Conceptual Design of an Air-Breathing Electric Propulsion System

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Abstract: Following an analysis of the requirements for an air-breathing electric propulsion mission in low earth orbit and a review of existing concepts, a particle collection intake is designed based on long aspect ratio inlet ducts. A mathematical model is derived which demonstrates the existence of an optimum aspect ratio and identifies intrinsic theoretical limits in terms of compression ratio and collection efficiency. Numerical simulations are performed to validate the model and conduct a preliminary design assessment.

Nomenclature

A	cross-section area of single inlet duct
A_c	total collection area
A_t	thruster inlet cross-section area
c_d	drag coefficient
I_{sp}	specific impulse
L	length of single inlet duct
n_b	number density of incident beam
n_c	number density inside collector
T_b	temperature of incident beam
T_c	collector temperature
V_b	relative speed of incident beam (=orbital speed)
$\bar{v}_c$	thermal speed inside collector
η_c	collection efficiency
ρ_b	mass density of incident beam
ρ_c	mass density inside collector
τ_b	inlet transmission probability for incident beam
τ_c	inlet transmission probability for collected particles
τ_t	thruster transmission probability
τ_t^*	effective thruster transmission probability ($\tau_t A_t / A_c$)

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I. Introduction

THE genesis of the air-breathing electric propulsion (EP) idea, a potentially enabling drag-compensation technology for long duration missions in very low earth orbit (<250 km), can be traced back to the beginnings of the space age. Accordingly, most of the early concepts reflected the then prevailing view that nuclear power generators would rapidly find mainstream use in space. This was in particular the case of the pioneering work of Demetriades who promoted the PROFAC concept[1], a spacecraft that would have been propelled by a MHD thruster in the multi-MW range with the goal to collect, liquefy and store atmospheric gases in orbit. Other very high power concepts have been contemplated[2] in the 1960s and 1970s as well as more recently, such as very large Hall thrusters in the 100 kW-1MW power range and operational altitude below 160 km.[3,4] Contemporary studies have mostly focused, however, on low to medium power solar-electric designs[5,6].

Starting from 2004, JAXA has had a sustained activity geared towards the development of the so-called ABIE concept in which an annular passive intake hosts the payload within its inner diameter and conducts air to an ion engine located at the rear[7]. This concept has been refined over the years[8] and partially demonstrated with on-ground experiments in both pulsed[9] and continuous intake flow conditions[10].

The ram-EP concept studied at ESA in 2007 shares some similarities with the ABIE concept but constitutes rather a broader, high level feasibility study with less focus on implementations details. It assumed a fairly generic reference design that could optionally include a compressor and a propellant storage unit[11]. It was followed by a technical assessment of the operation of conventional Hall thrusters and gridded ion thrusters with atmospheric propellants[12-14].

A number of studies with related but somewhat more restricted scopes have also been undertaken in the U.S. in recent years. A noteworthy attempt to solve an often overlooked issue is the demonstration of a microwave-powered cathode with a view to operate a complete EP system on atmospheric gases[15]. A follow-up experimental investigation of a hybrid Hall thruster with ECR ionization stage[16] and a later similar effort at University of Michigan to develop an air-breathing hybrid RF helicon Hall thruster[17] have both reported issues which question the viability of the dual-stage approach.

Although it actually focuses on a prospective Mars atmosphere breathing mission, the MABHET study initiated in 2011 at Busek is one of few U.S. studies that specifically address the question of the intake design[18]. Except for the use of CO_2 as propellant and the somewhat lower Knudsen numbers, the MABHET concept is based on the same general principles as JAXA's ABIE or ESA's ram-EP. As in the aforementioned U.S. concepts, it is meant to be propelled by a Hall thruster.

Pursuing the strategy outlined in the former ESA study[11] and based on a review of existing approaches and alternative concepts, this work discusses some of the main theoretical and practical considerations pertaining to the design of an intake for an air-breathing EP system. In a first step, the time-averaged and time-dependent environmental boundary conditions are derived based on a probable ram-EP mission profile, using the NMSISE-00 model. In a second step, a mission requirement analysis and intake concept trade-off analysis are performed to estimate the feasibility of various air-breathing principles. Finally, theoretical and numerical investigations of passive intakes are performed to assess the achievable compression ratios and collection efficiencies.

II. Assessment of space environment parameters

In most air-breathing EP studies, boundary conditions are qualitatively estimated from equatorial values and from a finite set of fixed levels of solar and geomagnetic activities taken from readily available sources such as the ISO 14222:2013 standard.[19] This section aims at providing a finer assessment of the environmental parameters that would be met in a realistic ram-EP mission.

A. Mission profile

A GOCE-like circular, dusk-dawn, sun-synchronous orbit is assumed. Without loss of generality, an ascending node at 18:00 is taken in all calculations but the assumption of an ascending node at 6:00 would not affect the conclusions in a measurable way. This type of orbit was also assumed in the former ESA study[11] as it minimizes eclipses and thus maximizes power availability meaning that, subject to compatibility with mission requirements, it is the most likely candidate for a ram-EP mission.

Altitude variations over the earth ellipsoid along the chosen circular orbit (approximately ±10 km) are accounted for as they are responsible for sizable variations of atmospheric densities. Therefore, *all altitudes referred to in this section are understood as mean orbital altitudes*. The range of mean altitudes investigated is 180-250 km. Indeed, theoretical calculations have indicated that the electrical power generated by solar arrays would become insufficient to compensate for the aerodynamic drag they generate under 180 km. Above 250 km, on the other hand, conventional propellant storage is likely to be more competitive than air-breathing due to the low drag.

B. Methodology

The operation of a ram-EP system is directly dependent upon the availability of atmospheric species and as such is directly affected by their density variations over both short-term scales (orbital period, diurnal cycle, solar activity transients) and long-term scales (annual cycle and solar activity cycle). For this reason, the relevant environmental parameters must be derived from an atmospheric LEO model. The ECSS-E-ST-10-04C space environment standard[20] recommends two atmospheric models, namely the NRLMSISE-00 model[21] and the JB-2006 model[22]. The NRLMSISE-00 model is generally considered better suited to cases where both the total density and the relative composition of the atmosphere are required. As the operation of EP systems is strongly dependent on the molecular composition of the propellant, this latter model is exclusively used in the assessment of the environmental parameters.

