 K. Startup and demand steps differ, because cold fuel can enter before the motor plate and stack coolant supply their steady duties. For the illustrative threefold step in stack demand used in Table 3, fuel flow is about 0.196 g s^{-1} . With the loop held at its cruise state and no additional cold-plate heat pickup assumed, the extra fuel draws 529 W from the loop, the bulk drop is about 1.0 K and the jacket requirement about 183 W; this is an initial imposed-step calculation, not a balanced takeoff or a validated transient. The 127 g loop charge corresponds to 32.5 minutes of nominal consumption or 10.8 minutes at that flow, consumption-equivalent times rather than usable reserves.

After the tap and throttle of Section III.A, a short counterflow jacket on the stack's internal coolant, taken from the 80 °C hot leg with a prescribed 75 °C return, keeps a positive terminal approach to the 77.5 °C fuel outlet; coolant supplied only at 75 °C could not reach that outlet passively. The 61 W duty and its conductance are component requirements, and the electric trim heater stays in series for the last leg before stack warm-up and for the full duty on the tank-direct fault route. None of these steady duties demonstrates inlet-temperature control: startup, demand steps and isolation need coupled transient verification of the stack coolant, jacket and fuel regulators.

III.F. Coating and Thermal Conductivity

Coating selection must balance the required surface properties with the low thermal resistance needed for skin heat rejection. The adopted optical inputs are solar absorptance 0.30 and emissivity 0.90 on both faces. They are assumptions, with a dark-finish absorptance alternative of 0.90. Optical and radar properties,

laminate construction and installed contact resistance require material selection; this calculation does not demonstrate low observability.

A series resistance path from the loop hydrogen to the air includes the internal film, tube wall, skin, coating and external film. A low-conductivity coating can therefore obstruct skin rejection even when the internal network performs as intended. A foam-like layer is a useful adverse example, but its conductivity is not representative of every electromagnetic-wave absorber.

Lu, Qu and co-workers²¹ report a vertically oriented boron-nitride/carbonyl-iron/silicone-rubber composite with both microwave absorption and thermal conductivity. Their maximum effective absorption bandwidth is 7.55 GHz at 1.7 mm thickness; their minimum reflection loss of -56.82 dB at 15.6 GHz occurs at 1.5 mm. They also report a maximum thermal conductivity of $4.023 \text{ W m}^{-1}\text{K}^{-1}$. These distinct reported extrema demonstrate the material research direction; they are not a jointly qualified installed coating specification, and the comparison below pairs them only for illustration. Zhan et al.²² report a 5 wt% BN/MWCNT/epoxy formulation with calculated effective X-band absorption at 3.37 mm and a measured thermal conductivity of $0.429 \text{ W m}^{-1}\text{K}^{-1}$. Neither composite is selected as the installed finish; the nominal thermal calculation omits its added resistance, and the mass allowance is for the stated primer/topcoat.

An illustrative resistance comparison is useful for separating this material question from the external film. The foam example and the two reported boron-nitride composites use the thicknesses and conductivities in Table 5. With the reference film, their effective coefficients are 14.4, 77.4 and $49.1 \text{ W m}^{-2}\text{K}^{-1}$, reductions of about 82%, 3.3% and 39% from 80. These are arithmetic consequences of t/k , not measured installed-system performance. The coupled model adopts the stated optical properties and neglects additional coating, bond and contact resistance.

Table 5: Illustrative coating-plus-reference-film resistance comparison. Here $h_{\text{eff}} = [1/h + t/k]^{-1}$ with reference $h = 80 \text{ W m}^{-2}\text{K}^{-1}$. Thickness and conductivity are the stated material examples; the calculation omits bond, contact and laminate resistances and does not specify the installed coating.

	t_{coat}	k_{coat}	$h_{\text{eff,ext}}$
Illustrative low-conductivity layer (foam-like)	2.0 mm	0.035 W/m K	$14.4 \text{ W/m}^2\text{K}$
Lu, Qu, et al. ²¹ (illustrative combination)	1.7 mm	4.023 W/m K	$77.4 \text{ W/m}^2\text{K}$
Zhan et al. ²² (5 wt% BN/MWCNT/epoxy)	3.37 mm	0.429 W/m K	$49.1 \text{ W/m}^2\text{K}$
Bare, uncoated skin (reference film)	—	—	$80.0 \text{ W/m}^2\text{K}$

The thermal model adopts an assumed in-plane skin conductance of $k_{\parallel}t_{\text{skin}} = 0.05 \text{ W K}^{-1}$, representing an enhanced heat-spreading construction between branches (Section IV.B). A lower value of 0.01 W K^{-1} is examined to assess sensitivity to skin construction.

The coating’s optical properties also enter the heat balance directly. With the skin as the rejection surface, absorbed sunlight is a load the architecture must accommodate: at the design point, $\alpha = 0.30$ adds 797 W on the networked skin, whereas the $\alpha = 0.90$ dark-finish bound adds 2,392 W. The finish is therefore a material and mission choice rather than a consequence of a favorable conductivity measurement. Section IV.E examines these alternatives while holding the other prescribed conditions. The nominal emissivity is 0.90 on each face; that study also includes an asymmetric finish, 0.3 above and 0.9 below. Lower emissivity reduces a surface’s own emission but increases the importance of reflected surroundings, so a lower-emissivity underside is not automatically lower in observed contrast.

III.G. Distributed Rejection: Thermal Contrast, Coverage and Airflow

Infrared-signature literature separates hot components, exhaust and airframe emission from reflected environmental radiation, and includes the intervening atmosphere when predicting the signal received by an observer.²³ A thermal balance supplies only part of that calculation; no seeker response, detection range, plume persistence or radar cross section is modeled here.

Two mechanisms act on the vehicle’s temperature distribution. The motor cold plate spends the fuel’s cryogenic enthalpy on a concentrated heat source; holding the mount near ambient is its sizing target, but no mount temperature or suppression of an observed emitter is demonstrated. The network spreads the remaining heat over the active skin. At nominal loiter its area-mean rise is 9.96 K and its warmest modeled

face-average rise 10.42 K; tube-base temperatures and the strip's between-tube variation are distinct, so these averages cannot establish a local peak.

Routing waste heat into wings, V-tail and body spreads it over surfaces that would otherwise be passive, but lower local temperature does not by itself imply a lower total radiated excess. For an idealized uniform surface with a small temperature rise, convection $Q_{\text{conv}} \approx hA\Delta T$ and linearized radiation $\Delta Q_{\text{rad}} \approx 4\varepsilon\sigma T^3 A\Delta T$ give $\Delta Q_{\text{rad}} \approx (4\varepsilon\sigma T^3/h)Q_{\text{conv}}$: increasing area lowers temperature at a fixed convective duty, but does not remove the energy being rejected. This limiting argument does not replace the separate sun, sky, terrain and strip balances used here.

A radiator fed directly by the assumed 75–80°C stack coolant would reject heat at a much higher temperature level than the skin, although its temperature distribution also depends on its air-side film and fin effectiveness, not on the coolant window alone. A radiator matched to MARID's heat load and flight condition has not been sized, and a separate low-temperature radiator loop could change the comparison; Section VI.B provides literature mass context and illustrative subsystem comparisons. Frey et al.⁵ model fan-fed ducted radiators for a 1 MW PEM system, including heat addition to the duct flow and the installation penalties, and show that low-drag designs are feasible. In their optimized cases, raising stack temperature from 80 to 200°C reduces frontal area by about 55%, dry core mass from roughly 8 to 3.6 kg and predicted external drag by about 36%. This explains why low-temperature PEM heat rejection deserves its own sizing analysis, but it supplies no radiator mass or drag for this much smaller aircraft, and extrapolating below their temperature range does not establish that a matched ducted radiator is infeasible.

The passive reference is the same skin and flight condition with no loop heat, in equilibrium with sun, sky, terrain and airflow. Its area mean is about 1.5 K above ambient, so the nominal network adds about 8.4 K. The radiometric significance depends on viewing background and aspect; the prescribed clear-sky effective temperature is about 214.5 K and the terrain reference 288.15 K, neither a measured scene. Seen from below, the clear sky is already much colder than either the passive or the active skin, so the loop's added temperature is only one contribution to contrast. Seen from above against the prescribed warm terrain, heating a skin that is colder than that background can move its temperature toward the background; over colder terrain the same heat addition can move it away. These comparisons do not account for spectral background radiance or reflected earthshine and skyshine.

Slow flight reduces aerodynamic recovery heating, which follows $\Delta T_{\text{rec}} = rV^2/(2c_{p,\text{air}})$. At the nominal 33.35 m s^{-1} true airspeed the recovery rise is only 0.49 K, which the thermal model includes by referencing convection to the adiabatic wall temperature.

The cathode exhaust leaves at 77.5°C, with 416 W of net sensible disposal including blower heat; the stack-only exhaust term is 384 W. Table 6 places it beside combustion-exhaust temperatures, including the engines associated with the MQ-1B²⁴ and MQ-9A,²⁵ together with a literature tailpipe range. These are engine-temperature references, not observations of matched installations at this aircraft's power, size or flight condition. Bhatt and Mahulikar²⁶ compare surface and plume emission in the table's two infrared bands.

Table 6: Exhaust and skin temperatures and ambient-subtracted blackbody-equivalent in-band radiance ratios, normalized to the cathode exhaust at the −4.5°C design ambient. Equal emitting area and emissivity are assumed; gas-band emission, path length, viewing angle and atmospheric transmission are excluded. Passive skin receives no loop heat. Exhaust-gas-temperature (EGT) limits are engine operating references, not measured installed-exhaust temperatures; the literature tailpipe range is a separate reference.

Source	Temp. °C	Above ambient K	Above a passive skin K	3–5 μm relative	8–12 μm relative
Cathode exhaust, this vehicle	78	82	80	1	1
Cathode exhaust, cooled to the dew point	51.5	56	54	0.4	0.6
Active rejection surfaces, area mean	5.5	10.0	8.4	0.03	0.08
Passive skin, area mean	−3.0	1.5	0	0.004	0.012
Gas-turbine tailpipe, literature range ²⁷	327–627	332–632	330–630	64–494	8–22
TPE331-10 turboprop, certified EGT limit ²⁸	650	654	653	549	24
Rotax 914 piston, certified EGT limit ²⁹	950	954	953	1,548	40

In the direct-bottom-view aero-engine cases of Bhatt and Mahulikar,²⁶ surface emission dominates the long-wave band while the hot plume dominates the mid-wave band; their relative contributions depend on Mach number, engine operation, altitude and surface emissivity. That finding supports keeping distributed skin and exhaust separate, without transferring a combustion-plume result to a PEM cathode stream.

Cooling exhaust to its calculated dew point, about 51.5 °C, is an optional sensitivity: using the nominal-point sensible cooling duty, it moves approximately 132 W from the exhaust to the loop and raises the mean skin temperature by about 0.3 K. Cooling below the dew point would also add condensation heat, which this sensible-only case does not include. An exhaust-lip cooler is also only a proposed component; its mass, duty, pressure loss and resulting wall temperature are unspecified and absent from the nominal inventory. Neither optional feature establishes a plume or detector-performance benefit.

The fuselage, 0.62 m in exterior diameter and 3.0 m long, provides the largest continuous rejection surface. Channels immediately inside its outer skin can use surface that the body already requires for packaging, following its shape rather than prescribing a separate external core. The cooled body footprint covers 4.03 m², 86% of the 4.67 m² wetted area: I extend the venation onto the nose and tail tapers, about 0.67 m each, leaving only the tips uncooled. The model treats it as an equal-area cylinder over $x = 0.48$ to 2.55 m, without resolving the taper's effect on hoop length, plumbing inventory, sunlight, film and flow sharing. This footprint boundary is not a transition location; natural transition is calculated separately from $Re_x = 5 \times 10^5$, near 0.279 m at the design point. Body convection averages 59.3 W m⁻²K⁻¹ over the footprint; its UA referenced to the tube-base temperature is 215.9 W K⁻¹ of the 422.6 W K⁻¹ total. The lower body film coefficient follows the longer streamwise development length; the turbulent flat-plate coefficient decreases locally as $x^{-0.2}$, so body area and lifting-surface area are not thermally interchangeable.

The coverage accounting follows the surface-exchanger approach of Kellermann et al.,¹² which deducts the areas unavailable to the exchanger rather than crediting the full wetted body. In this design the uncooled nose tip holds the sensor, air-data, intake and exhaust openings, and the uncooled tail tip holds the motor mount and attachments. A more conservative allowance, reserving half of the 0.6 m forward avionics zone, the aft 15% and a further 8% for access, gives $(1 - 0.10 - 0.15)(1 - 0.08) = 0.69$ of the wetted area, about the full-diameter section alone. These are layout allowances, not measurements from finalized CAD. Table 7 spans coverage from that 69% allowance to the whole wetted body. Higher coverage lowers skin rise and pump power, as flow shifts toward the fuselage and the governing V-tail pressure drop falls, at the expense of more tubing and greater intrusion into reserved regions; the gross mass is held fixed rather than recalculated for each coverage case. The 94% row is a sensitivity of the equal-area cylinder model, and the 100% row is a limit, not a feasible layout.

