Multidisciplinary Design Optimization for a Solar-Powered Exploration Rover Considering the Restricted Power Requirement

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Received: 29 October 2020; Accepted: 14 December 2020; Published: 16 December 2020

Abstract: The energy requirements of a solar-powered exploration rover constrain the mission duration, traversability, and tractive capability under the given limited usable power. Thus, exploration rover design, more specifically, rover wheel design (related to considerable energy consumption in driving), plays a significant role in the success of exploration missions. This paper describes the modeling of an operational environment and a multi-body dynamics (MBD) simulation tool based on wheel-terrain interaction model to predict the dynamic behavior on a digital elevation model (DEM) map. With these simulation environments, a multidisciplinary optimal wheel design methodology, integrating the MBD simulation tool and non-dominated sorting genetic algorithm-II (NSGA-II), is developed. Design parameters are chosen through sensitivity analysis. These multi-objective optimizations in dynamic states are conducted to obtain the optimal wheel dimension that meet the limited power condition with maximal tractive capability under the given operational environment. Furthermore, numerical and experimental verification using a single wheel testbed on lunar simulant are conducted to convincingly validate the derived optimization results. Finally, these results reveal that the proposed design methodology is an effective approach to deciding the best design parameter among a large variety of candidate design points considering the restricted power requirement.

Keywords: multi-objective design optimization; solar-powered exploration rover; operational environment; multi-body dynamics (MBD); wheel-soil interaction; single wheel testbed

1. Introduction

When a solar-powered exploration rover traverses the Moon and/or planetary surface on a scientific mission, it has a limited mission duration because of limitations regarding the available energy, and therefore, prediction on the power condition are key aspects of the success of the mission. In addition, the planetary and/or lunar surface are composed of a deformable terrain with various geometry ground composition; thus, it is indispensable to improve the tractive capability of the exploration rover under the given limited energy resources [1,2]. Moreover, the tractive capability or traversability of an exploration rover, which is dictated by various factors, such as the tractive coefficient, sinkage, etc., is directly a function of the wheel and terrain interaction on the driving surface. For example, under these physical phenomena, the deformable terrain below the wheel is partially compacted, leading to improved terrain resistance, and the exploration rover has a source of substantial power loss and reduced tractive capability at that time. Previous research has reported that about 20% to 55% of the power delivered to the vehicle to drive wheels is wasted in the wheel-terrain interaction [3]. Many researchers have conducted extensive studies and developed technologies for exploration rovers. The physical phenomena involved in the wheel-terrain
interaction have been investigated through numerical and experimental approaches [4–16]. Some studies have investigated the design and development for a novel type of the exploration rover that considers the obstacle-climbing capability and/or locomotion performance [17–23]. Several studies have also analyzed the effects of the wheel shape [24–29].

From these studies, the rover wheel features, such as the wheel radius, width, and lug shape, are observed to significantly influence dynamic performance in terms of factors such as the tractive coefficient of the rover and its rolling resistance (and therefore energy dissipation). However, increasing the wheel dimensions to improve tractive coefficient (related to the tractive capability) in all dynamic states under the given terrain foundation increases the power consumption of the driving part of the exploration rover. Moreover, in exploration mission, the available energy is limited. Up to this point thus far, the wheel design of the exploration rover is a difficult assignment for designers who now have to decide on an appropriate wheel shape under a large variety of wheel design parameters. Nonetheless, comprehensive research regarding the wheel design guidelines or methodology for enhancing the dynamic performance in all dynamic state, under the given operational environment with limited available energy, has not yet been conducted to date. For such reasons, this paper proposes a multidisciplinary wheel design methodology integrating MBD simulation and optimization technique to provide a more comprehensive design recommendation. This research is different from other studies on the related design approach because it considers not only the dynamic performance in various driving conditions but also the amount of usable power based on the reasonable operational environment model. Through this proposed approach, we effectively determine the optimal wheel shape of the rover, covering the multiple dynamic states under the condition of limited power, which we have not encountered in other related researches.

In this paper, we describe the modeling of operational environment that predicts the usable power condition based on the solar irradiance and terrain characteristics on the Moon. In addition, we also analyze the basic system level requirements for the on-going Korean lunar exploration program, which includes the survey of previous development rovers. To obtain an identical and compromised selection of design points, this research conducts a sensitivity analysis and multidisciplinary optimal wheel design methodology that integrates the MBD simulation based on the rover wheel-terrain interaction model and multi-objective optimization algorithm (NSGA-II) considering the restricted power requirement. Moreover, further numerical and experimental analysis using a single wheel tested on the lunar simulant are conducted to validate the derived optimization results when compared with the reliable comparative group.

The rest of this study is organized as follows. In the next section, we present the modeling of the power acquisition and terrain composition as an operational environment. In Section 3, we describe the developed MBD simulation tool based on wheel-terrain interaction phenomena. In Section 4, we explain sensitivity analysis and our multidisciplinary design optimization methodology in dynamic states under the operational environment. In Section 5, we report on the scenario-based MBD simulations to demonstrate the feasibility of the proposed design methodology. In this part of the study, further experimental verification is conducted. Finally, in Section 6, we conclude the study.

2. Modeling of the Operational Environment

2.1. Power Acquisition Model

In general, the use of solar energy has been demonstrated to be an excellent choice as a power source in solar system exploration. It serves enough energy to operate rovers on the Moon and allows the mission duration to be expanded [30,31]. To accomplish our goal of designing the optimal wheel shape for a solar-powered exploration rover on a given operational environment, it is necessary to know the amount of power acquisition as a radiation field environment that reaches the lunar surface. From this point of view, this subsection presents the power acquisition for the solar-powered exploration rover, and how it varies according to the spatial relationship between the Moon and the Sun, and the energy radiant quantities of the Sun. The energy radiant quantity of the \( G_{SR} \), or the solar constant, changes between 1413 and 1322 and thus, in our calculations in the proposed wheel design
procedures, we consider a mean value of 1367 W·m⁻² from the spring and fall seasons. The effects of seasonal/locational solar energy radiant on the Moon, providing insights for future work and developments, are discussed in Section 5.

In addition, the solar position changes over a cycle of 29.5 days and differs according to the latitude and longitude [31–33]. Thus, the solar position can be estimated using the solar elevation angle $\alpha_s$ and azimuth angle $\beta_s$, and is derived as a function of the latitude $\varphi$, declination $\delta$, and hour angle $\omega$. The solar elevation and azimuth angle are expressed as:

\begin{equation}
\sin \alpha_s = \sin \delta \sin \varphi + \cos \delta \cos \varphi \cos \omega,
\end{equation}

\begin{equation}
\tan \beta_s = \frac{\cos \delta \sin \omega}{\sin \delta \cos \varphi + \cos \delta \sin \varphi \cos \omega}.
\end{equation}

Under the assumption that the exploration rover is above the given surface, the incidence angle of the Sun $\theta_i$ varies with the solar position. The incidence angle of the Sun can be expressed using the following equation:

\begin{equation}
\cos \theta_i = \sin \alpha_s + \cos \alpha_s \cos \beta_s.
\end{equation}

Finally, the power collected by the rover $P_{obt}$ is:

\begin{equation}
P_{obt} = \eta_{cell} G_{sc} S_{cell} \cos \theta_i,
\end{equation}

where $\eta_{cell}$ and $S_{cell}$ denote the efficiency and the area, respectively, of the solar cell. Based on Equations (1)–(4), the solar elevation, azimuth and power acquisition according to the various landing sites on the Moon are shown in Figure 1a–c, respectively.
Figure 1. Solar elevation, azimuth angles, and power acquisition according to various landing sites on the Moon: (a) solar elevation angles; (b) solar azimuth angles; (c) power acquisitions.