The NRLMSISE-00 model provides the density of the dominant species and the temperature as a function of the altitude, geodetic coordinates, time of the day, day of the year, current solar and geomagnetic activity (F10.7 index and AP index) and average solar activity within a period span of ±40 days (F10.7avg index). In order to assess as accurately as possible the effect of both short and long term effects on the actual trajectory (dusk-dawn sun-synchronous), a purpose-built orbital software library has been written, coupled directly to the NRLMSISE-00 model.

C. General influence of altitude

Altitude is the main factor in the determination of the relative composition and total number density of the thermosphere. The ISO 14222:2013 standard[19] provides number density values as a function of altitude derived directly from the NRLMISISE-00 model.

These values represent, however, averages over diurnal and seasonal variations at *equatorial latitudes* and *constant altitude*. They may therefore sensibly differ from the densities met along a circular sun-synchronous orbit where not only the latitude varies but also the actual altitude over the earth ellipsoid, with oscillations of the order of ±10 km about the mean altitude.

Because geodetic position is an important parameter of the NRLMSISE-00 model, accurate estimates for the assumed dusk-dawn sun-synchronous orbit have been derived from trajectory sampling every 3 min, reported in Figure 1. In keeping with the ISO 14222:2013 standard, the results have been averaged over diurnal and seasonal cycles and the ISO-defined *moderate* solar and geomagnetic activity levels ($F_{10.7}$=$F_{10.7avg}$=140, A_p=15) have been assumed.

As expected, the thermosphere between 150 km and 250 km is largely dominated by N_2 and O, atomic oxygen becoming slightly more abundant than N_2 under 190 km.

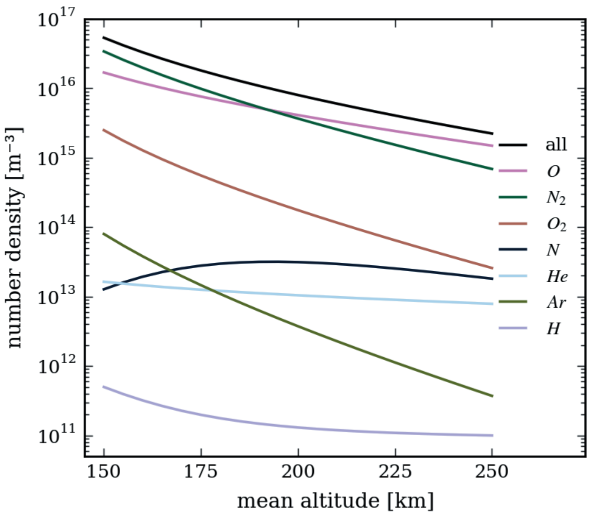

Figure 1. Mean thermosphere composition for circular dusk-dawn sun-synchronous orbits, averaged over orbital, diurnal and seasonal cycles, for moderate solar and geomagnetic activity[18] (NRLMSISE-00 model, $F_{10.7}$=$F_{10.7avg}$=140, A_p=15).

D. Long-term behavior

Although a complete statistical analysis of solar and geomagnetic activity is beyond the scope of this general feasibility study, it was deemed important to convey their general influence on the short-term and long-term variations of the main parameters by taking as reference historical activity measurements.

The period from January 1970 to December 2014 was selected, which covers approximately 4 full solar cycles. Historical indices compiled from Ref. 23 were used to compute the daily $F_{10.7}$ index, its 81-day average and the daily A_p index required by the NMSISE-00 model. The evolution of the thermospheric density and temperature variations along the orbit was simulated over this 45-year period with a 3-minute resolution (Figure 2).