Table 7: Fuselage venation coverage as a fraction of the 4.67 m² wetted area, at the design point with gross mass fixed; hoop count, absorbed sun and tubing mass vary with the networked length

Coverage	Channeled run m	Body area m ²	Half-hoops	Body rise K	Mean rise K	Pump W	Tubing kg
69%	0.67–2.33	3.23	98	11.5	11.0	86.7	11.32
86%	0.48–2.55	4.03	122	10.4	10.0	77.0	12.70
94%	0.45–2.70	4.38	132	10.0	9.6	73.9	13.28
100%	0.35–2.75	4.67	142	9.7	9.3	71.2	13.82

Altitude and speed couple the demand side to the rejection side. At fixed mass and equivalent airspeed, climbing requires higher true airspeed at approximately unchanged aerodynamic drag, so propulsion power rises while thinner air weakens convection. At a fixed altitude, turbulent convection grows approximately as $V^{0.8}$, whereas parasite-dominated propulsion power grows approximately as V^3 . Higher and faster segments therefore require separate thermal checks; Section IV.E resolves the altitude cases under the stated aerodynamic and stack assumptions. The adopted $L/D = 27.64$ is the loiter value of the wetted-area build-up, not its maximum; Section VI.C treats its build-up and uncertainties.

The trips at 20% chord on wings and V-tail are assumed to establish the thermal boundary layer over the cooled footprint. A flat-plate comparison assigns about 2.25 N additional wing drag and 2.21 N tail drag, 6.3% together, already represented in the adopted drag build-up. At the same L/D , with no credit for drag relief, a natural-transition film raises mean skin rise to 12.8 K and pump input to 185 W; a net trip-selection

trade is not quantified. Surface heating can also alter skin friction and boundary-layer development, with different consequences in turbulent and laminar flow;¹² no wall-heating drag correction is applied to the adopted polar. These are correlation-based estimates, not validated trip aerodynamics.

IV. Thermal Model

IV.A. Power Budget

Table 8 summarizes the power budget at the cruise design point, with the lift-to-drag ratio from the wetted-area build-up of Section VI.C and a propeller efficiency $\eta_{\text{prop}} = 0.80$. The feed pump, the rejection-loop pump and the cathode blower are inside the electrical demand, so their work is paid for in fuel and, as heat, lands in the fluid they move.

Table 8: Power and heat budget at the design point: 200.4 kg, 28.73 m s⁻¹ EAS, 33.35 m s⁻¹ TAS at 3,000 m, $L/D = 27.64$. All hotel electricity is counted once: 200 W avionics, 200 W payload and 100 W actuators.

Quantity	Value	Notes
Gross mass	200.4 kg	Self-consistent, weight-spiral-converged at 3,000 m
Aerodynamic drag	71.1 N	$W/(L/D)$; unchanged with altitude at constant EAS
Thrust (propulsive) power	2,372 W	$F_D \times V_{\text{TAS}}$
Required shaft power	2,965 W	Thrust power/ η_{prop}
Motor input power (single thruster)	3,294 W	assumed $\eta_{\text{motor}} = 0.90$, with inverter
Avionics + payload + actuators	500 W	Bus-side, incl. conversion
Feed pump, tank $\rightarrow$ 100 bar	14.4 W	Real para-hydrogen pump work at 60% efficiency
Rejection-loop pump	77.0 W	One pump at the highest circuit drop plus the common path, Section V.B
Cathode blower	33 W	3 kPa channel drop at 45% efficiency, an assumption
Total electrical demand	3,919 W	
PEM chemical power input	7,837 W	$\eta_{\text{PEM}} = 0.50$, lower-heating-value (LHV) basis
H ₂ consumption (loiter)	0.0653 g/s	11.29 kg burned per 48 h; 11.50 kg loaded with 0.21 kg unusable ullage residual
Motor waste heat	329 W	incl. inverter
PEM stack waste heat	3,919 W	includes the 384 W that leaves in the cathode exhaust
Avionics, payload and actuator waste heat	500 W	400 W common avionics/payload plate; 70/30 W wing/V-tail jackets
Total component waste heat	4,748 W	before pump work, blower heat and absorbed sunlight

The direct passive-hybrid stack-battery bus assumes zero net battery power at loiter. The inverter is on the motor cold plate, and each load's conversion heat enters its cooling route and is not removed by the surviving loop if that route is isolated. Battery cycling and bus-protection and conduction losses are not separately modeled. The 200 W payload allocation is bus-side, corresponding to about 190 W delivered at an assumed 95% conversion efficiency.

At fixed 3.919 kW bus demand and stack efficiency 0.50, a 95–97% efficient stack-to-bus converter would increase fuel burn by 3.1–5.3% and combined stack and converter waste-heat generation by 242–413 W; these estimates exclude mass, auxiliary-power and thermal feedback.

IV.B. Formulation

The model simultaneously balances the fuel path, rejection loop and skin at a specified mass, flight condition and set of assumptions. Bulk loop states are lumped, surface allocation and one-dimensional manifold flows are iterated, external coefficients are integrated over the stated footprint, and each skin face has an analytical strip between a tube and mid-pitch. These spatial resolutions do not provide a full conjugate airframe temperature field. For near-linear rejection, the lumped loop temperature approximates the rejecting branches' length-mean fluid temperature; a linear branch estimate at nominal number of transfer units $NTU \approx 0.65$ places supply and return about 0.4 K above the estimates obtained by placing them symmetrically about the loop mean temperature and gives a main-exchanger requirement of about 52.7 W K^{-1} . Figure 3 illustrates the formulation basis.

Fuel line. The fuel-line enthalpies use real-fluid states: tank liquid at 20.23 K and 2 bar; pump outlet from the isentropic compression to 100 bar at 60% efficiency; cold-plate outlet at the lesser of the motor-and-inverter waste heat and the fuel's capacity to ambient,

$$Q_{\text{plate}} = \min(Q_{\text{motor}}, \dot{m}_f [h(T_{\text{amb}}, P_1) - h_{\text{pump}}]), \quad (6)$$

with the bypass valve holding the outlet at ambient; the loop pre-heat by mixing, taken from the loop's load, and the jacket duty,

$$Q_{\text{pre}} = \dot{m}_f [h(T_{\text{tap}}, P_2) - h_c], \quad Q_{\text{jacket}} = \dot{m}_f [h(T_{\text{stack}}, P_a) - h(T_{\text{tap}}, P_2)]. \quad (7)$$

Here $h_c = h(T_{\text{amb}}, P_1)$ at the nominal point, where the fuel's heat uptake to ambient limits the cold-plate duty. This enthalpy is unchanged across the makeup regulator. The tap regulator delivers the 3 bar anode supply pressure P_a ; fuel enthalpy at the anode is evaluated at this pressure, while actual anode pressure requires downstream regulation. Both throttles are isenthalpic, and the tap temperature is the loop mean plus half the calculated loop rise. The fuel's total absorption is $Q_{\text{plate}} + Q_{\text{pre}} + Q_{\text{jacket}} = \dot{m}_f [h(T_{\text{stack}}, P_a) - h_{\text{pump}}]$. Define $h_{\text{ref}} = h(T_{\text{amb}}, P_a)$ and

$$Q_{\text{fuel,net}} = \dot{m}_f (h_{\text{ref}} - h_{\text{pump}}), \quad Q_{\text{sens}} = \dot{m}_f [h(T_{\text{stack}}, P_a) - h_{\text{ref}}]. \quad (8)$$

The total absorption is $Q_{\text{fuel,net}} + Q_{\text{sens}}$: nominally, the fuel absorbs 325 W along its path, of which 79 W enters the stack as sensible enthalpy above the ambient reference, leaving a net fuel sink of $325 - 79 = 246 \text{ W}$. Because h_c is evaluated at the cold plate's higher pressure, Q_{sens} includes the ambient-temperature pressure enthalpy difference as well as $Q_{\text{pre}} + Q_{\text{jacket}}$. The main exchanger duty is

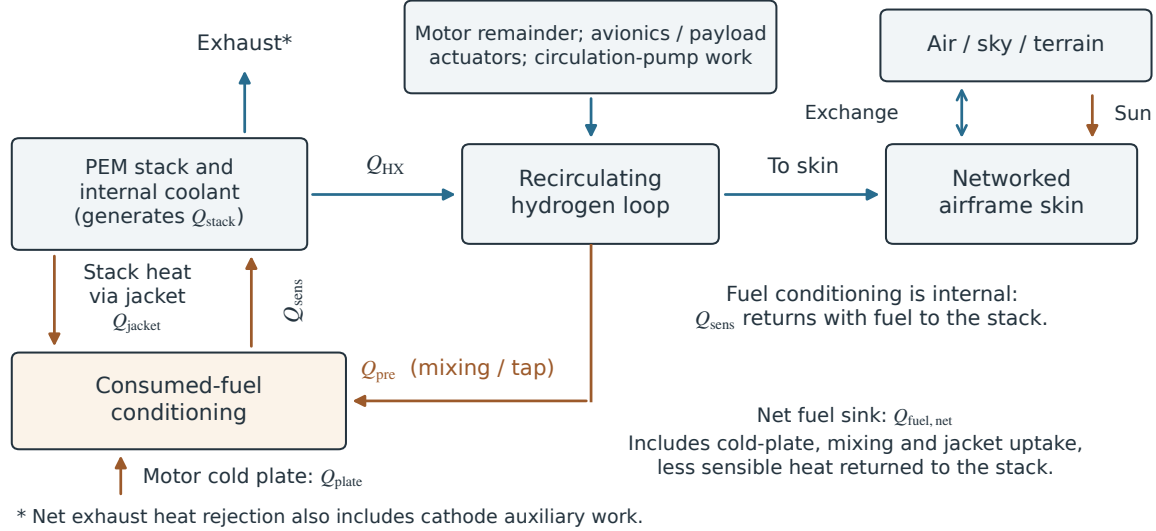
$$Q_{\text{HX}} = Q_{\text{stack}} + Q_{\text{sens}} - Q_{\text{exhaust,stack}} - Q_{\text{jacket}}. \quad (9)$$

The chemical term uses the nominal lower heating value at the standard 298.15 K reference, while sensible air, product-water and fuel terms are referred to ambient; omitting the reaction-enthalpy reference correction overstates the calculated stack heat by about 11 W at nominal loiter and 48 W at 11 km on the adopted para-hydrogen basis, conservatively for heat rejection. The fuel preheat and jacket uptake must not be counted again as external heat disposal.

At a specified consumption rate and pair of endpoint states, the fuel's total enthalpy uptake is fixed; changing the loop temperature redistributes it between mixing and the jacket, and the sensible heat carried into the anode reappears in the stack balance.

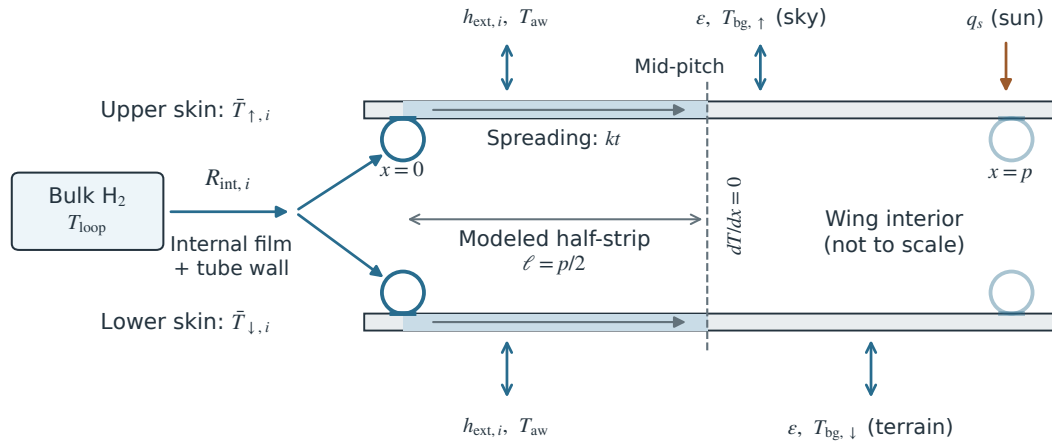
Cathode. Air at twice stoichiometric is heated from ambient to stack temperature, with product water treated as vapor on the LHV basis. Oxygen and water mass rates use molecular-mass ratios. At the nominal 3,000 m condition, an air-supply blower overcomes the assumed 3 kPa channel drop at 45% efficiency, requiring about 33 W; the additional compression pressure ratio is one, so compressor and turbine work are zero. The altitude sensitivity adds inlet-air compression and exhaust-turbine recovery when ambient pressure falls below a prescribed 42.8 kPa threshold, corresponding to the water-vapor pressure at 77.5 °C. This schedule is an assumption, not a derived membrane-hydration requirement or a specification for selected hardware. At the design point, the exhaust removes approximately 384 W from the stack and carries approximately 416 W out of the aircraft, including the blower contribution.