2.2. Modeling of Terrain Characteristics in Lunar Simulant

Real lunar regolith rarely exists on Earth because of the difficulty in gathering samples from outer space. Therefore, lunar simulants were developed over the last few decades for use on Earth in various scientific and engineering studies related to lunar/planetary exploration missions. In this research, the KLS-1 lunar simulant, which was produced using basalt from an area of Cheorwon in South Korea, was used to generate the bevameter and single wheel testbed ground. It has a particle-size distribution and chemical properties that are closer to those of real lunar regolith than do other lunar simulants (JSC-1, FJS-1), as shown in Figure 2 [34,35]. Thus, it is a suitable material for performing and experiment based on wheel-terrain interaction for exploration rover.

Figure 2. Korean lunar simulant KLS-1 [35]: (a) Photographic and 500X-magnified SEM images of KLS-1 lunar simulant; (b) particle-size distribution of real lunar regolith and lunar simulants JSC-1, FJS-1, and KLS-1.

2.2.1. Pressure-Sinkage Relationship Associated with Normal Stress Component

The pressure-sinkage relationship is employed to derive the normal pressure and sinkage formulas, which are subsequently used to derive the physical phenomena, associated with normal stress, below the rover wheel on the lunar surface. Furthermore, this method ultimately depends on experimentally determined terrain characteristics. Describing the normal pressure and sinkage under a rectangular plate, a relatively simplified equation on the pressure-sinkage relationship suggested by Wong and Reece is:
\[ \sigma = k'_{eq}(z_{plate}/b_{plate})^n, \] (5)

where \( \sigma \) is normal pressure, \( z_{plate} \) is sinkage of the plate, \( b_{plate} \) is width of the plate, \( n \) is exponent of terrain deformation, and \( k'_{eq} = c k' + \rho b k' \) is pressure-sinkage modulus. To determine the terrain characteristics using measured normal pressure \( \sigma_m \), the Levenberg-Marquardt method is used to find the \( n \) and \( k'_{eq} \) in this study. The pressure-sinkage test was conducted to determine the terrain characteristics associated with normal stress on lunar simulant, as shown in Figure 3. From the experimental results, numerical analysis was utilized to derive the terrain characteristics for \( n \) and \( k'_{eq} \), and Table 1 summarizes the derived values associated with normal stress.

![Experimental results of pressure-sinkage test for identifying the terrain characteristics associated with normal stress on the lunar simulant.](image)

**Figure 3.** Experimental results of pressure-sinkage test for identifying the terrain characteristics associated with normal stress on the lunar simulant.

**Table 1.** Predicted lunar simulant parameters.

| Parameter | Description         | Unit   | Value  |
|-----------|---------------------|--------|--------|
| \( \gamma \) | relative density    | ton/m³ | 1.756  |
| \( n \)   | sinkage exponent    | –      | 0.5425 |
| \( k'_{eq} \) | pressure-sinkage modulus | kN/m\(^n\) | 1.1453 |
| \( c \)   | cohesion            | Pa     | 1999.99|
| \( \emptyset \) | friction angle      | deg    | 41.55  |
| \( K \)   | shear deformation modulus | m      | 0.0017 |

2.2.2. Mohr–Coulomb Failure Criterion/Janosi–Hanamoto Model Associated with Shear Stress Component

The wheel-terrain interaction model also uses the Mohr–Coulomb failure criterion and Janosi–Hanamoto model, together with the pressure-sinkage relationship. According to the Mohr–Coulomb failure criterion, for a given normal load, the shear strength-maximum normal stress relationship is represented by a straight line, and is expressed as:

\[ \tau_{max} = c + (\sigma \tan \emptyset), \] (6)

where \( \tau_{max} \) is the maximum shear stress, \( \sigma \) is for each value of the applied normal stress, \( c \) is the cohesion, and \( \emptyset \) is the friction angle. In addition, to describe the shearing displacement in deformable terrain, Janosi and Hanamoto proposed the shear stress-shear displacement relationship:

\[ \tau = \tau_{max}(1 - \frac{j}{K}), \] (7)

where \( \tau \) is the shear stress, \( j \) is the shear displacement, and \( K \) is the fitting shear modulus. Moreover, to identify the terrain characteristics associated with shear stress through the measured shear stresses \( \tau_m \) and measured maximum shear stress \( \tau_{m,max} \), the Levenberg-Marquardt and the linear fit method are used to find the \( c, \emptyset, \) and \( K \). The terrain characteristics associated with shear
stress were taken from the experimental data in [34,35]. Figures 4 and 5, and Table 1 show the experimental results and derived values associated with shear stress.

![Figure 4](image1.png)

**Figure 4.** Experimental results of direct-shear test for identifying the terrain characteristics associated with shear stress on the lunar simulant using the Mohr–Coulomb failure criterion.

![Figure 5](image2.png)

**Figure 5.** Experimental results of direct-shear test for identifying the terrain characteristics associated with shear stress on the lunar simulant using the Janosi–Hanamoto equation.

### 3. Multi-Body Dynamics Simulation Tool Based on the Wheel-terrain Interaction Model

#### 3.1. Wheel-Terrain Interaction Model

After modeling the power acquisition and terrain characteristics as an operational environment, the wheel-terrain interaction model is used to design the optimal rover wheel on the deformable terrain, as shown in Figure 6. While the solar-powered exploration rover is traveling across a flat/inclined terrain, the force/torque acting on the rover wheel are completely defined by the integrals of the normal and shear stress below the wheel, which change the angular position $\theta$ [8,9]. Given this model, the normal stress $\sigma(\theta)$ can be represented as:

$$\sigma(\theta) = \begin{cases} 
k'_{eq} \left( \frac{r}{b} \right)^n (\cos \theta - \cos \theta_f)^n & (\theta_m \leq \theta < \theta_f) \\
k'_{eq} \left( \frac{r}{b} \right)^n \left[ \cos (\theta_f - \frac{\theta - \theta_r}{\theta_m - \theta_f} (\theta_f - \theta_m)) - \cos \theta_f \right]^n & (\theta_f < \theta \leq \theta_m) \end{cases}$$

(8)

where $r$ is the radius of wheel, $b$ is the width of the wheel, $\theta_f$ is the wheel entry angle, $\theta_r$ is the exit angle of the wheel, and $\theta_m$ is the specific angle at which the normal stress is maximized. In addition, this normal stress, which consists of two portions, namely the grousers and concave portions, is rewritten to analyze the influence of the wheel grouser through the sensitivity analysis, which will be discussed in Section 4. Thus, the equivalent normal stress $\sigma_{eq}(\theta)$ denotes the average of the normal stress at grouser tip $\sigma_c(\theta)$ and concave portion $\sigma_c(\theta)$, which can be expressed as follows [36]:
\[
\sigma_c(\theta) = \begin{cases} 
\mu \sigma_c(\theta) + (1 - \theta) \sigma_c(\theta) & (\theta_r \leq \theta \leq \theta_{fc}) \\
\mu \sigma_c(\theta) & (\theta_{fc} \leq \theta \leq \theta_f) 
\end{cases}
\]

where \( \mu \) is the area ratio of the grouser, and \( \theta_{fc} \) and \( \theta_f \) denote the wheel entry angle, which are derived from the geometry conditions corresponding to the concave and grouser portions, respectively. For the case of the grouser wheels, the outer radius of the grouser wheel, accounting for the grouser height \( h \), can be replaced, i.e., the outer radius \( r_e \) is equal to \( r + h \). Meanwhile, the slip ratio \( \lambda \) and slip angle \( \beta \) are expressed as:

\[
\lambda = \frac{1 - (v_x/r_\omega)}{(r_\omega/v_x)} \quad \text{(if} |r_\omega| > |v_x|:\text{driving)}
\]

\[
\beta = \tan^{-1}(v_y/v_x),
\]

where \( v_x \) is the longitudinal velocity, \( v_y \) is the lateral traveling velocity, and \( \omega \) is the angular velocity. Furthermore, the longitudinal \( \tau_x \) and lateral shear stress \( \tau_y \) on the driving surface are calculated from Equations (12) and (13):

\[
\tau_x(\theta) = (c + \sigma(\theta)\tan\theta)[1 - e^{-jl_x(\theta)/k}],
\]

\[
\tau_y(\theta) = (c + \sigma(\theta)\tan\theta)[1 - e^{-jl_y(\theta)/k}],
\]

where \( j_x(\theta) \) and \( j_y(\theta) \) denote the shear displacements beneath the rover wheel, which are derived using the longitudinal \( v_{jx}(\theta) \) and lateral \( v_{jy}(\theta) \) slip velocities on deformable terrain. These slip velocities are expressed as:

\[
v_{jx}(\theta) = r_\omega[1 - (1 - \lambda)\cos\theta],
\]

\[
v_{jy}(\theta) = -r_\omega(1 - \lambda)\tan\beta.
\]