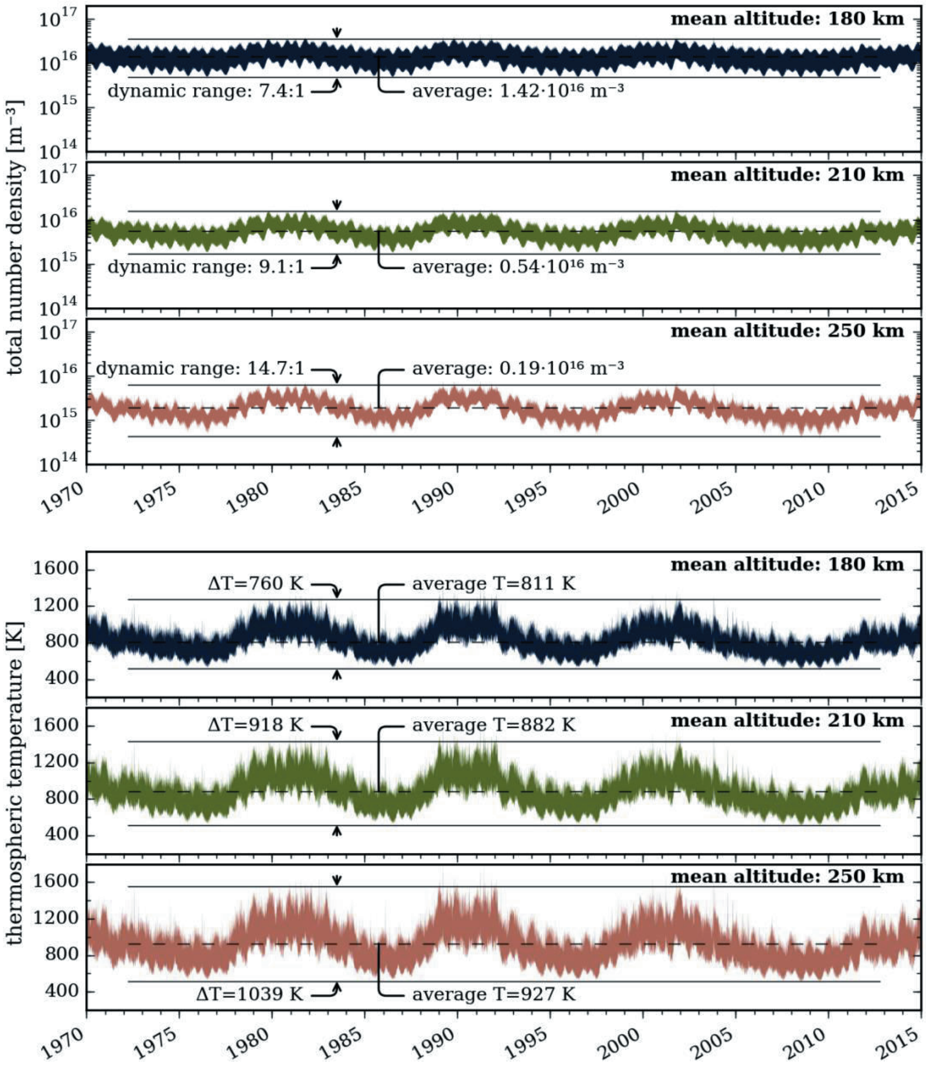

Figure 2. Day-by-day evolution of the diurnal variation range (vertical color-filled range) of the total number density and temperature at different altitudes along a circular dusk-dawn sun-synchronous orbit, based on historical solar and geomagnetic indices from January 1970 to December 2014 processed by the NRLMSISE-00 model. The indicated lower and upper bounds ignore transients which do not exceed 3 days.

The altitude dependency is seen to affect not only the mean values around which the density and temperature oscillate but also the amplitude of their long-term variations within each 11-year solar cycle.

Some short-lived extremes of high or low solar/geomagnetic activity can be identified in the form of thin hair-size spikes. Clearly, the atmosphere density at higher altitudes is much more affected by solar and geomagnetic activity, with a total dynamic range of total number density of 14.7:1 at 250 km. At 180 km, in contrast, orbital and diurnal density variations are greater but solar and geomagnetic activities have a much smaller impact, resulting in a smaller overall dynamic range of 7.4:1, which is only slightly greater than the variations generated by orbital, diurnal and seasonal effects alone.

Lower altitudes also translate into lower average temperatures and lower temperature gradients. This may be significant in the context of intake design since the conductance of long aspect ratio ducts directly depends on the divergence of the incident flow, which in turn depends on its transverse thermal velocity. Since thermal velocity only varies with the square root of temperature, however, the influence of temperature variations is expected to be relatively moderate.

Finally, simulations not shown here indicate that the relative composition is characterized by extreme variations over the short-time scales (orbital, diurnal and especially seasonal), but is relatively less affected by solar and geomagnetic activity. Even though the *average* composition changes from an N_2–dominated to a O-dominated atmosphere as altitude increases from 180 km to 250 km, the *instantaneous* number density can actually be dominated by either N_2 or O at any of the investigated altitudes depending on the date and time of the day.

III. Intake concept selection

Several options to raise the pressure of the collected propellant have been reviewed in a trade-off study. A short summary of the rationale behind the selection of the baseline intake concept is provided below.

A. Requirements in terms of collection efficiency

Drag compensation is the core function of the ram-EP system and as such imposes the main functional constraint, namely that the EP thrust be able to balance the drag at all time:

$$F_T = F_d = \frac{1}{2}\rho_b V_b^2 c_d A_c \tag{1}$$

where F_T is the thrust generated by the EP system, F_d is the drag force, ρ_b is the total mass density of the incident beam, V_b is the velocity of the incident beam (orbital velocity), c_d is the satellite drag coefficient and A_c the reference frontal area of the spacecraft, which in the sequel is assumed to be equal to the collection area of the intake. In a practical ram-EP system, only part of the incident stream will be collected and conducted to the thruster, which leads to the definition of the collection efficiency:

$$\eta_c \equiv \frac{\dot{m}_T}{\rho_b V_b A_c} \tag{2}$$

where $\dot{m}_T$ is the mass flow rate at the thruster inlet. Substituting now the EP thrust by $F_T = \dot{m}_T g_0 I_{sp}$ and thereafter the mass flow rate by its expression inferred from the above relation, one obtains the fundamental governing relationship for an air-breathing EP concept:

$$\eta_c g_0 I_{sp} = \frac{1}{2} V_b c_d \tag{3}$$

This relation is visually depicted in Figure 3. It reflects the intuitive view that a low I_{sp} should be compensated by a large collection efficiency and vice versa. In practice, it can be seen that a minimum collections efficiency of the order of 25-40% is required to match the typical I_{sp} of ion or Hall thrusters, assuming a c_d reasonably close to the theoretical minimum of 2.

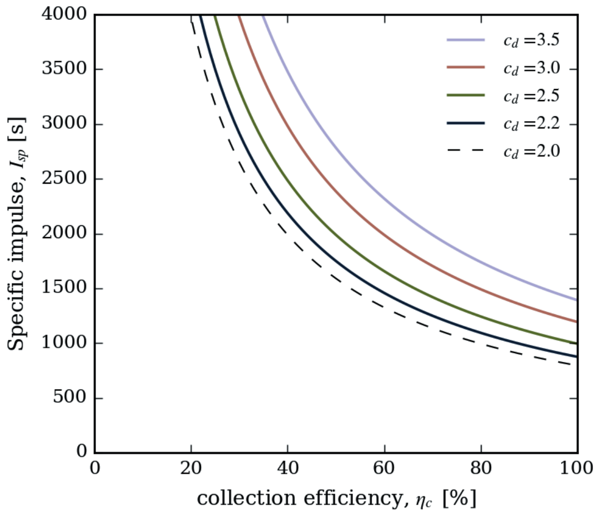

Figure 3. Relationship between specific impulse and collection efficiency required by an air-breathing EP system, as a function of the spacecraft drag coefficient.

B. Requirements in terms of pumping speed

Pumping speed is only relevant for active compression concepts as passive intakes are typically not constrained in terms of maximum flow rate.