(a) System heat transfers



(b) Hydrogen-to-skin strip: wing section

View along the chordwise tubes; x runs spanwise between neighboring tubes.



Tube bases share one modeled $T_{b, i}$; $R_{\text{int}, i}$ aggregates all branches on surface i .

Figure 3: Physical heat transfers in the nominal architecture. (a) System heat transfers. (b) Spanwise wing section viewed along chordwise tubes, with separate tubes bonded to each skin; shading marks the half-strip to mid-pitch symmetry, and the wing interior and tubes are schematic. For the fuselage, the analogous strip runs lengthwise between half-hoops. Arrows show physical transfers; optional band and exhaust-cooling paths are omitted.

Rejection loop. The loop collects the stack heat not taken by the exhaust or the fuel jacket, with the fuel's sensible enthalpy the stack hands back, the motor remainder, the avionics, payload and actuator heat and its own pump work, less the fuel's pre-heat by mixing, and sheds it through n surfaces in parallel. The skin between two tubes is treated as a fin. On each face f of surface i a strip of skin runs from a tube, whose base is at $T_{b,i}$, to the mid-pitch line a half pitch $\ell = p/2$ away, where symmetry leaves it adiabatic. The strip loses heat to the air at T_{aw} through the external film and exchanges radiation with its background $T_{\text{bg},f}$, the sky for the upper face and the terrain for the lower; the upper face also absorbs the sun. With the radiation carried by the secant coefficient $h_r = \varepsilon\sigma(\bar{T} + T_{\text{bg}})(\bar{T}^2 + T_{\text{bg}}^2)$, which reproduces the fourth-power exchange exactly at the strip's mean temperature $\bar{T}$, the strip is the classical fin written about the effective equilibrium of this linearization, $T_e = (h_{\text{ext}}T_{\text{aw}} + h_rT_{\text{bg}} + q_s)/(h_{\text{ext}} + h_r)$, with q_s the absorbed solar flux on that face:

$$\begin{aligned} T(x) &= T_e + (T_b - T_e) \frac{\cosh m(\ell - x)}{\cosh m\ell}, & \bar{T} &= T_e + \eta(T_b - T_e), \\ \eta &= \frac{\tanh m\ell}{m\ell}, & m^2 &= \frac{h_{\text{ext}} + h_r}{kt}. \end{aligned} \quad (10)$$

where kt is the skin's in-plane conductivity–thickness product and $\bar{T}$ and h_r are iterated to agreement. Each surface i then satisfies

$$\frac{T_{\text{loop}} - T_{b,i}}{R_{\text{int},i}} = \sum_{f=\uparrow,\downarrow} A_{f,i} [h_{\text{ext},i}(\bar{T}_{f,i} - T_{\text{aw}}) + \varepsilon\sigma(\bar{T}_{f,i}^4 - T_{\text{bg},f}^4)] - \alpha G A_{\text{proj},i} + Q_{\text{band},i}, \quad (11)$$

where $Q_{\text{band},i}$ is positive for outward conduction into adjacent unnetworked skin. The baseline excludes cooling by outward conduction into adjacent unnetworked skin but retains inward heat transfer. This restriction is applied separately to each upper/lower and leading/trailing path before summing. The nominal band contribution is zero; the networked area is unchanged. $R_{\text{int},i}$ is the internal film plus wall resistance of the surface's branches, $T_{\text{aw}} = T_{\text{amb}} + rV^2/2c_p$ is the adiabatic wall temperature the moving air presents to the skin; here c_p is the specific heat of air, whereas the bulk-loop heat capacity is that of hydrogen. The turbulent recovery factor is $r = 0.89$. The model assigns half of each surface wholly to sky and half to terrain. At nominal loiter, mixed view factors change individual face means by less than 0.1 K and leave the area mean unchanged to reported precision; the simplified assignment overstates the per-face radiative exchange in Figure 4(d). G is the midday solar flux on the projected area of the networked skin. The strip solution distinguishes the tube-base temperature T_b , the mean of each face, their area-weighted mean and the mid-pitch temperature at the point farthest from the tubes. The internal heat-transfer balance uses T_b ; the reported skin rise is the overall area-weighted mean minus free-stream ambient temperature, used as a thermal-contrast metric. These modeled temperatures do not bound local airframe hot spots. The reference skin has $kt = 0.05 \text{ W K}^{-1}$, an assumed installed conductance, a conductive ply or a thick laminate, at which the strip efficiency η on the wing is 0.86; a plain 1 mm carbon skin at $10 \text{ W m}^{-1} \text{ K}^{-1}$, $kt = 0.01 \text{ W K}^{-1}$, gives 0.58. At the adopted 34 mm pitch, reducing the assumed skin conductance from $kt = 0.05$ to 0.01 W K^{-1} increases the largest modeled wing tube-to-mid-pitch temperature difference from approximately 1.9 to 8.0 K. The wing's area-mean temperature rise changes only slightly, from 9.46 to 9.37 K, while the loop supply temperature increases by approximately 4.1 K. The principal penalties in this comparison are therefore greater temperature variation across the skin and a warmer hydrogen loop (Section VI.E).

The loop mean temperature is found by requiring the surface duties to sum to the loop load. Flow is divided among surfaces in proportion to their duties. Total circulation is the largest of the flow required to limit the bulk rise to 20 K, the nominal minimum of 35.6 g s^{-1} and the flow required to keep each circuit's least-fed branch at or above $Re = 3,500$, using properties at the solved loop mean temperature. A reduced surface-duty fraction can therefore require greater total circulation to maintain its branch flow; the bulk temperature rise can fall below 20 K. At each allocation step, updated surface flows determine the mean branch Reynolds numbers, internal heat-transfer coefficients and route pressure losses. The manifold calculation updates the branch flow distribution used for the least-fed-branch criterion. These quantities determine $R_{\text{int},i}$ and pump work, which enter the next thermal and flow calculation.

The fuel-conditioning and skin heat balances are solved together with the loop flow distribution and pumping demand. Each outer iteration updates the electrical demand and fuel-conditioning duties; inner iterations update the heat and flow allocated to each rejection surface. Outer iterations run in blocks of sixteen. Within each outer iteration, surface-allocation iterations run in blocks of twelve until the relative

change in every circuit's flow is no greater than 10^{-8} . Outer convergence is assessed from successive changes in pump power and in the combined mixing and fuel-jacket duty. Each must satisfy a relative tolerance of 10^{-6} , with a 1 W lower scale. Sequential updates leave a small difference between quantities evaluated in successive iterations. The energy-ledger residual must be within 10^{-6} W after subtracting this difference, defined as the pump-power change minus the change in combined mixing and jacket duty. Both changes must also satisfy their separate convergence tests. Doubling the iteration counts and tightening the outer tolerance by a factor of 1,000 leave the reported quantities unchanged at their printed precision. This establishes numerical convergence and accounting consistency under the stated assumptions; it does not establish physical predictive accuracy or test sensitivity to the number of integration points used for the external heat-transfer coefficients.

IV.C. Properties and Transfer Coefficients

Hydrogen properties come from CoolProp,³⁰ with the para-hydrogen equation of state of Leachman et al.³¹ Loop c_p , density, viscosity and conductivity are evaluated at the solved mean state; the nominal heat capacity is $c_p = 15.125 \text{ kJ kg}^{-1} \text{ K}^{-1}$. Retaining para composition after warming is an assumption, not a demonstrated 48-hour conversion bound. A sensitivity calculation substitutes normal-hydrogen properties in the warm loop while retaining the fuel-conditioning and stack calculations, the prescribed non-pump loop load, hardware and flight conditions. Recalculating loop temperatures, circulation and pump heating changes mean loop temperature from 9.025 to 9.123 °C, bulk rise from 7.791 to 8.191 K, pump input from 76.975 to 76.897 W and area-mean skin rise from 9.964 to 9.966 K. It does not re-model conversion chemistry, cold-plate duty or mass convergence.

External convection uses the local flat-plate correlations, $Nu_x = 0.0296 Re_x^{0.8} Pr^{1/3}$ in the assumed turbulent region and $Nu_x = 0.332 Re_x^{0.5} Pr^{1/3}$ before natural transition. Air viscosity follows Sutherland's law and density ISA. Coefficients are area-averaged over 20–80% chord on the cooled wing and V-tail, and $x = 0.48\text{--}2.55 \text{ m}$ on the body. Trip efficacy, local pressure gradients and actual airfoil behavior remain unvalidated.

Internal convection uses Gnielinski³² with the corresponding friction relation above the laminar switch, and fully developed $Nu = 4.36$ below $Re = 2,300$. The $Re = 3,500$ flow criterion uses properties at the loop mean temperature; it does not establish fully turbulent flow in a transitional regime or at every temperature along a branch. Recalculating the nominal least-fed branch Reynolds number using viscosity at the loop supply temperature gives 3,855, 10.1% above the criterion. Cold-plate pseudo-critical heat transfer is a separate unresolved component-design problem.^{11,33}

The nominal effective sky is 214.545 K, a favorable clear-sky assumption, with $1,033 \text{ W m}^{-2}$ midday solar flux. Sky sensitivities at 234.545, 254.545 and ambient 268.650 K give active-skin mean rises of 10.282, 10.691 and 11.045 K, versus 9.964 K nominal, holding other inputs fixed. These four prescribed cases are not a weather distribution. Terrain is separately fixed at 288.15 K; its effective radiating temperature and atmospheric screening remain assumptions. The terrain sensitivity is about 0.34 K of skin rise per 10 K near the nominal point, not a universal derivative. The reference omits ground-reflected sunlight: effective albedos (diffuse upwelling-to-downwelling shortwave ratios at 3,000 m) of 0.1, 0.2 and 0.3 add 106, 213 and 319 W of absorbed sunlight and raise the mean rise to 10.18, 10.39 and 10.60 K, about 8.4 K above the passive reference throughout.

IV.D. Results at the Design Point

Table 9 shows how the modeled heat is divided among disposal paths at 3,000 m with the baseline coating. The generated and disposed totals agree within 0.1 W at the displayed precision, an internal accounting check rather than independent physical validation. The loop's 4,195 W is about 86% of the 4,858 W of waste heat generated, excluding absorbed sunlight; the rest leaves in the cathode exhaust and the fuel stream.

The 3,919 W PEM waste heat in Table 9 is the LHV-based chemical-conversion heat generation. The 3,553 W delivered by the stack exchanger also accounts for incoming fuel sensible enthalpy, the stack's contribution to cathode exhaust heating and the fuel-heating jacket: the rounded balance is $3,919 + 79 - 384 - 61 = 3,553 \text{ W}$. The assumed component sequence is listed in Table 10.

Active skin disposes of about 88.3% of the daytime total. Loop mean temperature is 9.025 °C and pump input 76.97 W, about 2.0% of electrical demand. The mean skin rise is 9.96 K; the highest face average is 10.42 K and the highest nominal tube-base rise 11.45 K. Table 11 summarizes the per-surface thermal state.

Table 9: Energy ledger at 3,000 m, 200.4 kg, $\alpha = 0.30$, midday, $\varepsilon = 0.90$. Net cathode exhaust includes the stack and blower terms; independently rounded components need not sum in the last digit.

Generated	W	Disposed	W
PEM stack waste heat	3,919	Cathode exhaust, net sensible	416
Motor waste heat	329	Fuel stream, net sink (pump outlet $\rightarrow$ ambient)	246
Avionics, payload and actuator waste heat	500	Skin convection	4,461
Rejection-loop pump	77	Skin net radiation	532
Cathode blower	33		
Absorbed sunlight, networked skin	797		
Total	5,655	Total	5,655

Table 10: Estimated nominal bulk-fluid temperatures along the assumed component sequence. The actuator row reports the mixed return temperature after heat pickup in the parallel wing and V-tail jackets. Prescribed coolant endpoints of 80 and 75 °C imply a main-exchanger capacity rate near 711 W K⁻¹. The -16 W makeup entry represents the consumed fuel's total heat uptake between injection and the warm tap, not a local temperature drop at the mixing junction. Station temperatures, including the jacket estimates obtained with finite c_p , are approximations, not an installation or control design.