The shear displacements in each direction are then computed:

\[
j_x(\theta) = r[\theta_f - \theta - (1 - \lambda)(\sin\theta_f - \sin\theta)],
\]

\[
j_y(\theta) = r(1 - \lambda)(\theta_f - \theta)\tan\beta.
\]

The forces and torques on a flat/inclined deformable terrain can be calculated via integration of the normal and shear stress distributions along the driving surface, as depicted in Figure 6. Finally, the corresponding forces and torques can be calculated as:

\[
\begin{align*}
F_x &= rb \int_{\theta_r}^{\theta_f} \tau_x(\theta) \cos\theta - \sigma(\theta)\sin\theta \, d\theta \\
F_y &= \int_{\theta_r}^{\theta_f} R_b(r - z(\theta)\cos\theta) - rb \tau_y(\theta) \, d\theta \\
F_z &= rb \int_{\theta_r}^{\theta_f} \tau_z(\theta) \sin\theta + \sigma(\theta)\cos\theta \, d\theta \\
T_x &= -r^2b \int_{\theta_r}^{\theta_f} \tau_y(\theta) \cos\theta \, d\theta \\
T_y &= -r^2b \int_{\theta_r}^{\theta_f} \tau_x(\theta) \, d\theta \\
T_z &= -r^2b \int_{\theta_r}^{\theta_f} \tau_y(\theta) \sin\theta \, d\theta \\
R_b(z) &= D_1 \left\{ z(\theta) \cdot c + \frac{1}{2} \rho z(\theta)^2 \cdot D_2 \right\} \\
D_1(X_c, \phi) &= \cot X_c + \tan(X_c + \phi), \\
D_2(X_c, \phi) &= \cot X_c + \frac{\cot^2 X_c}{\cot \phi}
\end{align*}
\]

where \( z(\theta) \) is the wheel sinkage, which is geometrically given as a function of contact angle \((z(\theta) = r(\cos\theta - \cos\theta_f))\), \( R_b \) is the passive terrain resistance with unit width, due to the shear motion acting on the side face of the wheel, \( X_c \) is the destructive angle \((X_c = 45^\circ - \phi/2)\), and \( \rho \) is the terrain bulk density [37]. In particular, among these forces/torques in Equation (18), the drawbar pull \( x-\)}
directional force) $F_x$ can directly affect the tractive capability of the rover on a flat/inclined deformable terrain, whereas the torque $T_y$ in the y-direction is the resistance torque required for the wheel to rotate, which is associated with the power consumed.

Figure 6. The wheel-terrain interaction model: (a) stress distribution and conventional force/torque model of the wheel on deformable terrain; (b) model of the grouser wheel on deformable terrain showing the normal stress components at grouser tip and concave portion.

3.2. MBD Simulation Tool Based on Wheel-Terrain Interaction

The MBD simulation tool based on the wheel-terrain interaction (introduced in the previous subsection) is developed to deal with the motion behavior of the body and wheel of the rover. In this study, it is used to provide a quantitative relationship between the tractive capability and power consumed according to the various design parameters, as described in Sections 4 and 5. The overall MBD simulation architecture is composed of the major three steps from Algorithm 1. First, the forces and torques of each wheel, corresponding to the kinematic parameters (mass, wheel parameters, obtained state of the slip ratio/angle, etc.) of the rover, are calculated based on the wheel-terrain interaction and via a Matlab function, which is used for the Newton’s method. These external forces/torques coincide with the longitudinal direction through coordinate system transformations according to the normal direction of the inclined terrain and/or wheel rotation angle. To obtain the forward dynamic solution of Equation (22), such as the position, orientation and velocities of the rover, the dynamic model of the exploration rover is then solved using Matlab/SIMULINK (Natick, MA, United States):

$$
\begin{bmatrix}
H_0 & H_{01} & \cdots & H_{0m_1} \\
H_{01}^{T} & H_{11} & \cdots & H_{1m_1} \\
\vdots & \vdots & \ddots & \vdots \\
H_{0m_k} & H_{m_11} & \cdots & H_{m_km_k}
\end{bmatrix}
\begin{bmatrix}
\dot{x}_0 \\
\dot{\theta}_1 \\
\vdots \\
\dot{\theta}_k
\end{bmatrix}
+ 
C + G
= 
\begin{bmatrix}
F_0^T \\
\tau_1 \\
\vdots \\
\tau_n
\end{bmatrix}
+ 
\begin{bmatrix}
F_{ex}^T \\
F_{ex1}^T \\
\vdots \\
F_{exk}^T
\end{bmatrix},
$$

(22)

where the symbols are

- $k$: number of limbs,
- $H_{0,0m_1,\ldots,m_k}$: inertia matrices for the entire system composed by the inertia property of each body,
- $x_0 \in \mathbb{R}^6$: position/orientation of the base body,
- $\varphi = (\varphi_1^T, \ldots, \varphi_k^T)^T \in \mathbb{R}^n$: articulated joint angles,
- $C$: non-linear velocity-dependent term,
- $G$: gravity term,
- $F_0^T = (0,0,-m_0g)$: forces exerted on the base body,
\[
\tau = (\tau_1^T, \ldots, \tau_k^T)^T \in \mathbb{R}^n \\
F_{ex} = (F_{ex1}^T, \ldots, F_{exk}^T)^T \in \mathbb{R}^{6k} \\
F_{ex,i} = (F_{wi1}^T, \ldots, F_{wi2}^T)^T [38].
\]

### Algorithm 1 Multi-Body Dynamics Simulation Based on the Wheel-Terrain Interaction Model

1: Input the operational environment parameters (digital elevation model map, normal surface vector for terrain inclination, terrain properties) and kinematic parameters (geometry of the rover, mass, and inertias, wheel parameters, steering angles, and angular velocities on each wheel).
2: Set the initial state to be used in the first iteration.
3: Update the steering angle, normal load, position, orientation, slip ratio, and slip angles with the obtained dynamic states from the last iteration.
4: Calculate forces/torques acting on each wheel based on wheel-terrain interaction model.
5: Obtain as output the forces and torques acting on the system.
6: Solve Equation (22), then obtain the rover’s positions, orientation, and velocities.
7: Calculate the tractive coefficient, power acquisition, consumed power in accordance with the rover’s kinematic states and appropriate desired steering angles to follow the predefined straight path in the given terrain condition. Return to Step 3 and iterate.

In general, the rover gradually accelerates in the lateral direction when it traverses inclined deformable terrain. That is, the required resistance torque underneath the wheel increases along with the skid behavior of the rover. It may be the intensive factor for wheel designs on a given terrain inclination. However, these side-slipping scenarios mainly depend on steering maneuvers in accordance with a predefined path via path planning rather than based on the influence of the wheel design. As a result, this is an important factor to consider in the path planning problem through the use of slippage compensation control strategies. This argument has been reported in previous researches [14,39–41]. Comprehensive treatment of this subject, however, is beyond the scope of this study. Thus, in all of our computational procedures, the rovers must traverse along a predefined path (straight line) with lateral slippage compensation control strategy, determined using the previously proposed path planning/following techniques for the given digital elevation map. More frameworks on this topic are discussed in more detail in a previous publication by the author [42].