The incentive for active compression is rather obvious if one considers that the average density expected at 200 km is of the order of 10^{16} m^{-3} whereas state-of-the-art ion and Hall thrusters in the 10 cm diameter range work with a nominal gas density of the order of 10^{19} m^{-3}. Due to the more favorable ratio of ionization free path to device dimension, larger thrusters such as the RIT-XT or PPS-20k ML manage to bring this figure down to nearly 10^{18} m^{-3}, but even with such thrusters a compression ratio of at least 100 would be mandated. Another important impetus for active compression is that it opens the possibility for on-board propellant storage in a pressurized tank, which would make it easier to cope with the wide fluctuations of density met in orbit.

For a reference altitude around 200 km, a number density of 10^{16} m^{-3} and a temperature of the order of 800 K can be taken for reference (see section II). This would translate into a pressure of the order of 10^{-4} Pa (10^{-6} mbar) at the intake, which falls within the High-Vacuum range. In practice, a passive pre-compression stage would be necessary to reduce pumping speed requirements and protect the pump from direct impingement by highly energetic particles. Taking into account a realistic passive compression ratio r=100 (to be justified later) and a wall temperature of 300 K, the pressure could be decreased to some $4\cdot10^{-3}$ Pa ($4\cdot10^{-5}$ mbar), which is still within the HV range. The pressure range 10^{-3}-10^{-2} Pa can thus be taken as reference for compressor inlet conditions.

The required pumping speed for the second (active) stage can be inferred from the pumping speed at the inlet of the first stage:

$$S_1 = V_b A_c \tag{4}$$

where V_b ≈7.8 km/s.

Assuming now that the passive pre-compression stage operates at a compression ratio r and collection efficiency η_c, the volumetric flow rate to be satisfied at the pump inlet becomes,

$$S_2 = \frac{\eta_c}{r} V_b A_c \tag{5}$$

Taking for instance r=100 and η_c=40%, a pumping speed of the order of 31 m^3/s per 1 m^2 of pump area is required. Considering this very high value together with the fact that low mass, low power and long maintenance-free operation are mandated in orbit, it becomes clear that active pumping is at the edge of feasibility, or more realistically—beyond.

C. Intake concepts assessment

A relatively exhaustive literature review and assessment of passive and active intake concepts has been performed which is only succinctly summarized in Table 1.

Expectedly, none of the intake concepts was found to score well in all critical area and a trade-off of the merits of each solution is required. In principle, the choice between passive and active intakes comes down to a trade-off between compression ratio and power/weight/feasibility. In practice, though, the feasibility of active intakes was judged low or very low, with in every case an impact in terms of mass and/or power that would most likely make it less competitive than a conventional stored propellant solution.

Table 1. Intake concepts trade-off summary.

<table>
<tr><th></th><th></th><th>Compression ratio</th><th>Feed line compatibility</th><th>Power requirements</th><th>Mass</th><th>Conceptual maturity</th><th>Estimated feasibility (cost, space qualif. etc)</th></tr>
<tr><td>Passive intakes</td><td>ABIE, MABHET</td><td>Low</td><td>Custom thruster inlet</td><td>Very low</td><td>Very low</td><td>Medium</td><td>High</td></tr>
<tr><td rowspan="3">Active intakes</td><td>Cryogenic</td><td>Very high</td><td>Very low</td><td>Very high</td><td rowspan="3">Very high</td><td rowspan="3">Very low</td><td>Very low</td></tr>
<tr><td>Turbomolecular</td><td>Very high</td><td>Very high</td><td>Low</td><td>Very low</td></tr>
<tr><td>Oil diffusion</td><td>Very high</td><td>High</td><td>Very high</td><td>Low</td></tr>
</table>

Within the current state of the art, the only reasonable concept for an active intake in terms of power requirement would be based on a turbomolecular compression stage. The expected development costs towards space qualification, very high mass and presumably very complex control of spurious angular momentum (even assuming contra-rotating rotors) make it, however, a rather unlikely option.

The passive intake option appears much more realistic in terms of feasibility, even if one considers the necessary customization of the thruster to provide a direct feeding option and to cope with low inlet pressures. It was for this reason ultimately selected as baseline intake concept.

IV. Theoretical analysis of passive intakes

A. Principle of operation

It was noted in the ABIE and MABHET studies that passive intakes with long aspect ratio ducts improve the containment of captured particles inside the collector[7,18]. This principle illustrated in Figure 4 relies on the fact that the velocity vector of particles collected is randomized upon their first impact inside the collector. This so-called momentum accommodation makes it difficult for back-streaming particles to exit back through the inlet duct since they will often experience a large number of collisions with the walls of the duct before they either escape back to the environment or return to the collector.

While the random-walk escape probability of collected particles obviously decreases rapidly with the length of the duct, the optimization of the duct aspect ratio must also consider that the incident flow is not perfectly collimated and that its transmission rate is also affected.

The main figure of merit for a duct operating in such manner is the co-called "transmission probability" or "Clausing factor".[24] This factor expresses the probability that particles which enter a duct are conducted towards its end. Clausing factors are for instance used to design ion thruster optics so as to minimize propellant leakage through the grids.

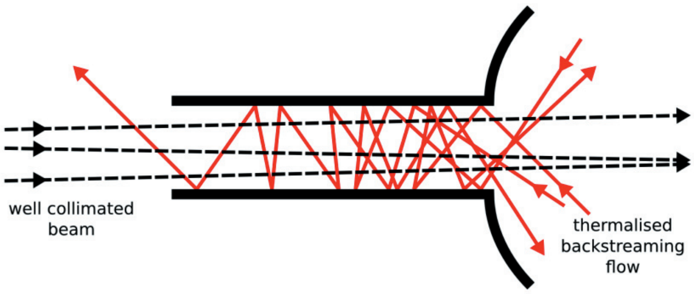

Figure 4. Principle of operation of a passive inlet duct: the transparency towards the well collimated incident flow is much greater than towards the contained thermalized flow.