Station	Heat added W	Loop temperature °C
Cold return from the surfaces	—	+5.1
Four actuator jackets, on wing and V-tail returns	100	+5.3
Return block, makeup mixed in	-16	+5.3
Loop pump	77	+5.4
Motor mount, loop-side plate	81	+5.6
Avionics plate and payload	400	+6.3
Stack exchanger, coolant 80 to 75 °C	3,553	+12.9 (tap)
Total, 35.60 g s⁻¹ at 15.1 kJ kg⁻¹ K⁻¹	4,195	7.8 K rise

Table 11: Per-surface state at the design point. Areas are both faces of each surface; UA is the surface's convection divided by the rise of its tube base above the adiabatic wall temperature, a base-referenced ratio rather than a differential conductance; duty is the heat the loop delivers to the surface; sun is on the projected networked area, upper face only; skin rise is the area-weighted mean of the two face means above the -4.5 °C ambient (Section IV.B).

Surface	Area m ²	h_{ext} W m ⁻² K ⁻¹	UA W K ⁻¹	Duty W	Convection W	Radiation W	Sun W	Skin rise K
Wings	1.77	81.8	127.2	1,156	1,297	133	274	9.46
V-tail	1.06	86.4	79.5	752	799	79	126	9.22
Fuselage	4.03	59.3	215.9	2,287	2,365	320	398	10.38
Total	6.86		422.6	4,195	4,461	532	797	9.96 mean

Three heat paths carry the architectural argument: the consumed fuel, whose 325 W uptake exceeds its 246 W net sink (Section III.A); the exhaust, which removes heat without using a rejection surface; and the network, which distributes the remaining load over the available active skin. Parallel supply lets each surface receive a flow allocation associated with its duty instead of forcing the full circulating flow through every

surface in series. At the nominal point, the surface means are within about 1.2K of one another; this is a result of the assumed films, geometry and allocation, not a uniformity guarantee for arbitrary hardware. Absorbed sunlight is the second-largest individual term in the daytime heat ledger after stack waste heat, which explains why the coating and the environment matter alongside the internal plumbing.

IV.E. Altitude, Coating and Aerodynamic Efficiency

The following studies examine altitude (Table 12), individual thermal assumptions (Table 13), PEM efficiency (Table 14), actuator demand (Table 15) and lift-to-drag ratio (Tables 16–18), the last as an imposed parameter; Figure 4 summarizes the altitude, absorptance and lift-to-drag results. Converged off-design gross-mass estimates do not establish installed tank fit or a flight envelope.

Table 12: Altitude sweep at constant equivalent airspeed ($\alpha = 0.30$, midday; 200.4 kg held for the thermal columns, weight spiral re-converged in the last two). Fuel includes the cathode blower and, where ambient pressure is below the cathode’s water-vapor pressure, the compressor. Gray: design point.

Altitude	T_{amb}	TAS	Fuel	Loop		Loop						Fuel,		
m	$^{\circ}\text{C}$	m/s	g/s	load	UA	mean	Wing	V-tail	Body	Mean	Pump	48 h	Gross	
				W	W/K	$^{\circ}\text{C}$	K	K	K	K	W	kg	kg	
0	15.0	28.7	0.0576	3,831	469.7	25.2	6.5	6.3	7.3	7.0	84	9.8	197.0	
2,000	2.0	31.7	0.0625	4,064	438.9	14.3	8.4	8.2	9.3	8.9	79	10.8	199.2	
3,000	−4.5	33.3	0.0653	4,195	422.6	9.0	9.5	9.2	10.4	10.0	77	11.3	200.4	
5,000	−17.5	37.1	0.0716	4,493	389.4	−1.3	11.9	11.6	12.9	12.4	73	12.5	203.2	
8,000	−37.0	43.9	0.0836	5,174	339.2	−15.2	16.8	16.5	18.0	17.4	66	15.0	208.7	
11,000	−56.5	52.7	0.1004	6,314	290.6	−25.9	24.4	24.1	26.0	25.3	62	18.6	216.6	

At constant equivalent airspeed, true airspeed scales as $\rho^{-1/2}$, so the 11,000 m case places about 1.51 times the design loop load against 0.69 times the base-referenced convective conductance; the larger skin rise is not solely a change in the ambient reference. It penalizes the low-thermal-contrast objective but does not by itself establish an altitude limit: for missions without that objective, a larger rise may be acceptable, provided component temperatures remain within their limits and installed heat-rejection capacity is sufficient.

Waste-heat rejection raises skin temperature, but it does not establish anti-icing protection. The nominal wing mean is about $+5.0^{\circ}\text{C}$; at 5,000 and 8,000 m it is approximately $−5.6$ and $−20.2^{\circ}\text{C}$. The geometric leading edge, unnetworked chord strips and dry outer panels are not protected by the modeled network. Water collection, phase change and icing are absent from the balance.

In Table 13, changing absorptance from 0.30 to 0.90 raises nominal area-mean skin rise by about 3.2 K, and a natural-transition film at the tripped L/D raises it to 12.8 K and pump power to 185 W.

Table 13: Single-parameter thermal cases at 3,000 m and 200.4 kg. Surface max.: largest two-face area-mean rise, not a local peak.

Case	Sun	Loop load	Loop mean	Mean rise	Surface max.	Pump
	W	W	$^{\circ}\text{C}$	K	K	W
$\alpha = 0.30$, midday (baseline)	797	4,195	9.0	10.0	10.4	77
$\alpha = 0.50$, midday	1,329	4,199	10.1	11.0	11.4	79
$\alpha = 0.90$, midday (dark bound)	2,392	4,207	12.3	13.2	13.6	83
Night	0	4,190	7.4	8.4	8.8	74
ε 0.3 above / 0.9 below	797	4,199	10.0	10.9	11.3	79
$\eta_{\text{PEM}} = 0.49$ (baseline 0.50)	797	4,344	9.4	10.3	10.7	77
Untripped film, tripped L/D retained	797	4,396	11.3	12.8	12.8	185
Exhaust cooled to its dew point, +132 W	797	4,327	9.4	10.2	10.7	77
Skin conductance between tubes 0.01 W K $^{-1}$, plain carbon laminate (assumed)	797	4,195	13.1	10.0	10.5	77

These comparisons separate three design questions. Absorptance changes the solar load that must leave the skin; the boundary-layer state changes its access to the external airflow; and in-plane conductance changes how effectively the skin between tubes participates, so a similar area mean can conceal a much hotter tube base and cooler mid-pitch region (Section IV.B). A coating or a conductive ply must therefore be assessed for both the heat balance and the spatial temperature variation, with its installed mass and contact resistance.

Table 14 examines the assumed PEM efficiency; 0.55 and 0.60 are prescribed alternatives, not validated operating bounds.

Table 14: Stack chemical efficiency, lower-heating-value basis, at the held 200.4 kg design point. Fuel is the 48 h integral of each steady flow, without mass reconvergence. Surface max. as in Table 13.

η_{PEM}	Chemical power W	Stack waste heat W	Fuel g/s	Fuel, 48 h kg	Loop load W	Mean rise K	Surface max. K
0.50	7,837	3,919	0.0653	11.29	4,195	10.0	10.4
0.55	7,115	3,202	0.0593	10.25	3,536	8.6	9.0
0.60	6,515	2,606	0.0543	9.38	2,988	7.5	7.8

MARID -- steady-state rejection model (200.4 kg, 28.7 m/s EAS, para-H₂ properties)

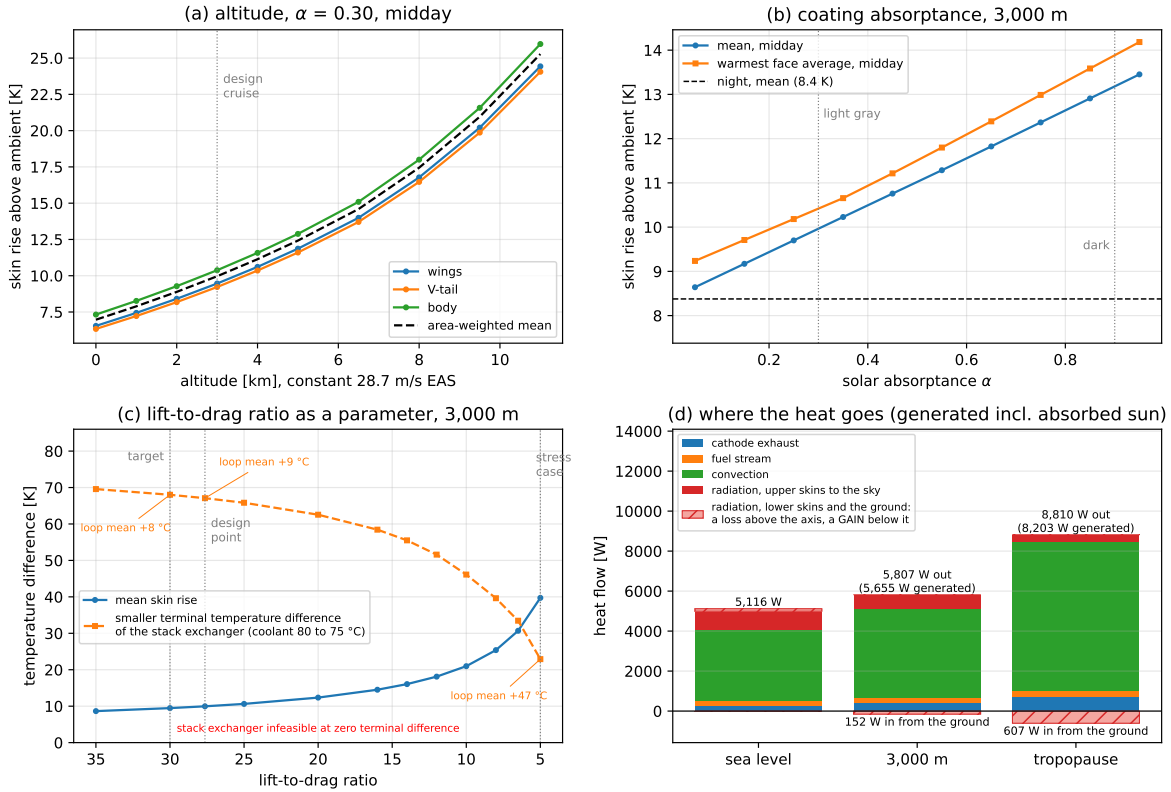


Figure 4: Steady-state rejection model results. (a) Skin rise per surface and area-weighted mean against altitude at constant equivalent airspeed. (b) Mean and warmest face-average skin rise against solar absorptance at the design altitude, with the night value. (c) Mean skin rise and the stack exchanger's smaller terminal temperature difference, which must stay positive, against lift-to-drag ratio; the loop temperature is annotated at three points. (d) The disposal ledger at sea level, the design point and the tropopause, with radiation drawn by face.

Table 15 varies actuator electrical allocation and captured heat together; it is not a heat-only redistribution. Table 16 gives the imposed aerodynamic-efficiency cases. The primary comparison target is 30; 35 is a more ambitious sensitivity. Both quantify potential thermal benefits at an imposed L/D ; no airframe that achieves them is designed here.

Table 15: Total actuator electricity and captured heat as an imposed parameter (400 W avionics/payload held; four jackets; heat split 70/30 wing/tail). Nominal: 100 W; no mechanical-work split or duty-cycle model. Surface max. as in Table 13.

Captured W	Loop load W	Loop mean °C	Mean rise K	Surface max. K	Pump W
0	4,010	8.5	9.6	10.0	76.7
100	4,195	9.0	10.0	10.4	77.0
180	4,344	9.4	10.3	10.7	77.2
200	4,381	9.6	10.3	10.8	77.2

Table 16: Imposed lift-to-drag ratio at 3,000 m, $\alpha = 0.30$, midday. Thermal columns hold 200.4 kg; gross mass reconverges with structure and design-point loop-pump power fixed. $L/D = 5$ is an extreme thermal-load case conditional on sufficient component capacity, establishing neither a flight condition nor a 48-hour gross mass. Gray: design point; blue: comparison target.

L/D	Electrical demand W	Fuel g/s	Loop load W	Loop mean °C	Body rise K	Mean rise K	Gross mass kg
35.0	3,217	0.0536	3,536	7.1	9.0	8.6	195.3
30.0	3,656	0.0609	3,949	8.3	9.9	9.5	198.5
27.6	3,919	0.0653	4,195	9.0	10.4	10.0	200.4
20.0	5,194	0.0866	5,393	12.4	12.9	12.4	210.0
14.0	7,172	0.1195	7,251	17.7	16.8	16.1	226.3
10.0	9,809	0.1635	9,728	24.8	22.0	21.0	251.2
5.0	19,534	0.3256	19,303	47.1	41.1	39.7	—

Table 17: Endurance on the nominal fuel load (11.50 kg loaded, 11.29 kg burnable) at 28.73 ms^{-1} EAS, 3,000 m and constant L/D ; no mission reserve. Gray: design point; blue: comparison target.

L/D	Endurance, mass held h	Endurance, burn-off h	Mean skin rise K
35.0	58.5	59.9	8.6
30.0	51.4	52.7	9.5
27.6	48.0	49.2	10.0
20.0	36.2	37.1	12.4
14.0	26.2	26.9	16.1
10.0	19.2	19.7	21.0
5.0	9.6	9.9	39.7

At the design point, the model gives 48.0 h with gross mass held and 49.2 h with burn-off (Table 17); burning throughout at the initial gross mass therefore overestimates the fuel needed for this idealized steady loiter.