### 4. Multi-Objective Design Optimization Based on Operational Environment in Accordance with the Target Landing Site

#### 4.1. System-Level Requirements

A lunar exploration mission is dependent on the target landing sites, terrain types, and mission duration. In addition, the exploration rover is also limited in its capability by the electrical energy provided by the solar panels, rover mass, wheel size due to the allowable payload, and so forth. For these reasons, determining the system-level requirements starts with the optimal design of the wheel shape for the exploration rover. To satisfy the specification of the solar-powered exploration rover in terms of the operational environment, the requirements of the system level, including the mass and velocity of the rover, and the mission duration, should be discussed. These requirements are determined based on the rovers outlined in Table 2 [43–46]. We determine the mission and system requirements for the ongoing Korea lunar exploration mission according to the prior development of rovers, as shown in Figure 7. Furthermore, this rover is equipped with scientific instruments and a thermal control system and the motor characteristics and/or gear train of the rover are supposed to work under an optimum condition. In addition, the rover in this study has a four-wheel drive and rigid wheels. Thus, this rover does not consider the influence of the gear train, motor state, and contact patch according to the wheel stiffness. The slip ratio of $\lambda = 0.2$ was chosen as reported by
previous research [45]. The mission duration of the rover is 14.75 days; Figure 7 describes all the system-level requirements.

Table 2. Previously developed rovers for lunar exploration missions.

|                                      | Lunokhod | Yutu | Polaris (Google Lunar X Prize) | Moonraker (Google Lunar X Prize) | Amalia (Google Lunar X Prize) |
|--------------------------------------|----------|------|--------------------------------|---------------------------------|-----------------------------|
| Mass (kg)                            | 840      | 140  | 149                            | 8.424                           | 30.9                        |
| Mission location (°)                 | Sea of Rains (38.24° N, 35.01° W) | Lacus Mortis (44.95° N, 26.61° E) | Moon’s Pole | Marius Hills (13.0° N, 306.2° E) | Lunar Equatorial |
| Mission timeline (day)               | 116      | 90   |                                |                                 | 14                          |
| Thermal control (-)                  | Yes      | Yes  | Yes                            |                                 |                             |
| Considered temperature range (°C)    |          |      | -180 to +50                    | to +120                         | to +200                     | -150 to +150               |
| Power source (-)                     | Solar    | Solar + Pu238                   | Solar                          | Solar                          | Solar                       |
| Solar cell area (m²)                 | 4        | 3.6  | 0.22                           | 0.36 or more                    |                             |
| Power consumption (W)                | 50       | 30   | 250                            | 24.2                            | 73.8                        |
| Number of wheels (-)                 | 8        | 6    | 4                              | 4                               | 4                           |
| Suspension type (-)                  | Custom-machined rocker-bogie | Rocker-bogie | Passive rocker | Roll axis swing arms | Roll axis swing arms |
| Maximum speed (m/s)                  | 0.22     | 0.056 | 0.41                          | 0.045                           | 0.014                       |
| Distance (km)                        | 39       | 0.1  |                                |                                 |                             |

Figure 7. System-level requirements for the Korean solar-powered exploration rover.

Additionally, the selected target landing site includes the Imbrium basin and its surrounding areas. These sites are located at 44.12° north of the lunar equator and were recently explored by China’s Chang’E-3 and Yutu rover mission. In addition, the Imbrium basin and its surrounding areas
are plane topography without any large impact craters, with slopes of no more than 7°, and an average slope of 2.2° [47]. Furthermore, the effects of power acquisition due to the seasonal behavior in accordance with the X-day (planned schedule for the landing on the Moon) and various target landing locations are analyzed in Section 5.

4.2. Wheel Parameter Sensitivity Analysis via Developed MBD Simulation Tool

In general, long-term experiments in accordance with the various conditions and/or designer experience are highly necessary for design improvements. However, these approaches require a large amount of time consumed and great effort from the designers. To overcome this problem, the problem of how to choose the dominant parameters influencing both drawbar pull (related to the trafficability) and power consumed (affected by the wheel resistance torque) is solved via sensitivity analysis in dynamic state based on the five scenarios (traversing/climbing/descending state of the flat or inclined deformable terrain), as shown in Figures 8 and 9. Prior to the optimization procedure, the ultimate objective of this approach in this subsection is to provide the design guidelines for designers related to the dominant design parameters based on the relative importance of each wheel geometry parameter (wheel radius, wheel width, lug height, lug width, and lug spacing) in accordance with the rate of change in the result of the MBD dynamic system response [48]. In this set of sensitivity analysis, the wheel radius is varied from 0.0005 to 0.5 m as a discrete uniform distribution, the wheel width is varied from 0.001 to 1 m as discrete uniform distribution, the wheel lug height and width are varied from 0.001 to 0.1 m, and 0.001 to 0.1 m, respectively, as discrete uniform distributions, and the wheel lug spacing is varied from 0 to 90° as discrete uniform distribution.

\[ \text{Equation} \]

**Figure 8.** Overall configuration of the proposed sensitivity analysis/multi-objective optimization procedure for solar-powered exploration rover wheel. Non-dominated Sorting Genetic Algorithm-II
(NSGA-II): (a) framework of the optimization procedure for wheel design of exploration rover; (b) data flow for realization of optimization method.

![Driving environments during sensitivity analysis and multi-objective optimization](image)

Figure 9. Driving environments during sensitivity analysis and multi-objective optimization and its various dynamic state modes.

Given the developed MBD simulation environment, Figure 10 shows the sensitivity of system dynamic response for the drawbar pull (related to the tractive coefficient and/or tractive capability) and power consumed (affected by the wheel resistance torque) on various wheel geometry configurations under the various driving environments. These results reveal that the drawbar pull and power consumed are more sensitive to the wheel radius and wheel width, rather than to the wheel lug height, lug width and lug spacing under the five dynamic states. As a result of this sensitivity analysis, the wheel radius and wheel width are chosen as the dominant design parameters in the multi-objective optimization method.

![Sensitivity analysis results with respect to wheel geometry parameters](image)

Figure 10. Sensitivity analysis results with respect to wheel geometry parameters: (a) parameter influence for the drawbar pull according to various aspects of wheel geometry; (b) parameter influence for the power consumed according to various aspects of wheel geometry.
4.3. Multi-objective Wheel Design Optimization

4.3.1. Objective Function

Optimization procedures coupling the MBD simulation tool based on the wheel-terrain interaction and optimization algorithm are developed, as shown in Figure 8. The goal is to determine the optimal rover wheel in terms of the operational environment under the condition of limited power, along with the maximal tractive coefficient (affected by the drawbar pull) $F_s/F_z$. For the supply and demand of the power condition on the Moon $P_{obt}$, we use Equation (4) to estimate the obtained power given the target landing site. Additionally, the parameter “power consumed $P_{used}$” is a function of the wheel resistance torque, wheel speed, motor efficiency $\eta_{motor}$, and avionics power consumption $P_{avionics}$. In addition, when the power condition referred to as the power margin $(P_{obt} - P_{used})$ has a negative value, the exploration rover suffers from a lack of power given the target landing site. Thus, in this process, the objective functions for tractive coefficient $f_{1M}$ and power margin condition $f_{2M}$ are defined as:

$$\text{Find } d \in R^n, \ d = \{d_1, r, d_2, b\} \text{ that}$$

$$f_{1M}(d_{1,2}) = \min (-F_s/F_z)$$

$$f_{2M}(d_{1,2}) = \min (-P_{obt} + P_{used})$$

$$P_{used} = \left[v_T^2T_P/\eta_{motor}r(1 - \lambda)\right] + P_{avionics}$$

4.3.2. Constraint Condition

The design parameters derived through the sensitivity analysis are the wheel radius $r$ and wheel width $b$. In addition, based on the information from previous studies and/or development of exploration rovers as shown in Figure 11 [11,12,14,18,25,38,39,45–66], we derive some relevant inequality constraint conditions, as in Equation (24). Previous or on-going development of exploration rovers provide insight into determining the consideration range for realistic wheel parameters, which can reduce the computational load for calculating the optimal results.