Although some approximate formulae exist, there is in general no closed-form exact formula for Clausing factors, even for Maxwell–Boltzmann distributions. For this reason, *Q-MoST*, a free molecular flow simulation tool developed at QuinteScience, was used to compute the transmission probabilities for various candidate duct shapes.

B. Transmission probability towards thermalized and collimated flows

As usually done for Clausing factor computations, total momentum and energy accommodation is assumed upon impact with a wall, *i.e.* particles acquire a velocity that is statistically consistent with a thermal equilibrium with the wall.

The transmission probabilities for species confined within the collector also assume that particles have a Maxwell-Boltzman distribution at the inlet, as is usually implicitly implied for Clausing factors. It is worth noting that with these hypotheses, the transmission probability depends neither on the mass of the species nor on the temperature at the inlet or in the collector.

The transmission probability for the beam population incident on the inlet must account, however, for the velocity shift imparted by the relative motion of the spacecraft in its environment. In the reference frame of the spacecraft, the distribution function for incident particles is thus a shifted Maxwell-Boltzmann distribution. In all calculations, the following parameters have been assumed for the collimated beam:

$$\begin{aligned} T_b &= 1000\ \mathrm{K} \\ V_b &= 7800\ \mathrm{m/s} \end{aligned} \tag{6}$$

The computed transmission probabilities are reported in Figure 5 as a function of the aspect ratio $L/\sqrt{A}$ where L is the length of the duct and A its cross-section area. It is interesting to note that the exact shape of the duct cross-section appears to play a relatively minor role. As expected, a duct with a circular section optimally exploits the symmetry of the beam and leads to the highest beam transmission probability, but the decrease of transmission probability with hexagonal and square sections is only barely noticeable. In all cases, beam transmission for atomic oxygen is somewhat worse than N_2 due to the greater value of the transverse thermal velocity component and subsequent lower collimation degree.

In practice, thus, it appears possible to select an aspect ratio such that the beam transmission probability is approximately 3-4 times greater than the transmission probability for back-streaming thermalized particles.

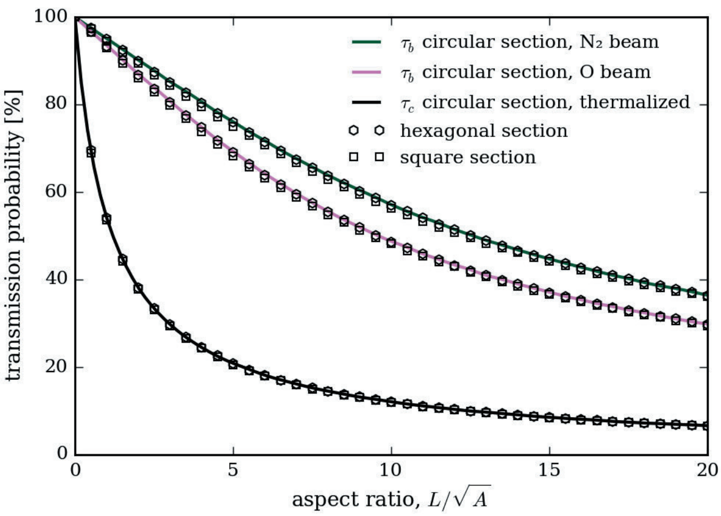

Figure 5. Transmission probability as a function of duct aspect ratio for various cross-section shapes, towards a collimated incident beam of atomic oxygen or N_2 (V_b=7800 m/s, T_b=1000 K) and towards a thermalized flow (conventional Clausing factor).

C. Theoretical intake performance

A simplified theory can be derived by assuming that particles are collected by an inlet of area A_c and are ideally thermalized inside a collector at a temperature T_c before they are either transmitted to the thruster (inlet area A_t) or lost due to back-streaming through the inlet. This theoretical description is applicable to both single-duct collectors and multiple-duct collectors.

The conservation equation for particles in the collector reads:

$$\begin{aligned} A_t \frac{n_c}{2} \bar{v}_c \tau_t &= A_c n_b V_b \tau_b - A_c \frac{n_c}{2} \bar{v}_c \tau_c \\ &= A_c n_b V_b \eta_c \end{aligned} \tag{7}$$

where τ_t is the transmission probability through the thruster, τ_b is the transmission probability of the incident beam through the inlet, τ_c is the transmission probability of the thermalized back-streaming flow through the inlet, n_b is the number density of the beam and n_c is the number density inside the collector. Note that the fractions $\frac{1}{2}$ in the above relation arise from the fact that only half of the particles have velocities directed towards the inlet of the collector (or of the thruster), in contrast to beam particles which nearly all have their velocity vector pointing to the inlet. The average thermal speed in a given direction for particles inside the collector is:

$$\bar{v}_c \equiv \sqrt{\frac{2\kappa T_c}{\pi m}} \tag{8}$$

Eq. (7) leads to the following reciprocal relationships between collection efficiency and compression ratio:

$$\eta_c = \tau_b - \frac{1}{2}\frac{n_c}{n_b}\frac{\bar{v}_c}{V_b}\tau_c \quad (9)$$

$$\frac{n_c}{n_b} = 2\frac{V_b}{\bar{v}_c}\frac{\tau_b - \eta_c}{\tau_c} \quad (10)$$

which make it clear that collection efficiency and compression ratio must in general be traded off.

D. Intake design optimization

For the purpose of intake optimization, it is useful to introduce the *effective thruster transmission probability*:

$$\tau_t^* \equiv \tau_t \frac{A_t}{A_c} \quad (11)$$

which embodies the fact that the overall transmission probability from the perspective of a random particle inside the collector is proportional the thruster cross-section area.

Introducing the above parameter, the efficiency and compression ratio can be rewritten in a relatively simple form:

$$\eta_c = \tau_t^* \frac{\tau_b}{\tau_t^* + \tau_c} \quad (12)$$

$$\frac{n_c}{n_b} = 2\frac{V_b}{\bar{v}_c}\frac{\tau_b}{\tau_t^* + \tau_c} \quad (13)$$

These expressions are convenient in that they show that for a given τ_t^*, the collection efficiency and the compression ratio are simultaneously maximized when the aspect ratio maximizes $\tau_b/(\tau_t^* + \tau_c)$. The existence of this optimum greatly simplifies the design study as ultimately the trade-off is solely expressed in terms of τ_t^*.