Table 18 asks a different question of the same fuel budget: the speed at which the burnable fuel lasts 48 h at each imposed L/D . Neither table constitutes a speed-dependent aerodynamic polar.

Table 18: Cruise speed for 48 h on the design fuel load, with L/D held constant with speed as in Section V.F (3,000 m). C_L is on the 4.69 m² reference area; rows with C_L above the assumed $C_{L,\max} = 1.4$ are infeasible. Gray: design point; blue: comparison target.

L/D	True airspeed m s^{-1}	Equivalent airspeed m s^{-1}	Mean skin rise K	Loop pump W	C_L , wing area
35.0	42.2	36.4	8.7	75.5	0.52
30.0	36.2	31.2	9.5	76.5	0.70
27.6	33.3	28.7	10.0	77.0	0.83
20.0	24.1	20.8	12.3	79.1	1.58
14.0	16.9	14.5	16.0	83.0	3.24
10.0	12.0	10.4	21.1	90.0	6.38
5.0	5.9	5.1	37.9	135.7	26.2

In the speed calculation, the higher allowable speed increases the external film while aerodynamic recovery heating also increases. The thermal benefit of aerodynamic efficiency is a reason to pursue aerodynamic validation, not a prediction that a particular modification will achieve the target ratio.

IV.F. Internal Heat-Transfer Sensitivity

Table 19 multiplies the internal heat-transfer coefficient predicted by the Gnielinski correlation by factors from 0.44 to 2.48 at fixed nominal aircraft geometry, bores, mass and flight condition. The laminar coefficient and friction law are unchanged; properties, flow allocation, pump work and total circulation are recalculated together under the flow-control rule. The $\pm 40\%$ pair and wider $-56\%/+148\%$ range are prescribed variations motivated by supercritical-hydrogen literature,¹¹ not validated error bounds for these warm branches. A multiplier of 1.0 reproduces the nominal solution; tighter numerical tolerances leave the displayed changes unchanged.

Table 19: Internal-film sensitivity at the nominal design point. Temperature changes are relative to a multiplier of 1.0, the nominal solution of Section IV.D. Required main-exchanger UA assumes the prescribed 80-to-75 °C coolant endpoints; it is not an installed capacity.

Film multiplier	Loop mean ΔT , K	Overall skin $\Delta \bar{T}$, K	Wings $\Delta \bar{T}$, K	V-tail $\Delta \bar{T}$, K	Fuselage $\Delta \bar{T}$, K	Required UA W K^{-1}
0.44 (−56%)	+3.13	+0.061	−0.404	−0.544	+0.423	54.9
0.60 (−40%)	+1.64	+0.032	−0.213	−0.286	+0.223	53.6
1.00 (nominal)	0.00	0.000	0.000	0.000	0.000	52.3
1.40 (+40%)	−0.70	−0.014	+0.092	+0.124	−0.096	51.8
2.48 (+148%)	−1.47	−0.029	+0.192	+0.260	−0.202	51.2

The hydrogen loop temperature responds more than the overall skin mean. Reduced internal transfer shifts duty toward the fuselage, which warms while the wings and V-tail cool. Total circulation remains 35.60 g s^{−1}; reduced V-tail allocation lowers pump input even as required exchanger conductance increases. Across the tested range, pump input changes by -2.7 to $+1.3$ W from nominal. At a multiplier of 0.44, the wing’s least-fed branch Reynolds number, evaluated at the loop mean temperature, falls from 3,891 to 3,661. A single bulk hydrogen temperature and one mean internal heat-transfer coefficient per surface do not resolve local wall temperatures in the least-fed branches.

IV.G. Combined Parameter Sensitivity

Table 13 examines thermal alternatives individually. A deterministic sensitivity study (Table 20) combines four of those alternatives with prescribed auxiliary allowances in the same steady model: skin conductance

0.01 W K⁻¹, wings and V-tail untripped, solar absorptance 0.9 and stack efficiency 0.49, in all sixteen combinations; an auxiliary set of three adverse allowances, 50 W of unmodeled balance-of-plant draw deposited as loop heat, a 5 kPa loss through the stack exchanger and the motor's plate in place of the 2.0 kPa allowed, with the avionics-plate allowance unchanged at 0.5 kPa, and a pump efficiency of 0.40 in place of 0.60; their combination; and a 100 W balance-of-plant draw, applied alone to the baseline and within that combination. Geometry, gross mass, altitude, airspeed and lift-to-drag ratio are held at the design point; electrical demand, fuel flow and loop state are solved together with the minimum circulation required by the flow-control rule. Untripped cases thus keep the tripped L/D of 27.64, with no credit for drag relief, which retains higher propulsion power and heat input; they are fixed- L/D film sensitivities, not thermal bounds. The cases are prescribed scenarios, not samples from a distribution, and the auxiliary levels are screening allowances, not estimates of hardware. Table 20 gives representative rows of the twenty-six.

Table 20: Representative cases from 26 prescribed combinations at fixed design geometry, mass and flight condition. Trunk bores are held; manifold flow distributions and the circulation needed to meet the least-fed-branch Reynolds criterion, using loop-mean properties, are recalculated for each case. Required main UA uses 80-to-75 °C coolant endpoints.

Case	Loop load W	Flow g s ⁻¹	Pump W	Loop mean °C	Mean rise K	Required UA W K ⁻¹
Design point	4,195	35.6	77	9.0	10.0	52.3
Skin conductance 0.01 W K ⁻¹ alone	4,195	35.6	77	13.1	10.0	55.8
Untripped alone	4,396	50.4	185	11.3	12.8	55.6
All four thermal alternatives	4,607	51.8	217	20.6	17.3	68.2
All three auxiliary allowances	4,479	35.6	180	9.8	10.5	55.2
Thermal + auxiliary, 50 W balance of plant	5,297	51.3	532	23.5	19.0	78.9
Thermal + auxiliary, 100 W balance of plant	5,391	51.3	532	23.9	19.3	80.4

All 26 cases close their energy balances numerically. Within this fixed- L/D study, the Reynolds criterion at the loop mean temperature sets total circulation in ten cases, each with untripped surfaces and the V-tail least-fed branch governing. Evaluating the least-fed branch Reynolds number instead with viscosity at the loop supply temperature gives a minimum of 3,472, below the criterion; no claim of turbulence everywhere follows.

The combined 50 W auxiliary case gives the peak pump input, 532.3 W; the 100 W case gives the peak loop duty, 5.391 kW, required main UA , 80.41 W K⁻¹, and mean skin rise, 19.27 K. Across the set, the highest face-average and tube-base rises are 22.10 and 26.34 K. Minimum main terminal approach remains 52.64 K; the jacket's hot-end approach is 2.5 K in every case, fixed by the prescribed 80 °C coolant supply and 77.5 °C anode inlet, with required jacket conductance spanning 3.240–3.647 W K⁻¹. These are requirements at prescribed coolant endpoints, not fixed hardware capacities.

When all four thermal alternatives are applied together, the increases exceed the sum of the increases from applying each alternative separately by 52.14 W of loop duty, 25.30 W pump power, 1.490 K loop mean and 3.820 W K⁻¹ main conductance. Fixed-mass 48-hour burned fuel spans 11.29–13.17 kg; no aircraft or tank is reconverged for these combined cases.

The combined cases expose the cost of enforcing the flow criterion through fixed passages. When the untripped lifting surfaces shed less heat, their duty-proportional share of circulation falls, and the V-tail's least-fed branch can set the total flow. Moving more hydrogen through unchanged trunks and component passages increases pressure loss and pump work; that work returns as heat and feeds back into the stack's electrical demand. This explains why applying the alternatives together does not reproduce the sum of their separate temperature increases. The nominal circulator mass allowance does not establish hardware capacity for the 532 W pump input required in these prescribed cases under the duty-proportional split. That requirement needs a new capacity and mass assessment and is not an upper bound. Some of the adverse inputs, including installed skin conductance and trip effectiveness, are unresolved hardware properties; this study does not establish that their combination is improbable.

V. Loop Hydraulics and Mass

V.A. Pressure Drop and Pump Power

Darcy–Weisbach losses use the current properties and branch flows. The calculation includes the model’s laminar and turbulent friction treatments, minor losses and finite headers. The one-dimensional manifold solve includes complete branch losses and conserves surface flow. It is not a cross-link CFD model. Header friction and branch losses are included, but momentum-induced junction pressure changes are omitted. At nominal loiter, the body’s 30 mm headers have a velocity head of about 90 Pa against an 84 Pa mean branch loss; body flow sharing, Reynolds-limited circulation and pump input therefore remain conditional on junction design. At nominal loiter the U-connected wing and V-tail branch-flow ratios span approximately 0.99–1.02 of the circuit mean; the Z-connected body spans 0.94–1.12. Those distributions are recomputed for each off-design case.

One circulator supplies the common path and the largest parallel-route head:

$$P_{\text{pump}} = \frac{\dot{m}}{\rho\eta_p} \left(\Delta p_{\text{common}} + \max_i \Delta p_i \right). \quad (12)$$

Each route head Δp_i is a lumped estimate (mean branch flow, full-flow header friction and minor-loss coefficients of 3, 6 and 2 for each branch, supply header and trunk line); the distributed manifold solve sets the branch allocation, not this head. Nominal V-tail head governs at 2.457 kPa; common head is 2.950 kPa, comprising approximately 0.450 kPa line loss and 2.500 kPa component allowance at nominal flow. This gives 76.97 W input using mean-loop density; using the approximately 1.3% higher cold-leg inlet density in the volume-flow factor alone, at unchanged calculated head and efficiency, lowers the estimate by about 1 W. On the same mean-density basis, summing individually balanced routes gives a lower reference of about 63.9 W; the difference is the additional pumping required when the routes share one pressure head and balancing valves throttle the lower-loss routes.

Under the flow rule of Section IV.B, the loop temperature rise can vary with duty below the 20 K ceiling; a narrowly fixed temperature rise would instead demand proportionally more flow as the load increased. For a fixed passage at approximately fixed properties, pressure loss grows roughly as flow squared and pump power roughly as flow cubed. The friction factor and hydrogen density modify that scaling in the actual calculation. The choice of control rule therefore matters to both electrical demand and heat rejection.

Circuit flow can be measured even though individual branch Reynolds numbers cannot be measured directly by the proposed three circuit sensors. Inferring the least-fed branch requires the manifold model and appropriate instrumentation and control margins; it is not a demonstrated flight controller. At the nominal state, the pump moves about 8.54 L s^{-1} , or 512 L min^{-1} , of 50 bar hydrogen against a relatively small head. Low electrical power alone does not establish that a hermetic machine of the assigned mass is available.

V.B. The Common Path and the Central Hardware

Outside the parallel surfaces the circulating stream follows the series path of Figure 1, then divides into five physical lines, one per wing, one per V-tail half and one for the fuselage. Supply and return isolation at each line lets a leaking surface be removed from the circulating pressure boundary, which requires separate return paths: sharing the fuselage network’s return with a wing would prevent the modeled fuselage isolation from leaving the wing route intact.

The aggregate common supply/return route is 4 m of 50 mm-ID drawn aluminum tube with a 1.5 mm wall, 53 mm OD and a lumped minor-loss coefficient of 10. Nominal velocity is 4.35 m s^{-1} and Reynolds number approximately 105,185. This is a routing allowance, not a layout. The trunk bores (Section V.D) are sized at the design point and kept fixed off-design; the body headers are 35 mm OD.

The unbuilt stack exchanger and motor plate together carry a 2.0 kPa passage allowance, and the common avionics/payload plate another 0.5 kPa, each on the whole circulating flow. Every connected actuator jacket adds its own 0.5 kPa nominal allowance on its wing or V-tail return. Jackets on parallel routes are not summed as though the full flow passed through all four. The nominal component pressure-loss allowances scale off-design as $\Delta p/\Delta p_0 = (\dot{m}/\dot{m}_0)^2(\rho_0/\rho)$, using the reference flow appropriate to each path. This fixed-passage relation does not scale the assigned hardware masses. Placing actuator heat on the returns adds it after surface rejection and before the next circuit pass; it does not create a separate external heat sink.

The distribution and return blocks are a drilled-bar estimate with a 50 mm gallery, five staggered ports and a combined internal volume of 0.62 L; bosses, closures and mounting remain provisional. Their mass is itemized with the shut-offs, balancing valves, sensors and jackets in Section V.E, separately from the tubing’s 30% fittings and cross-link allowance; none is a selected hardware package.

Coupled sensitivities give 198.8, 200.4 and 202.0 kg gross for 2, 4 and 6 m aggregate common path; corresponding mean skin rises are 9.91, 9.96 and 10.01 K. Changing the ten-shutoff allowance from 1.0 to 1.5 kg gives 200.1–200.7 kg gross. These are mass and routing sensitivities under unchanged inventory assumptions.