$$\text{s.t. } g_{1,2}(d_{1,2}) \leq 0$$

$$g_{1}(d_{1,2}) = b/(2 + r) \geq 0.438$$

$$g_{2}(d_{1,2}) = b/(2 + r) \leq 0.75$$

$$l_b \leq d_{1,2} \leq u_b$$

$$l_b = [0.01; 0.01], \ u_b = [0.15; 0.2]$$

4.3.3. Optimization Algorithm

In the current work, the non-dominated sorting genetic algorithm-II (NSGA-II), which uses an elitist principle and explicit diversity preserving mechanism, is applied in the proposed multidisciplinary optimization of rover wheel design for all dynamics states on the flat/inclined
deformable terrain. The NSGA-II is chosen here because of its simplicity, high efficiency, excellent diversity preserving mechanism, and fast convergence near the true Pareto-optimal set, which can reduce the computational complexity and improve accuracy based on an elitist mechanism [67–71]. The main parameters of NSGA-II involve the population size $N$, number of generations, crossover probability, mutation probability, crossover distribution index and mutation distribution index. In the current optimization procedure, the population size is set to 50, and 10 generations are formulated to obtain the Pareto front. Moreover, the probabilities for the crossover and mutation are 0.9 and 0.04, respectively, whereas the distribution indexes for crossover and mutation are 25 and 25, respectively [67–71].

4.3.4. Optimization Procedure

The detailed multi-objective optimization design procedure combining MBD simulation environment based on the wheel-terrain interaction and NSGA-II is as follows: First, the main parameters of NSGA-II, MBD simulation tool, and initial values of design parameters such as $r$ and $b$, which are shared to the design parameters space of the optimization process, are input. Second, the average tractive coefficient $f_{1M}$ and average power margin $f_{2M}$ are solved using the MBD simulation tool under the developed operational environment, and the results are sent to the design objective space of the optimization process. Third, the optimization algorithm decides on the design parameters of the next group, for the generation and feedback of the MBD simulation tool. This set is actuated by the optimization algorithm and does not stop until the preset generation number is finished, as shown in Figure 9. In addition, total optimization process requires almost 10 – 12 h to be finished at an Intel® Core™ i9-7900X CPU @ 3.30 GHz and 32 GB of RAM.

4.3.5. Results of Multi-Objective Optimization Problem with NSGA-II

After the operating parameters and processing constraint condition are configured, the design variables are optimized via NSGA-II. Figure 12 shows a Pareto front that can be an optimal solution at any point. From this figure, it should be noted that maximizing the tractive coefficient (related to the drawbar pull) will minimize the power margin (affected by the wheel resistance torque) under the design constraints [72]. In this study, an optimal design is to be created from the maximal tractive coefficient without extreme solutions, as marked in Figure 12. For the chosen optimized design parameter, the wheel radius is 0.06 m, the wheel width is 0.10 m, and the calculated corresponding values of the objective functions are 0.476 and 1.776 W, respectively. At the same time, the arbitrary initial design parameter for wheel radius and wheel width are 0.045 and 0.06 m, and the calculated corresponding values of the objective functions are 0.379 and 1.919 W, respectively. As a result, when the proposed design methodology is applied to determine the wheel geometric parameter of the rover, the tractive coefficient $f_{1M}$ has been enhanced significantly compared to that of the initial design under the limited power condition. Although the power margin $f_{2M}$ has been decreased on the preferred design parameter, it can still meet the design requirements (such as the constraint conditions) for the restricted power condition.
5. Performance Evaluation of Optimal Design Results

5.1. Case Study: Scenario-Based Dynamic Analysis

5.1.1. Rover Model and MBD Simulation Procedure

Prior to conducting the experimental verification, dynamic simulation using the MBD simulation based on the wheel-terrain interaction is conducted to demonstrate the practicality of the derived optimal wheel. In addition, from a realistic point of view, the power condition and tractive coefficient of the rover can be directly affected by several characteristics, e.g., terrain configuration induced the inclined angle, inertia of the rover body. Up to this point, these effects are considered as parts of the scenario. For the MBD simulation purposes, the three scenarios comprise flat and inclined deformable terrain in the longitudinal/lateral direction in the case of the landing site at latitude 44.12° N, as shown in Figure 13a. In these case studies, when each rover is driven over the flat and/or inclined deformable terrain at the latitude 44.12° N, these example case studies have to be guaranteed to represent the advantages of the preferred optimal wheel type compared to the others, as shown in Figure 13b. In addition, the rover testbed models equipped with the optimal wheel type and arbitrary initial (nominal) wheel types from the comparison group, which covers all physical properties of the rover related to its inertia, dimensions and mass, are established from the system-level requirements for the Korean exploration rover platform, as mentioned in Section 4, and the other previously developed rover models [12,14,39,54].
Figure 13. Schematic of scenario-based dynamic analysis for verifying the performance of optimal wheel type: (a) simulation scenarios; (b) different rover structure and wheel configurations.

5.1.2. Case Study: Driving over Flat Deformable Terrain

Here, the simulation results from the first example case study are represented. Figure 14 shows a visualization of the simulation results, where each solar-powered exploration rover drove over the flat deformable terrain in the case of the landing site at latitude 44.12° N. On the other hand, Figure 15 depicts the maximum sinkage, maximum tractive coefficient, wheel torque, and power margin of the rover on the flat terrain: for the rover equipped with the arbitrary initial (nominal) wheel design (represented as a black block), for the rover equipped with the preferred optimal wheel design (represented as a red block), for the rover-1 (represented as a green block), and for the rover-2 (represented as a yellow block).

Figure 14. Illustration of paths for various rover testbed models, where each rover drives over flat deformable terrain in the case of landing site at 44.12° N.
Figure 15. Simulation results from scenario-based dynamic analyses for three defined scenarios (flat deformable terrain, 2.2° longitudinal inclined deformable terrain, and 2.2° side slope deformable terrain) at landing site of latitude 44.12° N: (a) sinkage; (b) tractive coefficient; (c) wheel torque; (d) power margin.

According to Figures 14 and 15, the trajectory of the rover-1 type, which has the highest value of the tractive coefficient, is longer than the trajectories of the other rovers. However, this rover has several potential concerns given the condition of lack of power, as shown in Figure 15d. According to Figures 14 and 15, the rover equipped with the optimal wheel type is able to save energy,
demonstrate a relatively long exploration range, enhanced driving performance and reduces any mobility hazard (such as a stuck wheel because of wheel sinkage) better than the other rovers over the same mission duration. This is because the optimal wheel achieved the smallest sinkage/resistance wheel torque values and, at the same time, relatively higher tractive coefficient than those of the other rovers under the restricted obtained power. Thus, the results given by Figure 15 show that this result is explicitly as expected: the rover equipped with the optimal wheel has the best performance in terms of decreasing the accumulated sinkage, enhancing the tractive coefficient, decreasing the resistance wheel torque and satisfying the power margin under the power condition on the flat deformable terrain. As a result, the rover equipped with the optimal wheel type has a relatively wider range of travel distance (related to the exploration area) than those of the other rovers over the same mission duration.

5.1.3. Case Study: Climbing Slope at 2.2° Inclined Terrain

The trajectories of the bodies of rovers, given a 2.2° inclined terrain at the latitude 44.12° N, are shown in Figure 16, which reveal the climbing ability of the optimal wheel type compared to those of specific wheel types under the restricted power condition. Furthermore, Figures 15 and 16 clearly show that the optimal wheel type was expected to perform much better than the others on both the flat and 2.2° inclined terrain in the longitudinal direction, when the total power condition induced consumed power (affected by the wheel resistance torque) meets the restricted obtained power. However, as the rover moves on the inclined terrain, an axle load movement is observed between the front and rear wheels. This phenomenon leads to a reduction in the drawbar pull/resistance wheel torque of the front wheel. As a result, the tractive coefficient and wheel torque are slightly decreased, as shown in Figure 15b,c.