The robustness of the optimization process is warranted by the existence of a large tolerance towards departures from the optimal aspect ratio. This is illustrated by Figure 6 which uses the data of Figure 5 for circular ducts to estimate the tolerable aspect ratio range that warrants the achievement of at least 95% of peak performance in terms of collection efficiency and compression ratio.

Because the optimal aspect ratio ranges for N_2 and O largely overlap, the composition of the collected gas is not critical from the viewpoint of intake design optimization. Likewise, the wide range of the optimal aspect ratio would likely allow the accommodation of wide variations in terms of temperatures T_b and T_c.

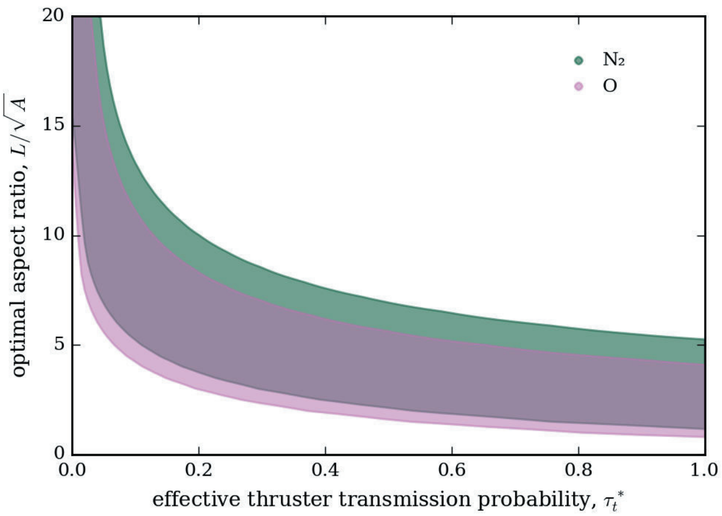

Figure 6. Range of inlet aspect ratio warranting near-optimum (>95% of theoretical maximum) collection efficiency and compression ratio, as a function of the effective thruster transmission probability.

E. Numerical validation of the model

In order to test the theory and assess the limits of its applicability, parametrical numerical simulations have been performed with the *Q-MoST* code, using as a benchmark a simple cylindrical collector with a circular outlet orifice representing the thruster feed. The inlet is made of a honeycomb duct array. Since only the aspect ratio of inlet ducts is significant, for computational ease the inlet was modeled as an infinitely thin honeycomb membrane which hexagonal pores have the desired aspect ratio. In order to facilitate comparison with theory, the aspect ratio used in simulations was set to the theoretical optimum for each investigated τ_t^*.

Simulations assume a free molecular flow as well as full momentum accommodation and full energy accommodation at the walls and in the inlet ducts. The geometrical setup and boundary parameters of the benchmark collector are displayed in Figure 7. Since the transmission probability of an open orifice is $\tau_t = 1$, the effective transmission probability in this geometrical model is simply $\tau_t^* = A_t/A_c$.

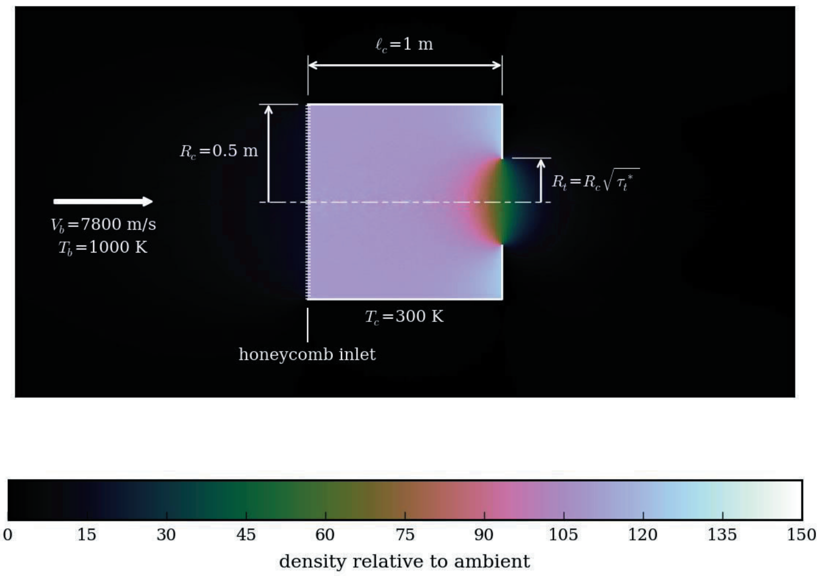

Figure 7. Collector geometry used in numerical simulations. The inlet is modeled as an infinitely thin honeycomb porous membrane with controlled pore aspect ratio. In the case shown, the species is N_2 and $\tau_t^* = 0.2$.

Since the pressure inside the simulated collector is non-uniform and exhibits sharp rarefaction gradients near the orifice, the definition of the compression ratio in numerical simulations requires the prior identification of a reference number density. In this respect, it is useful to note that, from the thruster viewpoint, the detailed topology of the number density inside the collector is actually irrelevant: its only visible impact is the relationship between the flux and the velocity of particles escaping from the collector. For this reason, the reference density $\langle n_c \rangle$ for the interpretation of numerical simulations was defined as the density which an infinite plenum in thermal equilibrium would need to have to provide the same ratio of total flux to average velocity within the orifice plane.

The relationship between the optimal η_c and n_c/n_b obtained by varying τ_t^* is summarized in Figure 8. The excellent match between theory and simulations is noteworthy: it emphasize not only the adequateness of the theory but also the fundamental character of the trade-off between collection efficiency and compression ratio.