Flow distribution is checked separately from the total circuit drop. Supply-header pressure decreases along the outgoing flow, while the return-header pressure also varies with the accumulated return flow; each branch consequently sees a different driving pressure, so the headers are evaluated as manifolds (Section III.B). The nominal U arrangement keeps both wing and V-tail connections at the root; the body’s Z arrangement places supply and return at opposite ends. The calculation uses representative equal branch lengths within each surface and does not resolve actual taper, blocked cross-links or a coupled spanwise skin field.

Each spanwise station feeds a forward and an aft half-branch, so the flow available to each secondary is smaller than for a single channel crossing the entire cooled chord. Increasing bore reduces resistance at a fixed flow but also reduces branch Reynolds number. With the least-fed-branch Reynolds criterion enforced using properties at the mean loop temperature, a larger bore can therefore raise total circulation enough to increase the pump demand of the common hardware. In Table 21, for example, the 1.5 mm wing option requires 111 W total pump input against 77 W at 1.25 mm, while a 1.0 mm wing raises branch loss instead. The V-tail carries less flow per branch; its selected bore is the smaller 1.0 mm. The comparisons explain the selected pair within the sampled geometry and control assumptions.

Table 21: Branch-bore sensitivity at fixed nominal mass and flight condition. One surface’s bore varies at a time; other geometry and the 35.6 gs^{-1} minimum are held. Pump includes common-path and maximum parallel-route head. Reynolds numbers use properties at the solved loop mean temperature and are circuit-mean branch values, so the least-fed branch is lower (3,891 for the nominal wing); allocation and manifold distributions are recomputed for each case.

Surface	Bore (mm)	Re	Velocity (m/s)	Δp (kPa)	Circuit pump (W)	Loop pump (W)	Surface rise (K)
Wing	2.0	3,675	3.81	2.5	14.7	319.7	9.8
Wing	1.5	3,552	4.90	1.6	6.9	111.0	9.3
Wing	1.25	3,917	6.48	1.8	7.1	77.0	9.5
Wing	1.0	5,102	10.54	3.4	13.8	90.1	9.8
V-tail	2.0	4,013	4.20	4.8	24.6	703.0	10.5
V-tail	1.5	3,657	5.04	2.5	8.7	207.9	9.1
V-tail	1.25	3,573	5.91	2.0	5.6	104.7	8.9
V-tail	1.0	4,063	8.40	2.5	6.3	77.0	9.2

V.C. Topology Trade: Serial and Parallel Networks

I compare a dense serial layout (four chains at 15 mm pitch) and two wider-pitch, sixteen-chain alternatives with the selected parallel network in Table 22. A continuous serial passage can simplify fabrication by avoiding repeated branch-to-header connections and cross-links, and every parcel follows its prescribed path, avoiding a within-chain flow-allocation problem; multiple chains still require distribution between them. These advantages suit the small once-through fuel-conditioning and cold-plate path (Section III.E).

For the distributed rejection duty, short parallel branches carry portions of the much larger circulating flow and reduce hydraulic resistance relative to the dense layout’s long, narrow serial chains, at the cost of greater joining, inspection and flow-distribution demands. This duty-specific trade motivates serial fuel conditioning and parallel skin cooling without establishing a universal topology ranking or a reliability advantage from connection counts, whose boundaries differ: the serial counts are chain ends only, while the parallel network’s shop-made connections, counted in Section VI.D, depend on the illustrated construction. These are not matched installed connection inventories.

Table 22: Wing-network screening at the design wing duty. Serial rows are separate energy-balanced calculations at 350 bar between the loop supply and return temperatures (1.5 mm-bore titanium tube, 0.30 mm wall; pump input returned to the hydrogen; bend losses adapted from the Crane method³⁴). The dense serial row holds 1,156 W of passage rejection; the spanwise rows target 1,149 W of net cooling, defined as passage rejection less pump input. The parallel row subtracts only its allocated wing-route pump share, so the pumping boundaries differ. Coverage is nominal area over both faces of both panels (8.09 m²), with placement on the taper unresolved. Serial tubing counts passes and bends only; parallel tubing includes return headers but not feed-spar metal. Skin heat transfer over the added area and the installed pressure system are not evaluated.

Network	Coverage	Routing	Pump	Net heat	Tubing	Connections
Dense serial, 15 mm pitch	not mapped	4 chains, 87 bends each	3.6 kW	−2.5 kW	0.81 kg	8 chain ends
Serial, 102 mm pitch	60%	16 chains, 3 spanwise passes each	81 W	1,149 W	0.40 kg	32 chain ends
Serial, 136 mm pitch	60%	16 chains, 2 spanwise passes each	57 W	1,149 W	0.29 kg	32 chain ends
Parallel leaf-vein, 34 mm pitch	22%	296 branches	7.1 W	1,149 W	0.86 kg	1,180 shop-made

V.D. Pressure Members

Table 23 gives nominal inner-wall Lamé hoop stresses at each member’s operating pressure; its ratio columns are allowable divided by hoop stress, not percentage margin, and titanium uses the typical-strength basis. The governing brazed member is the body header, at a ratio near 1.12, and the governing drawn member the body trunk, near 2.0. The spar’s combined root check is separate from its pressure-only ratio. Static hoop checks do not cover handling damage, defects, fatigue, hydrogen qualification or unpressurized buckling. Maximum loop pressure and the governing standard’s burst factor remain undefined; a sealed nominal charge at fixed volume would reach about 56–61 bar at 40–70 °C.

Table 23: Hoop stress of the pressure members (brazed 6061 aluminum, walls sized to the as-brazed allowable of 37 MPa, with the T6 allowable of 184 MPa alongside; the drawn trunk lines and the makeup line, joined by mechanical fittings, at the T6 allowable; Ti-6Al-4V, allowable 587 MPa, walls set by bending and by the bearing seat)

Member	Pressure bar	Bore mm	Wall mm	Hoop stress MPa	Allow./stress, T6 ×	Allow./stress, as-brazed ×
Wing secondaries (Al)	50	1.25	0.30	13	13.7	2.7
V-tail secondaries (Al)	50	1.0	0.30	11	16.1	3.2
Wing return headers (Al)	50	13.0	1.10	32	5.7	1.1
V-tail return headers (Al)	50	10.0	0.90	30	6.0	1.2
Fuselage secondaries (Al)	50	5.0	0.45	30	6.0	1.2
Fuselage headers (Al)	50	30.0	2.50	33	5.6	1.1
Common line, drawn (Al)	50	50.0	1.50	86	2.1	—
Wing trunk lines, drawn (Al)	50	16.5	1.00	44	4.2	—
V-tail trunk lines, drawn (Al)	50	13.7	1.00	37	5.0	—
Fuselage trunk lines, drawn (Al)	50	35.4	1.00	91	2.0	—
Wing spar bore, the feed (Ti)	50	62.0	4.50	37	15.8	—
V-tail spar bore, assumed (Ti)	50	15.0	4.50	11	51.4	—
Hub sleeve, return annulus (Ti)	50	77.8	3.50	58	10.1	—
Makeup line, fuselage (Al)	100	4.0	0.60	39	4.7	—

V.E. Mass of the Thermal Plumbing

Table 24 itemizes the thermal-plumbing mass, which enters the weight spiral of Section V.F as fixed mass. The 6061 aluminum tubing follows the geometry of Table 4 at the walls of Table 23. Spar metal is already counted as airframe mass; the 30.6 L loop volume includes the spar bores and hub annuli. These preliminary estimates do not separately itemize the avionics/payload plate, loop-side motor cooling, fuel jacket, trim heater, tank-direct valves or insulated cryogenic feed line; coverage by existing allowances remains unresolved.

The circulating hydrogen charge is part of this fixed mass and is not consumed in an ideal sealed steady

state. Fuel loaded for endurance comprises the burnable fuel and unusable ullage residual; purge, leakage, startup duty and mission reserve have not been independently budgeted.

The mass distribution also identifies where installation choices matter. The fuselage half-hoops and headers contribute 5.32 kg, while the common path adds 2.62 kg. By comparison, the wing and V-tail secondaries and return headers together contribute 1.29 kg. Assessing the architecture's mass therefore requires attention to the larger-bore fuselage network and the distribution plumbing, not only the fine wing and V-tail venation. The final routing and component selection will determine how closely an installed system can approach this estimate.

Table 24: Thermal-plumbing mass carried in the gross-mass budget. Rows are rounded independently, so the column may differ from the total in the last digit.

Item	Mass	Basis
Wing secondaries and return headers	0.86 kg	$296 \times 177 \text{ mm} \times 1.25 \text{ mm}$; four 13 mm headers at 1.10 mm; spar-bore feed
V-tail secondaries and return headers	0.43 kg	$232 \times 135 \text{ mm} \times 1.0 \text{ mm}$; four 10 mm headers at 0.90 mm; spar-bore feed
Fuselage half-hoops and headers	5.32 kg	$122 \times 974 \text{ mm} \times 5 \text{ mm}$, 0.45 mm wall; two 30 mm headers, 2.5 mm wall
Stubs, collectors, fittings, cross-links	1.99 kg	30% of the network tubing
Trunk lines, blocks to roots	1.47 kg	16.5 mm to each wing, 13.7 mm to each V-tail half, 35.4 mm to the fuselage; nominal velocities near 5 m s^{-1} ; layout-bounded lengths, placement not yet fixed
Common path, block to block	2.62 kg	4 m of 50 mm drawn T6 tube, 1.5 mm wall, supply and return together; Section V.B
Distribution and return blocks	1.6 kg	provisional, 0.8 kg each from a drilled-bar geometry
Balancing valves and flow sensors	0.4 kg	provisional, two valves and three sensors
Line shut-offs, ten	1.25 kg	provisional, 0.125 kg each, supply and return on the five surface lines
Loop pump and standby pump	0.8 kg	allowances, 0.4 kg each
Motor cold plate	0.3 kg	allowance
Stack coolant loop and exchanger	0.8 kg	allowance, including the $\approx 52.35 \text{ W K}^{-1}$ required core-to-loop conductance
Tank pressure-build line	0.2 kg	allowance
Regulators, valves, injector	0.6 kg	allowance
Hub sleeves, four	1.15 kg	allowance, scaled to the 78 mm annulus, net of replaced root fittings
Actuator jackets, four	0.6 kg	allowance
Cathode blower	0.3 kg	allowance
Hydrogen charge in the loop	0.13 kg	30.6 L at 50 bar, 9°C
Total	20.82 kg	

V.F. Weight-Spiral Mass Convergence

More fuel requires more storage volume and tank mass; the extra aircraft mass increases the power and fuel needed for the prescribed loiter, which requires another storage update. A fuel estimate at an arbitrarily fixed gross mass would miss that feedback. The weight spiral therefore iterates fuel burn plus unusable residual, vessel mass and gross mass until the gross mass used to calculate power agrees with the sum of component, fuel and tank masses.

The fully coupled calculation also updates speed, drag and wing sizing. It converges at 200.4 kg gross, comprising 171.0 kg fixed mass, 11.50 kg loaded hydrogen (including 11.29 kg burned over the mission) and 17.89 kg of dry tanks.

I selected three equal vacuum-jacketed spherical tanks, each about 512 mm across, so the fuselage headers pass beside them. Each inner vessel uses 5 bar design pressure with safety factor 2 and the 0.5 mm manufacturing floor where governing. Both shells are Ti-5Al-2.5Sn ($4,410 \text{ kg m}^{-3}$ and 828 MPa yield, with an assumed 110 GPa modulus for buckling), separated by a 15 mm vacuum gap holding multilayer insulation at 1.33 kg m^{-2} . Ti-5Al-2.5Sn ELI has been investigated for cryogenic hydrogen tanks;³⁵ the present shell estimate does not qualify a particular grade, weld process or warm-up exposure. The tank volume adds 7.2% ullage to the fuel volume at 71.0 kg m^{-3} , and a fittings factor of 1.25 on shell and insulation mass covers fill and vent tubing, valves, supports and the vacuum port. Outer jackets are checked against external pressure with knockdown 0.20 and safety factor 1.5. The conservative sensitivity uses 10 bar and factor-5 inner-vessel

assumptions, not the selected design. The combined dry tank mass is 17.89 kg and the loaded-fuel gravimetric index 39.1%.