![Figure 16. Illustration of paths for various rover testbed models, where each rover climbs 2.2° inclined deformable terrain in the case of the landing site at 44.12° N.](image)

5.1.4. Case Study: Traversing on 2.2° Side Slope

The final case study was conducted to determine the slope traverse ability in a lateral direction, with the path following control strategies (straight line) with lateral slippage compensation. When traversing the 2.2° side slope deformable terrain at the latitude 44.12° N, the trajectories of the bodies of rovers are illustrated in Figure 17. The performance of the rover equipped with the optimal wheel type was also observed to perform better than the other rovers for all the scenario cases under the restricted power condition. In addition, Figure 15d shows that the power margin for both the 2.2° inclined terrain and side slope scenarios were greater than those on the flat terrain. This is because the solar radiation condition is also affected by the spatial relationship between the Sun and the attitude of the rover, and the solar zenith angle. Finally, based on these simulation cases, the derived
optimal wheel type has relatively enhanced driving performance in terms of factors such as exploration range, sinkage, wheel resistance torque and tractive coefficient, as much as those of the comparative group, under the limited energy condition.

![Image showing paths for various rover testbed models](image)

**Figure 17.** Illustration of paths for various rover testbed models, where each rover travels 2.2° side slope in the case of landing site at 44.12° N.

5.2. Experimental Verification

5.2.1. Single Wheel Testbed and Experimental Procedure

To validate the optimal design results and MBD simulation tool based on the wheel-terrain interaction model utilized for the proposed design methodology, we designed and constructed a single wheel testbed for the exploration rover wheel, consisting of the driving part and a sensing part that precisely measures the various relevant parameters including the sinkage, tractive coefficient, driving resistance wheel torque and slip ratio, as shown in Figure 18. Furthermore, the initial wheel, optimal wheel and rover-1 type were designed and prepared. The lunar simulant used in the experimental verification fills the soil bin of the single wheel testbed, occupying nearly 60% ($\gamma = 1.756 \text{ ton/m}^3$) of the relative density. To maintain the desired slip ratio for the driving part of the testbed, a closed-loop control system, called a PID control, was configured with two motors and encoders. Thus, experimental verifications were conducted with the different wheel geometries (for the optimal wheel, initial wheel and rover-1 type) and different axle loads, using the testbed on the lunar simulant to measure the sinkage, driving resistance torque, and tractive coefficient according to the variations in the slip ratio ($\lambda = $ about 0.33, 0.5 and 0.74). In addition, our experiments were repeated thrice under the same experimental condition.
5.2.2. Results of Experimental Verification

Figures 19–21 plot the simulation results for the sinkage, tractive coefficient and driving resistance torque against experimental data with standard deviation bars. Figure 19 shows curves for the sinkage versus the slip ratio for initial wheel \((r = 0.045 \text{ m}, b = 0.06 \text{ m with } F_x = 48 \text{ N})\), optimal wheel type \((r = 0.06 \text{ m}, b = 0.10 \text{ m with } F_x = 48 \text{ N})\) and rover-1 type \((r = 0.09 \text{ m}, b = 0.11 \text{ m with } F_x = 80 \text{ N})\). As shown in Figure 19, the experimental results reveal that the sinkage gradually increases as the slip ratio is increased, showing a similar trend to previously reported research [61]. In addition, from the experimental and simulated sinkage results, the initial wheel and rover-1 type compared to the preferred optimal wheel type, had increasingly penetrated the lunar simulant in the range of slip ratio from 0.33–0.74. In particular, the measured sinkage of the optimal wheel type is \(-70.6\%\) relative to that of the initial wheel type (12.01→3.533 mm) when the slip ratio \(\lambda = 0.5\). Therefore, this result indicates that employing the multidisciplinary wheel design methodology presented in this study can reduce immobility due to the wheel getting stuck in the terrain.
Figure 19. Comparison of experimental and simulated results for sinkage according to slip ratio and different axle load, for initial wheel type, optimal wheel type, and rover-1 type.

Figure 20. Comparison of experimental and simulated results for tractive coefficient according to slip ratio and different axle load, for initial wheel type, optimal wheel type, and rover-1 type.
Figure 21. Comparison of experimental and simulated results for driving resistance torque according to slip ratio and different axle load, for initial wheel type, optimal wheel type, and rover-1 type.

Figure 20 shows the measured and predicted tractive coefficients according to the different wheel geometries and axle loads in the various slip ratio condition. In this figure, the rover-1 type compared to the others is seen to demonstrate an enhanced tractive coefficient. Thus, this wheel type can be more efficient at traversing, but requires significantly higher driving resistance torque, which also leads to a higher power consumption from Equation (23). Owing to this, the measured tractive coefficient of the rover-1 type is +830% relative to that of the initial wheel type (0.0434 → 0.404), and the measured driving resistance torque of rover-1 type is +257.9% relative to that of the initial wheel type (0.859 → 3.074 Nm) when the slip ratio $\lambda = 0.5$, as shown in Figures 20 and 21. These verification results reveal that it may be possible for the power consumed to gradually increase as the driving resistance torque from Equation (23) is increased to enhance the trafficability. If amounts of energy from its limited energy source and/or torque limit of a wheel driving motor are not high, this is a tremendous drawback for solar-powered lunar/planetary exploration mission.

Meanwhile, the measured tractive coefficient of the optimal wheel type is +398.2% relative to that of the initial wheel type (0.0434 → 0.216), and the measured driving resistance torque of the optimal wheel type is −59.7% relative to that of the rover-1 type (3.074 → 1.24 Nm) when the slip ratio $\lambda = 0.5$, as shown in Figures 20 and 21. From these evaluations, the preferred optimal wheel type can be chosen as the most appropriate wheel type between these two candidate wheel types, because this wheel type has a relatively improved tractive coefficient, comparable to that for the initial wheel type and requires less power consumption (affected by the driving resistance torque) comparable to that of rover-1 type.

In addition, as shown in Figures 20 and 21, the experimental results reveal that the tractive coefficient and driving resistance torque gradually increase as the slip ratio is increased, showing a slightly similar tendency to that estimated using the numerical results. However, in the simulation results, the driving resistance torque and tractive coefficient are greater than those obtained via the single wheel testbed experiments. One of the main reasons for the difference is that the wheel-terrain interaction model utilized for the multi-objective optimization procedure assumes terrain characteristics for a homogeneous composition, rather than for the non-homogeneous composition in actual driving conditions. This trend has also been reported in previous researches [50,61,73]. To overcome this problem, several techniques could be made to enhance the accuracy of the wheel-terrain interaction model. For example, a modified pressure-sinkage model considering the effect of the wheel curvature could be more reduced for the sinkage and compaction resistance model errors [74]. Further, a stochastic identification of terrain parameters via a Bayesian approach can be
employed to accurately estimate the wheel-terrain interaction phenomena [75]. Lastly, alternate numerical analysis using the discrete element method could be also utilized to model the displacement and velocity of non-homogeneous soil particle [61,76]. Thus, more reliable numerical analysis model is required, which will be investigated further in future studies. However, these are beyond the scope of this paper.

5.2.3. Discussion

In this study, modeling of power acquisition and terrain characteristics as an operational environment, wheel design, MBD simulation, and experimental verification were conducted to determine the optimal wheel dimensions of solar-powered exploration rover for different terrain compositions given the target landing site. For experimental verification, although differences exist between the experimental and simulation results, the three results exhibit similar tendencies. This result indicates that the simulation model for the MBD simulation can be used for the proposed wheel design approach in the development stage. Furthermore, the results of experimental verification on the sinkage, driving resistance torque and tractive coefficient reveal that the proposed wheel design methodology is a reliable and effective approach.