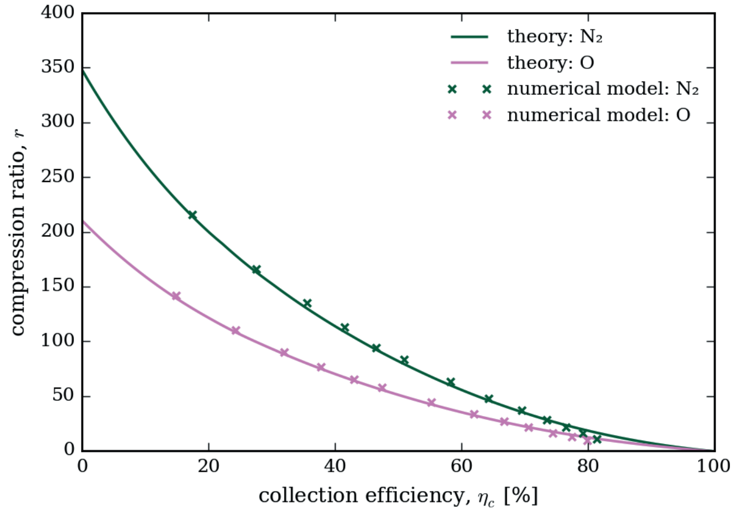

Figure 8. Trade-off between collection efficiency and compression ratio obtained by varying τ_t^*: comparison between theory (continuous lines) and numerical simulations ($0.05 \leq \tau_t^* \leq 0.3$ in increments of 0.05 and $0.3 < \tau_t^* \leq 1$ in increments of 0.1).

V. Practical design considerations

A. Compression vs collection efficiency trade-off

The main design trade-off for a passive intake consists in balancing the compression ratio with the collection efficiency through an appropriate choice of τ_t^*, as shown in Figure 8.

Note that, unlike the compression ratio, the collection efficiency is only weakly dependent on the species for a given geometry so the nominal operating point can be in practice identified by the target collection efficiency. While the ultimate target collection efficiency would need to consider a number of aspects and would require a well-characterized propulsion subsystem, the 25-40% range appears as a likely trade-off region to ensure reasonable levels of compression ratios. This corresponds to τ_t^* in the range 0.1-0.2 and optimal aspect ratios $L/\sqrt{A}$ in the 5-10 range.

Since the thruster transmission probability τ_t is likely to be large for both ion and Hall thrusters, the desired value of $\tau_t^* = \tau_t A_t / A_c$ would mainly be achieved by varying the thruster cross-section area. Realistically, thruster cross-sections larger than that of conventional thrusters of similar nominal flow rates are to be expected, which may be actually beneficial from the point of view of lifetime due to the reduced erosion rate. It may also help with ionization efficiency since the minimum required gas density is inversely proportional to the linear extent of the ionization zone.

B. Misalignment between intake axis and orbital velocity vector

An important practical consideration in the design of a passive ram-EP intake is robustness towards misalignment of the inlet ducts with the incident flow. Although misalignment is expected to be mitigated by appropriate use of static stabilizing fins like in GOCE, its impact needs to be assessed in order to confirm the feasibility of the concept and its requirements in terms of attitude control accuracy.

Computations were performed with *Q-MoST* for circular ducts at various inlet aspect ratios to assess the impact of misalignment on beam transmission probability. The computation results displayed in Figure 9 show negligible loss of beam transmission up to 2° and a maximum relative loss of 15% at 5° for aspect ratios $L/\sqrt{A}$ up to 8. This is qualitatively consistent with the figures obtained with Monte-Carlo simulations with respect to the ABIE design.[7]

While not entirely benign, thus, the impact of misalignment is expected to be relatively moderate unless very large inlet aspect ratios are chosen, which should not be mandated in practice given the large range of aspect ratio warranting near-optimal operation (Figure 6).

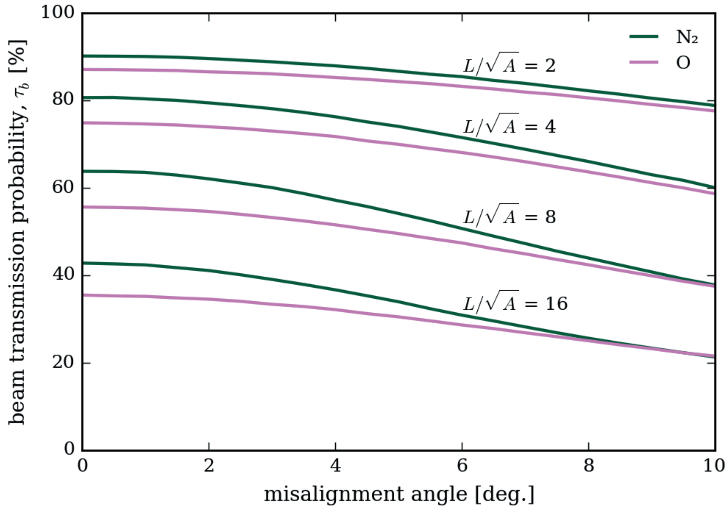

Figure 9. Impact of misalignment between a circular-section inlet duct and the incident flow on the beam transmission probability.

C. Duct geometry

In general, for a given inlet aspect ratio, there is little incentive in using a single long inlet duct instead of an inlet made of an array of small-diameter ducts.

It is easily shown that for a given $L/\sqrt{A}$ and for a given total intake area, the total "wet" area of the inlet (area of walls in contact with the collected flow) remains actually the same whether the intake is of the single-duct or multiple-duct type. Therefore, if the inlet is made from a constant thickness metal sheet, a honeycomb inlet will not only be much more compact but will actually be nearly twice lighter owing to the fact that each wall is in contact with the flow on both sides (except for peripheral ducts).

The only consideration that may favor a single duct inlet is the theoretical possibility to achieve a partially adiabatic compression by working in the transition regime between continuum and free molecular flow. In order to obtain sizable gains, however, a density of the order of $10^{17}\mathrm{m}^{-3}$ would be required[18] which can only be achieved below 180 km, an altitude range which is not accessible to solar-electric missions as the power necessary to compensate for solar array drag becomes then greater than the generated power.