The tank-mass calibration uses a measured 12 L, 0.85 kg hydrogen titanium vessel.^{36,37} Other cited comparisons are model-to-model or geometry-derived estimates,^{38–40} not measurements of these tanks. Each selected vessel holds about 3.83 kg, an extrapolation from that measured vessel. Total heat leak, mounting, plumbing and internal clearances remain assumptions. A $\pm 30\%$ imposed tank-mass factor moves the design-point mass to approximately 194.5–206.5 kg; at 60 m s^{-1} TAS it spans 210.9–232.4 kg. The imposed factor tests extrapolation from the smaller measured vessel; it is not a probability bound, a sizing-uncertainty estimate or an installed-fit check. The wider gross-mass spread at 60 m s^{-1} also illustrates why a tank-mass error matters more as the fuel demand increases. Surface area influences insulation and shell mass, but pressure sizing, buckling and manufacturing floors can change the governing wall; a universal two-thirds-power dry-mass law is not imposed on these tank calculations.

In this model, the mass-runaway indicator is the speed at which the fixed-point mass iteration ceases to meet its numerical convergence criterion, with up to 5,000 iterations and fixed efficiency and mission duration. For LH_2 in the three selected vacuum-jacketed spherical tanks, it is 386.3 m s^{-1} TAS at 48 h. The nominal 33.35 m s^{-1} TAS is 8.6% of that value, and 60 m s^{-1} is 15.5%. This indicator is not an attainable airspeed or flight-envelope limit. The speed sweep in Table 25 holds $L/D = 27.64$ instead of a validated speed-dependent polar, and at the sampled speeds above 40 m s^{-1} the larger tanks no longer fit beside the body headers, even though the mass iteration converges.

Table 25: Converged gross-mass estimates versus true airspeed, with the selected three vacuum-jacketed spherical tanks, fixed $L/D = 27.64$ and 48-hour burn plus ullage residual. Structure and flight assumptions are held at the design point. Convergence is not a tank-installation or aerodynamic-feasibility check.

$V \text{ (m/s)}$	Converged MTOW (kg)	Loaded LH_2 (kg)	Tank gravimetric index
25	194.4	8.83	37.8%
30	198.0	10.41	38.6%
33.35	200.4	11.50	39.1%
40	205.4	13.74	40.0%
45	209.2	15.50	40.6%
50	213.1	17.32	41.1%
55	217.2	19.20	41.6%
60	221.3	21.16	42.1%

To examine the mass implications of storing the fuel as compressed gaseous hydrogen (GH_2) at 350 bar instead of cryogenic LH_2 , the calculation replaces the LH_2 tank-mass model with the referenced relation of 13.6 kg of pressure vessel per kilogram of usable fuel.⁴¹ The other mass terms remain, so heavier storage also increases the fuel required by the mass feedback. The substitution gives a 48-hour critical speed of 46.6 m s^{-1} and approximately 683 kg gross, including 34.4 kg of burned fuel and a 477 kg vessel sized on the loaded fuel, about 35.1 kg with the retained residual. At 36 h it gives about 408 kg versus 193.3 kg for liquid storage. This is a storage substitution within the same model, not a complete GH_2 aircraft redesign; retained LH_2 residual and feed-pump terms limit its interpretation. Table 26 gives the critical speeds across the sampled mission durations.

The low, reference and high unsized inventory assumptions give coupled gross masses of 167.4, 200.4 and 250.1 kg, respectively. Corresponding mean skin rises are 9.02, 9.96 and 11.49 K. The high endpoint fails the assumed-minimum spar-strength screen (margin -3.66%), and its 559 mm tanks leave no room for the body headers. It is not a feasible high-mass aircraft. The reference inventory is not a target and the 33.3 kg payload/growth allowance is not demonstrated useful payload.

Table 26: Mass-runaway true airspeed versus mission duration: the numerical non-convergence threshold, with up to 5,000 iterations, for the selected LH₂ tanks and the stated GH₂ vessel substitution. None of these critical speeds is a flight-envelope prediction.

Mission duration	GH ₂ , 350 bar	LH ₂
48 h (design endurance)	46.6 m/s	386.3 m/s
24 h	93.2 m/s	772.6 m/s
18 h	124.3 m/s	1,030.2 m/s
12 h	186.4 m/s	1,545 m/s
8 h	279.6 m/s	2,318 m/s

VI. Discussion

VI.A. Fault Behavior and Redundancy

The normal route passes through the loop; a proposed tank-direct route and trim heater bypass a failed feed path, including a blocked makeup or tap regulator.

The distinction is between supplying the consumed stream and moving the much larger recirculating flow. The feed pump admits makeup to the high-pressure loop; the circulator moves the loop's charge repeatedly through the exchanger and surfaces. Losing the first function may admit a different fuel-delivery route, but losing the second removes the principal stack-coolant heat path. A bypass that delivers fuel therefore cannot substitute for a standby circulator, and a standby circulator cannot repair a failed fuel-delivery path. Table 27 states the modeled requirements.

Table 27: Fuel-route requirements at nominal loiter. Fault rows are conditional calculations, not demonstrated continued full-power operation.

Route	Path	Electric heat	Consequence
(a) Normal	tank → feed pump → cold plate → loop → tap → jacket → anode	0 W cruise trim heat; startup unresolved	325 W total fuel uptake, 246 W net reference sink; illustrative initial step drop of 1.0 K from the cruise loop state
(b) Feed-pump fault	tank-direct trim heater boiling and warming the stored liquid; reduced anode pressure after losses	371 W, 8.7% of fault demand; fuel increase 9.2%	44.0 h on nominal fuel; motor-and-inverter 329 W to loop; mean rise increases 1.3 K; component capability unverified
(c) Loop-pump fault	proposed alternative fuel path; delivery and conditioning unresolved	No trim-heater power included in this estimate	Initial-rate estimate from 77.5 to 90 °C: 42.2–73.9 s, not a transient survival allowance

Sectional isolation and pump redundancy address different failures. The intended response to a breached line is to isolate it at both ends (Section V.B) and preserve a circulation path through the others; makeup could then replenish lost charge only after the leak is controlled. Table 28 tests the thermal consequence of removing complete surface routes; it does not model the pressure excursion, isolation sequence or blocked-secondary response.

Table 28: Surviving-loop steady calculations at fixed loiter conditions and propulsion/hotel allocations, with parasitic electrical demand recomputed and isolated surface routes removed. Actuator heat not removed by the surviving loop: 35/70 W for one/two wings, 15/30 W for one/two V-tail halves, 100 W fuselage-only; none with the fuselage isolated. Surface max.: largest surviving-surface area mean. Margin: loop feed below the assumed 75 °C coolant endpoint. No row demonstrates fault flight or installed component capacity.

Isolated	Area m ²	UA W K ⁻¹	Loop mean °C	Mean rise K	Surface max. K	Pump W	Margin K
None (design)	6.86	422.6	9.0	10.0	10.4	77	62
One wing	5.98	358.4	10.5	11.4	11.7	90	61
Both wings	5.09	294.5	12.8	13.4	13.7	111	58
One V-tail half	6.33	382.4	10.0	10.8	11.2	85	61
Both V-tail halves	5.80	342.3	11.1	11.9	12.2	82	60
Wings and V-tail (fuselage only)	4.03	214.9	17.2	17.5	17.5	98	54
Fuselage (wings and V-tail only)	2.83	204.6	21.4	19.5	19.7	210	49

VI.B. Mass Comparison

A thermal inventory comparison against the components a conventional installation would carry is presented in Table 29. No net saving is claimed, because no conventional baseline has been sized at this scale.

Table 29: Thermal-management functions and their mass. Conventional entries are estimates at other scales or an equal-volume fluid illustration. Architecture masses are from Table 24; rows are rounded independently.

Function	Conventional installation (illustrative)	This architecture
Fuel pre-heating to the stack inlet	Pre-heater exchanger, 0.5–2 kg (estimate)	Motor cold plate, 0.3 kg; loop mixing and the jacket do the rest
Heat rejection to air	Ram-air or ducted radiator with fan and duct; dry core alone about 8 kg for a 1 MW stack at 80 °C (Frey et al. ⁵)	Skin network: tubing, headers, trunk lines and common path, 12.70 kg
Heat-transport fluid	Water–glycol or dielectric coolant at 0.85–1.10 kg L ⁻¹ , 26–34 kg to fill the same 30.6 L	Hydrogen charge, 0.13 kg
Distribution and control	Pump, plumbing and valves, not sized here	Blocks, balancing valves, sensors and ten shut-offs, 3.25 kg; loop and standby pumps, 0.8 kg
Other thermal allowances	—	Stack-coolant exchanger, regulators, pressure build, hub sleeves, actuator jackets, blower: 3.65 kg
Total	Not sized at this scale	20.82 kg , carried inside the 200.4 kg gross mass

VI.C. Span, Aerodynamic Efficiency and the Thermal Case

The rejection network is sized for heat transfer and the wing for aerodynamic force, so the two need not cover the same planform. Depending on mission requirements, I extend an unnetworked outer panel to improve aerodynamic efficiency while avoiding added pumping pressure drop and any extension of the hydrogen pressure boundary. The added span and area lower the induced drag and the loiter speed, reducing the propulsion power and with it the waste heat the fixed network must reject: in the span comparison below, lengthening the dry panels from an 8 to a 10 m span raises L/D from 23.8 to 27.6 and lowers the loop load from 5,023 to 4,195 W. The trade-off is added dry mass, which that comparison includes, together with structural, aeroelastic and flight-control challenges that warrant their own investigation.

The cooled inboard planform remains 0.795 m^2 per wing over a 1.35 m span, while the dry panels extend beyond it. In this preliminary model the panel tapers from a 0.649 m root chord to 0.529 m at the venation boundary and 0.25 m at the tip. I intend the panel to be swept back. Within the thermal and propulsion scope of this paper, I have not investigated the aerodynamic, thermal or aeroelastic effects of sweep; the drag build-up treats the panel as unswept.

Table 30 gives the wetted-area drag build-up. Lift and drag coefficients, wing loading and the assumed maximum lift coefficient all use the same 4.69 m^2 gross projected reference area, which includes the 1.0 m spacing between panel roots. Local drag of the root-joint region between the fuselage and each panel is not separately resolved. MID assumes excrescence 1.20, natural transition on the dry panels at 0.45 chord, span efficiency 0.84 and $C_{L,\max} = 1.4$; the LOW/HIGH bands vary those to 1.10/1.35, 0.60/0.30, 0.88/0.80 and 1.6/1.2, respectively, as prescribed alternatives, not confidence limits. The 20% chord trip is assumed on the networked lifting surfaces. Loiter is 1.3 times stall speed, 28.73 m s^{-1} EAS, where the operational $L/D = 27.64$ is a reproducible conceptual build-up, not a measured or CFD-validated polar.

The discrete-gust sizing relation is

$$n = 1 + \frac{K_g \rho_0 U_{de} V_e a}{2(W/S)}, \quad K_g = \frac{0.88 \mu_g}{5.3 + \mu_g}, \quad \mu_g = \frac{2(W/S)}{\rho \bar{c} g a}, \quad a = \frac{2\pi AR}{AR + 2}. \quad (13)$$

Here U_{de} is the derived gust velocity, V_e the equivalent airspeed, $\bar{c}$ the mean chord, a the lift-curve slope and AR the aspect ratio. The selected wing loading is 419 N m^{-2} , $\mu_g = 34.9$ and $K_g = 0.764$. Gust velocities 15.24 m s^{-1} at loiter and 7.62 at $1.4V_e$ give factors 3.81 and 2.97; the former exceeds both the assumed maneuver factor of 2.5 and the 2010 § 23.337 normal-category benchmark of 3.8. This is a mission sizing use of the rule's form,⁴² not a certification envelope. No separate certification-speed spar sizing is established.

The gust calculation uses density at altitude in the mass ratio and sea-level density with equivalent airspeed in the load term. The linear gust response is not clipped at the assumed maximum lift coefficient; this is a stated sizing convention, not a resolved unsteady wing response. At each span, gross mass, lift-to-drag ratio, loiter speed, load factor, spar section, fuel and tank mass are converged together, and the thermal model is evaluated at that converged point with the rejection geometry held fixed. Table 31 summarizes these converged cases.

Table 30: Wetted-area drag build-up at selected 10 m , $3,000 \text{ m}$, 200.4 kg and 33.35 m s^{-1} TAS, adapted from Raymer,⁴³ with mixed laminar–turbulent skin friction at the stated transition, component form factors (Raymer's Mach multiplier omitted, raising the lifting-surface values at this design point) and an excrescence and interference factor of 1.20. C_{D0} is evaluated at loiter.

Component	Length m	Re	Transition	c_f	FF	S_{wet} m^2	Drag area m^2
Cooled sections, t/c 0.16	0.589	1.05×10^6	trip, 0.20	0.00379	1.258	3.31	0.0158
Dry panels, t/c 0.12	0.390	0.70×10^6	0.45 (0.30–0.60)	0.00333	1.261	5.06	0.0212
Fuselage	3.0	5.4×10^6	0.28 m	0.00302	1.542	4.67	0.0218
V-tail, t/c 0.14	0.45	0.81×10^6	trip, 0.20	0.00403	1.318	2.01	0.0107
Sum $\times 1.20$							0.0834
C_{D0} on 4.69 m^2							0.0178
Induced, $C_L = 0.83$, $e = 0.84$, $AR = 21.3$							0.0122
C_D ; L/D							0.0300; 27.6

Table 31: MID span comparison at 3,000 m and 48 h with three vacuum-jacketed spherical tanks and the cooled network fixed. The 10 m row is selected (gray); its L/D range is the LOW–HIGH drag band. Wing masses include sized spars and structural allowances; the outer-panel rise is passive.