In this study, conditions in terms of vacuum, thermal characteristics, mission schedule and complex terrain configuration (i.e., rock, undulating terrain and numerous obstacles) were negligible. However, in real scenarios, these conditions have to be considered. Thus, we will conduct these problems in our future work. Despite these limitations, the proposed wheel design methodology can be used at the conceptual design stage to determine the initial selection of the wheel shape of the exploration rover in accordance with each target landing site among a large variety of candidate design points. This wheel design approach can also enhance the coverage of vast regions far from a landing site.

Moreover, in the last decades, the complexities of the exploration rover and the energy demands of the experimental apparatus, avionics, and locomotion parts have increased [30]. Thus, we discuss here the power produced by solar energy on the Moon as a function of target landing site and seasonal behavior to provide some guideline for energy requirement in the future exploration missions. The maximum energy provided by the solar energy is presented in Figure 22. For solar energy, we have assumed a solar cell efficiency of $\eta = 27\%$, which is the maximum solar cell efficiency commercially obtained nowadays under a best-case scenario, and a solar panel area of 1 m$^2$. In Figure 22, we see that the obtainable power is between 51.670 and 357.072 W. According to an analysis of the values shown in Figure 22, this energy map on the Moon is useful for the decision-making process in analyzing the energy demands with consideration for the locomotion, avionics and experimental apparatus, among others, according to the target landing location and seasonal behavior of X-day.

**Figure 22.** Solar energy map for a 1 m$^2$ solar panel with $\eta = 27\%$, as a function of latitude and season on the Moon.
6. Conclusions

This research proposed a multidisciplinary design methodology that can provide an optimized design parameter among a large variety of eligible wheel designs, considering the operational environment and dynamic behavior of exploration rover. The four major accomplishments of this research are as follows:

- The modeling of solar power acquisition and terrain characteristics on the Moon for predicting the operational environment of the exploration rover is described.
- An MBD simulation tool based on the rover wheel-terrain interaction model, which can deal with the longitudinal/lateral dynamic behavior of exploration rover with the path following the controller on a given digital elevation model map, is developed.
- Via sensitivity analysis with regard to the wheel geometry parameters, the dominant design parameter is selected, and a multi-objective wheel design optimization method integrating the developed simulation environments and NSGA-II is created over various dynamic states. With the use of MOOP based on the NSGA-II method, an optimal design point that considers both tractive coefficient and energy consumption is obtained.
- Numerical/experimental verification using a single wheel testbed were conducted to convincingly validate the derived optimization results and simulation environment. Compared to the comparative group, the optimized design was enhanced by −71% in terms of the sinkage (associated with the immobility), +308% in terms of tractive coefficient (associated with the tractive capability), and −57.9% in terms of the driving resistance torque (associated with power condition).

These results show that the proposed wheel design methodology can be used in the initial selection of the most appropriate robot wheel, i.e., to decide on the optimal design points for a given operational environment. Moreover, with this design approach, a quick comparison of the performance indexes to provide design recommendations is made possible, thus reducing the time consumed in otherwise conducting a detailed evaluation of a very large number of alternatives during the development stage.

Still, some problems remain to be solved. First of all, our future study will focus on developing a more reliable model to account for the effect of non-homogeneous terrain composition. In addition, assuming that the wheel radius is set, the minimum and maximum grouser height can be geometrically derived. Thus, basic wheel dimensions are decided first. Therefore, the results reported here will be used for a food starting point during the grouser shape design stage in the future work. Finally, four wheeled rover testbeds equipped with the derived optimal wheel according to the various driving condition will be carried out in order to analysis and derive an appropriate wheel control strategy.

Author Contributions: Conceptualization, K.-J.K. and K.-H.Y.; methodology, K.-J.K.; validation, K.-J.K. and K.-H.Y.; investigation, K.-J.K.; data curation, K.-J.K.; writing—original draft preparation, K.-J.K. and K.-H.Y.; writing—review and editing, K.-J.K. and K.-H.Y.; visualization, K.-J.K.; supervision, K.-H.Y. All authors have read and agreed to the published version of the manuscript.

Funding: This research was funded by the National Research Foundation of Korea, grant number 2016M1A3A3A02018194, grant number NRF-2017R1D1A1B03029381, grant number 2020R1A6A3A01100324, and this work was also partially supported by the Jeonbuk National University.

Acknowledgments: The authors are thankful to the Korea Institute of Civil Engineering and Building Technology for their provision of the lunar simulant and the related technical information.

Conflicts of Interest: The authors declare no conflict of interest.

References

1. Weathered, C.J.; Bendix Corporation; Bendix Systems Division. Lunar Surface Mobility Systems Comparison and Evolution (MOBEV). Final Presentation Report, NASA-CR-92641; University of Michigan Press: Ann Arbor, MI, USA, 1966.
2. Zhang, P.; Deng, Z.; Hu, M.; Gao, H. Mobility Performance Analysis of Lunar Rover Based on Terramechanics. In Proceedings of the Advanced Intelligent Mechatronics, AIM 2008, IEEE/ASME International Conference on IEEE, Xi’an, China, 2–5 July 2008; pp. 1201–1225.

3. Taghavifar, H.; Mardani, A. Evaluating the effect of tire parameters on required drawbar pull energy model using adaptive neuro-fuzzy inference system. Energy 2015, 85, 5865–5893.

4. Bekker, G. Theory of Land Locomotion: The Mechanics of Vehicle Mobility; University of Michigan Press: Ann Arbor, MI, USA, 1956.

5. Bekker, G. Introduction to Terrain-Vehicle Systems; University of Michigan Press: Ann Arbor, MI, USA, 1969.

6. Wong, J.; Reeco, A.R. Prediction of rigid wheel performance based on analysis of soil-wheel stresses, Part I. Performance of driven rigid wheels. J. Terramechanics 1967, 4, 81–98.

7. Wong, J.; Reeco, A.R. Prediction of rigid wheel performance based on analysis of soil-wheel stresses, Part II. Performance of towed rigid wheels. J. Terramechanics 1967, 4, 7–25.

8. Wong, J. Theory of Ground Vehicles, 4th ed.; John Wiley & Sons: Hoboken, NJ, USA, 2008.

9. Wong, J. Terramechanics and Off-Road Engineering, 2nd ed.; Elsevier: Amsterdam, The Netherlands, 2010.

10. Ding, L.; Yang, H.; Gao, H.; Li, N.; Deng, Z.; Guo, J.; Li, N. Terramechanics-based modeling of sinkage and moment for in-situ steering wheels of mobile robots on deformable terrain. Mech. Mach. Theory 2017, 116, 14–33.

11. Iagnemma, K.; Kang, S.; Brooks, C.; Dubowsky, S. Multi-Sensor Terrain Estimation for Planetary Rovers. In Proceedings of the 8th International Symposium on Artificial Intelligence, Robotics, and Automation in Space, I-SAIRAS, Nara, Japan, 19–23 May 2003.

12. Ishigami, G.; Miwa, A.; Nagatani, K.; Yoshida, K. Terramechanics-based model for steering maneuver of planetary exploration rovers on loose soil. J. Field Robot. 2007, 24, 233–250.

13. Iagnemma, K.; Kang, S.; Shibly, H.; Dubowsky, S. Online terrain parameter estimation for wheeled mobile robots with application to planetary rovers. IEEE Trans. Robot. 2004, 20, 921–927.

14. Ishigami, G. Terramechanics-Based Analysis and Control for Lunar/Planetary Exploration Robots. Ph.D. Thesis, Tohoku University, Sendai, Japan, 2008.

15. Filippo, D. Design and Analysis of Rover Wheel Testbed. Ph.D. Thesis, University of Oklahoma, Oklahoma, OK, USA, 2009.

16. Imani, R. Dynamic Terramechanic Model for Lightweight Wheeled Mobile Robots. Ph.D. Thesis, The University of Dalhousie, Halifax, NS, Canada, 2011.

17. Chen, B.; Wang, R.; Jia, Y.; Guo, L.; Yang, L. Design of a high performance suspension for lunar rover based on evolution. Acta Astronaut. 2009, 64, 925–934.