As a rule, therefore, the use of a multiple-duct inlet is recommended, which may for instance be realized with a honeycomb inlet or a split-ring inlet with constant duct cross-section.

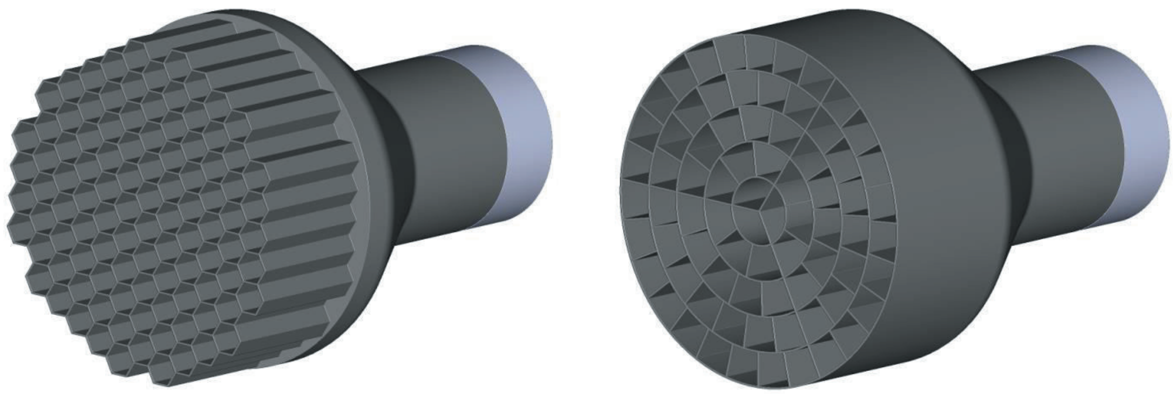

Figure 10. Honeycomb and split-ring multiple-duct inlet design. The split-ring design maintains a constant duct cross-section area through an adequate geometrical parameterization.

D. Possible integration with the payload

From a general perspective, however, one may distinguish between (i) non-intrusive integration whereby interferences between the payload and the intake operation are avoided at the price of increased spacecraft drag and (ii) low-drag integration where the payload is placed in the vacuum wake of the collector inlet, for instance surrounding a reduced diameter duct that feeds the thruster.

The ABIE design clearly falls in the first category: while the payload constitutes a non-negligible source of drag, its seamless integration with the inlet presumably leads to very little impact on the intake performance. By contrast, a low-drag design partially compromises intake performance due to the pressure loss inside the duct conducting the propellant to thruster. A qualitative assessment of the performance impact of such design has been made with numerical simulations using a realistic setup with a Hall-thruster type annular outlet. A relatively significant reduction of the compression ratio from 110 to 81 and of the collection efficiency from 31% to 23% was observed when passing from a compact intake to the intake shown in Figure 11 where the inlet is extended by a reduced-diameter duct to leave room for an annular outer payload. Further optimizations appear thus required to make the non-intrusive design viable.

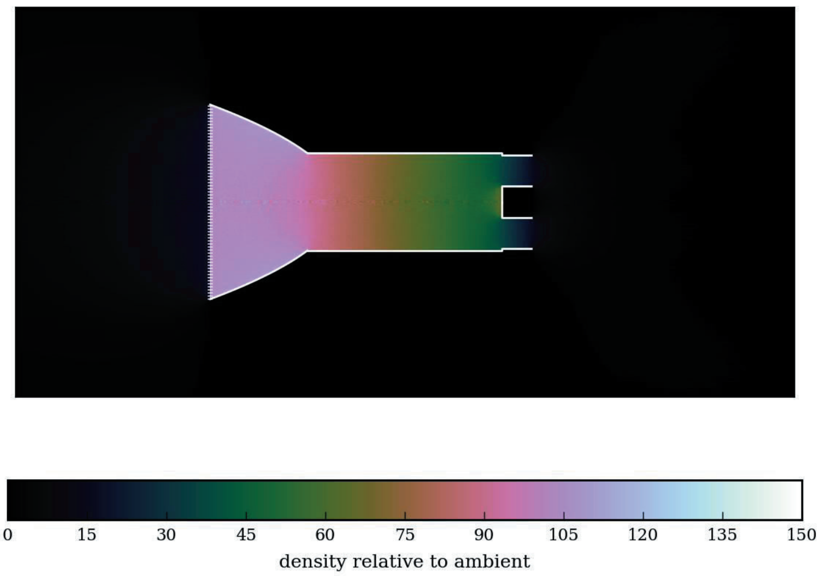

Figure 11. Intake simulation for a low-drag payload integration setup in which the payload is located in the vacuum wake of the intake, around the thruster feed line (inlet simulated as an infinitely thin honeycomb porous membrane).

VI. Conclusion

In view of the sheer complexity, power consumption and mass impact associated with active compression concepts, the passive intake contemplated in several recent air-breathing electric propulsion studies[7-11,18] was upheld as the most realistic candidate for long-duration low earth orbit missions.

While not new, the idea to exploit long aspect ratio ducts for a passive intake had up to now never been formalized from the theoretical viewpoint. The simplified intake model proposed in this work has been validated against a particle-based simulation and has proved very useful for preliminary design assessment due to its ability to predict the optimal duct aspect ratio depending on the intake area and on the flow impedance generated by the thruster. Although the ideal aspect ratio depends in principle also on atmospheric composition, it was shown that it is usually possible to operate at more than 95% peak performance with either pure N_2 or pure atomic oxygen flows using a fixed aspect ratio. Ultimately, the main design trade-off for a passive intake is thus confined to the choice of a suitable nominal operation point in terms of compression ratio and collection efficiency: a larger thruster that offers smaller flow resistance will typically lead to better collection efficiency, but also to smaller compression ratios.

Among several potential design issues identified, the impact of misalignment between spacecraft and orbital motion axis was found to be relatively uncritical, with significant performance degradation occurring only when misalignment becomes of the order of 5°. Optimal integration with the payload appears to be a more complex issue, however, which will likely require an iterative design process and extensive recourse to numerical simulations.

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