Span m	S_{ref} m^2	AR	L/D	Loiter m/s	n	Spar mm	Wing kg	Gross kg	Loop W	Mean rise K	Outer panel K	48 h burn kg
8	3.89	16.5	23.8	31.5	3.55	60.2/51.2	10.8	199.5	5,023	11.0	2.2	13.8
9	4.29	18.9	25.8	30.0	3.69	65.0/56.0	12.3	199.2	4,538	10.4	2.3	12.3
10	4.69	21.3	27.6 (23.8–30.5)	28.7	3.81	71.0/62.0	13.9	200.4	4,195	10.0	2.3	11.3
11	5.10	23.7	29.3	27.7	3.91	74.2/65.2	15.5	201.8	3,927	9.7	2.3	10.5
12	5.51	26.1	30.8	26.8	4.00	76.6/66.8	17.7	205.1	3,746	9.5	2.3	9.9
13	5.92	28.5	32.3	26.2	4.07	77.3/65.9	20.6	210.1	3,627	9.3	2.3	9.5

The competing trends in Table 31 explain why better aerodynamic efficiency is not automatically a lighter aircraft. Fuel demand and tank mass fall as span and L/D increase, while the outer panels and loaded spars grow. Across the converged 8–11 m cases with MID drag assumptions, these trends substantially offset one another; they do not establish a unique optimum, and the tank-fit checks exclude some cases from a feasible comparison.

The selected 10 m MID case is not the lightest sampled aircraft. The 9 m case is lighter by 1.20 kg in MID, 0.75 kg in LOW and 0.76 kg in HIGH. Every sampled row sizes its titanium to a margin of 15–17% except the selected 10 m MID row, which retains the 71.0/62.0 mm section at 20.0%. I retain 10 m for logistics and its lower mean skin rise and heat load than 8–9 m. With three spheres every sampled tank clears the 620 mm exterior. With the fuselage network’s two 35 mm headers beside them, the HIGH-band 8–10 m tanks exceed that section and the 8 m MID and 11 m HIGH tanks leave less than 5 mm; these converged masses do not represent feasible installed designs.

The titanium sections of the 12 and 13 m MID alternatives encounter the geometric diameter cap, requiring 4.9 and 5.7 mm walls rather than the 4.5 mm minimum. At the 1.50 m splice, the selected 10 m MID spar fits the conditional depth envelope of Section III.C; the 11–13 m MID and 10 m HIGH spars exceed theirs. The passive outer-panel midday rise remains about 2.3 K, neglecting conduction across the inboard boundary.

A first-order beam estimate at the selected 10 m sizes the composite outer box within the dry airfoil depth, about 1.9 kg per side, and gives 0.19–0.22 m tip deflection at 1 g and 0.73–0.85 m at the design gust. Deflections of that size need a nonlinear aeroelastic model; outer-panel torsion, the splice and the titanium-to-composite transition are not designed.

The selected 10 m span places the vehicle near the size class of the fuel-burning Vanilla aircraft family. NASA reported a 36-foot (≈ 11 m) span and a 121-hour, 24-minute flight of the VA001 in 2017;⁴⁴ the later FAI duration record for Vanilla is 192 hours and 50 minutes, more than eight days, in 2021.⁴⁵ These are measured flight achievements of another aircraft, whereas MARID’s 48 hours is a calculated design condition. The comparison positions the proposed span and endurance requirement; it does not establish propulsion equivalence, comparable payload, or a demonstrated endurance advantage for the hydrogen architecture.

VI.D. Manufacturing Considerations

The proposed rejection network design entails repeated channels between headers. The venation consists of 74 half-length secondary channels per wing face at a 1.25 mm bore. It also includes 122 half-hoops at 5 mm bore in the fuselage inner periphery. Manufacturing analogues include compact, low-cost aluminum micro-channel heat exchangers used in air conditioning.⁴⁶ In this wing design, the channels could be formed in chordwise extruded strips along the skin, fed from stubs on the spar and connected to the headers at 20% and 80% of the chord. Transverse cross-links, header slots and dissimilar-metal transitions would need their own forming and joining operations. As for the fuselage, longitudinal headers are where the bent tubes (body half-hoops) meet. These candidate manufacturing routes do not demonstrate a leak-tight or fatigue-resistant hydrogen panel.

If the aluminum network’s expansion mismatch or joining requirements prove decisive, electron-beam melting of titanium⁴⁷ and diffusion-bonded etched plates,⁴⁸ including the investigated etching of titanium channels,⁴⁹ become possible alternatives. However, that would require a thorough reassessment of mass, pressure-wall and heat-transfer assumptions. Notably, the spar itself is a pressure boundary. Each wing’s

spar carries 148 neutral-axis stub holes. That alone would require its own joint design, thorough structural inspection and a manufacturing-feasibility assessment. The joints must also be compatible with hydrogen service, not only with structural loading. Moreover, hub sleeves and the transition to the composite extended dry spar add local details that go beyond the unpierced tube calculation.

The proposed rejection network provisionally counts about 2,350 shop-made connections across the wings, V-tail and fuselage, separate from interfaces connected at installation. While I retain the parallel venation because it provides high hydraulic conductance, it is clear that it requires many pressure-boundary joints, and inspection, flow maldistribution and leak detection remain valid engineering concerns.

VI.E. Limitations and Future Work

VI.E.1. Limitations

In this paper, I present a conceptual hydrogen propulsion and heat-management architecture for a UAV and examine steady operating points and prescribed sensitivities. The results depend on assumed stack efficiency and warm-loop para-hydrogen properties; conversion kinetics are not modeled. I prescribe the external heat-transfer correlations and the sky, terrain and solar conditions. Component and prototype testing would be needed to assess these efficiencies and correlations. The prescribed environmental conditions represent selected operating cases rather than the full range the aircraft may encounter. The V-tail geometry and fuselage shape are provisional, so the calculated endurance and thermal loads remain conditional on them. The drag build-up remains unvalidated.

For the skin calculation, I use an analytical strip model with an assumed in-plane conductance, and I couple each channel to the skin over its full modeled length. I have not established a construction that achieves that conductance within the assumed skin mass, and I have not yet independently resolved three-dimensional conduction, interior risers, contact and bond resistance or the finish layer. The manifold model does not resolve junction momentum, cross-link redistribution or blocked branches, and surface-average temperatures do not bound local hot spots.

Exchanger duties, pump demands and component masses are requirements or allowances; they do not establish installed capacity. The steady and imposed-step cases do not establish startup, power-demand, diurnal or fault-recovery behavior; these require dynamic analysis. The 42.2–73.9 s loop-loss estimate uses an assumed thermal capacity of 12–21 kJ K⁻¹, a 12.5 K temperature margin and a fixed initial imbalance of 3,553 W; it is not an allowable response time. The proposed fuel bypass does not demonstrate continued full-power operation. On the electrical side, I have not designed the direct stack–battery bus for voltage compatibility, charge control or fault isolation; adding power conversion would change the fuel and heat balances, as estimated in Section IV.A.

The mass estimate includes source-informed component allowances. Tank accommodation is addressed only as an envelope screen; mounts, access, plumbing and operating heat leak need an installed design. Spar and outer-panel sizing is first-order: local joints, hinge moments, asymmetric loads, fatigue and aeroelastic response require further assessment, as do hydrogen compatibility and the applicable pressure-design requirements; a qualified liner or different spar material could change the structural and mass estimates. I have not established a mass benefit from the proposed skin support provided by the channels. The circulating hydrogen charge does not bound a leak that continues receiving hydrogen from the tank; detection, isolation and release behavior require a system safety assessment.

These calculations establish neither a complete flight envelope nor detector or radar performance. The infrared comparison is a thermal proxy, and no matched conventional installation has been sized to establish net cooling-system mass or drag savings.

VI.E.2. Future Work

A sound first step would be to build and test a representative bonded wing panel and manifold to measure heat transfer, pressure loss, flow sharing and construction mass, supported by a three-dimensional conjugate model. A coupled transient model of the system, initially based on the four modeled lifting-surface circuits and the equal-area body representation, should then address the stack coolant, fuel conditioning, tank pressure, feed-pump inlet condition and rejection loop, including startup and fault responses, informed by published PEM coolant-control studies.⁵⁰ CFD of the finalized whole-aircraft geometry, followed by wind-tunnel or flight measurements, should assess the drag build-up, the effectiveness of the boundary-layer trips and the external heat-transfer inputs to the thermal model. Selecting the actual stack, circulator, exchangers

and electrical architecture would support an installed mass estimate and tank-layout assessment. Structural analysis of the skin support provided by the channels, together with local structural and hydrogen-service qualification, should accompany this work. Finally, a coupled comparison with the serial-chain alternative should include complete hardware mass at equal net cooling duty and under comparable operating conditions and design constraints before either construction is preferred on manufacturing grounds.

VII. Conclusion

The proposed LH₂–PEM architecture connects two requirements that are often treated separately: warming the consumed cryogenic fuel and disposing of the much larger PEM waste-heat load. The fuel flow is too small to absorb all of that heat, so its useful enthalpy lift is combined with a 50 bar recirculating hydrogen loop for distributed rejection, while retaining the stack’s internal coolant. The wing-spar bore supplies the inboard leaf-vein network, edge headers and root collectors return its flow, and the V-tail and body provide additional parallel rejection surfaces. The small consumed stream enters by makeup and leaves through a tap and coolant-heated jacket. Dry outer panels supply aerodynamic span without extending the active hydrogen network. For the selected 10 m design, with the reference (MID) drag assumptions and reference inventory, the main results are:

1. Fuel takes approximately 248 W from the motor cold plate, 16 W from loop mixing and 61 W from the jacket. Total uptake is 325 W; the net sink against ambient enthalpy at the anode supply pressure is 246 W. At this steady point the cold plate, mixing and jacket condition the fuel without dedicated cruise electric preheat, and the trim heater is a startup and fault provision. These duties establish the normal-route energy balance, not the motor-mount temperature or transient control performance.
2. Nominal loop duty is 4.195 kW over 6.86 m², with 35.6 g s^{−1} circulation, a 7.79 K bulk hydrogen temperature difference between warm supply and cold return, and 76.97 W pump input including the common route. Area-mean active-skin rise above ambient is 9.96 K and the warmest face-average rise is 10.42 K; neither is a local maximum. The mean active skin is about 8.4 K above the corresponding passive equilibrium. That comparison and the small spread among nominal face averages explain the thermal motivation for distributing rejection rather than concentrating it in a hot coolant-fed core. No matched conventional installation has been sized to establish a mass, drag or infrared advantage.
3. The sensitivity calculations identify aerodynamic efficiency, external convection, solar absorption and skin conductance as inputs the hardware assessment must include, and the internal network and flow-control rule also affect loop temperature, heat sharing and pump input. Exchanger conductances, adverse-case pump inputs and combined-case duties are requirements at prescribed points, not proven installed capacities or a demonstrated operating envelope.
4. Coupled mass convergence gives 200.4 kg gross, with 11.50 kg hydrogen loaded, including 0.21 kg unusable ullage residual, and three vacuum-jacketed spherical tanks totaling 17.89 kg dry; the titanium spar’s unpierced section passes the nominal static screen (Section III.C). The high inventory endpoint fails the assumed-minimum strength screen and the header-fit screen.
5. Loss of feed supply and loss of circulation are distinct faults: a proposed tank-direct route targets the first, while the second removes the principal stack-coolant sink and motivates standby pumping (Section VI.A).

The selected span illustrates the coupling behind these results: additional dry span reduces the waste heat delivered to a fixed active area while adding structural mass. The 9 m aircraft is lighter in the sampled bands; 10 m is selected for logistics and its lower skin rise.

These calculations support reproducible conceptual heat-path and mass accounting under the stated assumptions. Section VI.E sets out the validation and installed-design work needed to assess the feasibility of this architecture.

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This paper is part of my single-author work on the MARID architecture, which spans computer-aided design, thermal, structural and aerodynamic analysis, and guidance, navigation and control. In preparing this paper, I used artificial-intelligence assistants Claude (Anthropic) and Codex (OpenAI) to draft and edit text, to write the Python analysis, figure-generation and consistency-check scripts, and to support literature searches. I conceived the MARID architecture and its leaf-vein hydrogen circuit, made the design choices, selected the modeling assumptions, directed the analyses, and critically evaluated and interpreted the results. I also checked the references, the consistency and accuracy of the reported numbers, and the mathematics against a separate derivation record, and I take full responsibility for the content of this paper.

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