18. Leite, A.C.; Schäfer, B. Mass, Power and Static Stability Optimization of a 4-Wheeled Planetary Exploration Rover. In Proceedings of the 2nd International Conference on Engineering Optimization, Lisbon, Portugal, 6–9 September 2010.

19. Ebrahimi, S.; Mardani, A. Expanding scissor-based UGV for large obstacles climbing. Mech. Based Des. Struct. Mach. 2019, 47, 20–36.

20. Mardani, A.; Ebrahimi, S.; Alipour, K. 6AP wheel: A new transformable robotic wheel for traction force improvement and halting avoidance of a UGV on soft terrains. Mech. Based Des. Struct. Mach. 2020, doi:10.1080/15397734.2020.1807360.

21. Sim, B.-S.; Kim, K.-J.; Yu, K.-H. Development of Body Rotational Wheeled Robot and Its Verification of Effectiveness. In Proceedings of the 2020 IEEE International Conference on Robotics and Automation (ICRA), Paris, France, 31 May 2020.

22. Alamdar, A.; Krovi, V.N. Design of articulated leg-wheel subsystem by kinetostatic optimization. Mech. Mach. Theory 2016, 100, 222–234.

23. Kim, D.; Hong, H.; Kim, H.S.; Kim, J. Optimal design and kinetic analysis of a stair-climbing mobile robot with rocker-bogie mechanism. Mech. Mach. Theory 2012, 50, 90–108.

24. Papantoniou, V.; Skevakis, A.; Katelouzos, A.; Papantoniou, A.; Kapellos, K.; Papadopoulos, E.; van Winnendael, M. Use of Flexible Variable Stiffness Wheels for High Speed Lunar and Planetary Exploration Rovers and Their Potential Impact on the Cost of Transport. In Proceedings of the I-SAIRAS, Pasadena, CA, USA, 18–21 October 2020.

25. Sutob, M.; Yusa, J.; Ito, T.; Nagatani, K.; Yoshida, K. Traveling performance evaluation of planetary rovers on loose soil. J. Field Robot. 2012, 29, 648–662.
26. Ding, L.; Gao, H.; Li, Y.; Liu, G.; Deng, Z. Improved explicit-form equations for estimating dynamic wheel sinkage and compaction resistance on deformable terrain. *Mech. Mach. Theory* **2015**, *86*, 235–264.
27. Ghotbi, B.; González, F.; Kövecses, J.; Angeles, J. Mobility evaluation of wheeled robots on soft terrain: Effect of internal force distribution. *Mech. Mach. Theory* **2016**, *100*, 259–282.
28. Baratta, M.; Genta, G.; Laurenzano, D.; Misul, D. Exploring the surface of the Moon and Mars: What kind of ground vehicles are required? *Acta Astronaut.* **2019**, *154*, 204–213.
29. Edwin, L.E.; Denhart, J.D.; Gemmer, T.R.; Ferguson, S.M.; Mazzoleni, A.P. Performance analysis and technical feasibility assessment of a transforming roving-rolling explorer rover for Mars exploration. *J. Mech. Des.* **2014**, *136*, doi:10.1115/1.4027336.
30. Delgado-Bonal, A.; Martín-Torres, F.J.; Vázquez-Martín, S.; Zorzano, M.P. Solar and wind exergy potentials for Mars. *Energy* **2016**, *102*, 550–558.
31. Duffie, J.A.; Beckman, W.A. *Solar Engineering of Thermal Processes*; John Wiley & Sons: New Jersey, NJ, USA, 2013.
32. Lee, J.-S.; Yu, K.-H. Optimal path planning of solar-powered UAV using gravitational potential energy. *IEEE Trans. Aerosp. Electron. Syst.* **2017**, *53*, 1442–1451.
33. Sutoh, M.; Otsuki, M.; Wakabayashi, S.; Hoshino, T.; Hashimoto, T. The right path: Comprehensive path planning for lunar exploration rovers. *IEEE Robot. Autom. Mag.* **2015**, *22*, 22–33.
34. Ryu, B.-H.; Wang, C.-C.; Chang, I. Development and geotechnical engineering properties of KLS-1 lunar simulant. *J. Aerosp. Eng.* **2017**, *31*, 1–11.
35. Ryu, B.-H.; Baek, Y.; Kim, Y.-S.; Chang, I. Basic study for a Korean lunar simulant (KLS-1) development. *J. Korean Geotech. Soc.* **2015**, *31*, 53–63.
36. Jia, Z.; Smith, W.; Peng, H. Fast analytical models of wheeled locomotion in deformable terrain for mobile robots. *Robotica* **2012**, *31*, 35–53.
37. Hagedus, E. A simplified method for the determination of bulldozing resistance. *Land Locomot. Res. Lab. Army Tank Automo. Command Rep.* **1960**, *61*, 1–29.
38. Yoshida, K.; Wilcox, B. Space Robots and Systems. In *Handbook of Robotics*; Springer: Berlin/Heidelberg, Germany, 2008; pp. 1031–1063.
39. Ishigami, G.; Miwa, A.; Yoshida, K. Steering Trajectory Analysis of Planetary Exploration Rovers Based on All-Wheel Dynamics Model. In Proceedings of the Eighth International Symposium on Artificial Intelligence, Robotics and Automation in Space, Munich, Germany, 5–8 September 2005; pp. 121–128.
40. Inotsume, H.; Sutoh, M.; Nagaoka, K.; Nagatani, K.; Yoshida, K. Modeling, analysis, and control of an actively reconfigurable planetary rover for traversing slopes covered with loose soil. *J. Field Robot.* **2013**, *30*, 875–896.
41. Inotsume, H. Analysis of Angle of Attack for Efficient Slope Ascent by Rovers. Master’s Thesis, Carnegie Mellon University, Pittsburgh, PA, USA, 2015.
42. Kim, K.-J.; Guerra Padilla, G.E.; Sim, B.-S.; Yu, K.-H. Path planning and following based on the Fast Marching Method (FMM)/feedback control for lunar exploration rover on Digital Elevation Model (DEM). *J. Inst. Control Robot. Syst.* **2019**, *25*, 1109–1115.
43. Berkelman, P.; Easudes, J.; Martin, M.C.; Rollins, E.; Silberman, J. *Design of a Day/Night Lunar Rover. Technical Report, Robotics Institute Technical Report CMLI-TR-95-24*; Carnegie Mellon University: Pittsburgh, PA, USA, 1995.
44. Della Torre, A.; Finzi, A.E.; Genta, G.; Curti, F.; Schirone, L.; Capuiano, G.; Sacchetti, A.; Vukman, L.; Maillard, F.; Monchieri, E.; et al. AMALI mission lunar rover-the conceptual design of the team ITALIA rover, candidate for the Google Lunar X Prize Challenge. *Acta Astronaut.* **2010**, *67*, 961–978.
45. Yoshida, K.; Britton, N.; Walker, J. Development and field testing of MoonRaker: A four-wheel rover in minimal design. In Proceedings of the 12th International Symposium on Artificial Intelligence, Robotics and Automation in Space, Montreal, QC, Canada, 17–19 June 2013.
46. Pitre, R. Systematic Structural Optimization of a Next Generation Lunar Rover Chassis. Master’s Thesis, Queen’s University, Ontario, ON, Canada, 2015.
47. Zhao, J.; Huang, J.; Qiao, L.; Xiao, Z.; Huang, Q.; Wang, J.; He, Q.; Xiao, L. Geologic characteristics of the Chang’E-3 exploration region. *Sci. China Phys. Mech. Astron.* **2014**, *57*, 569–576.
48. Pin, F.G.; Oblow, E.M.; Horwedel, J.E.; Lucius, J.L. *ADGEN: An Automated Adjoint Code Generator for Large-Scale Sensitivity Analysis. No. CONF-871101-4*; Oak Ridge National Laboratory: Oak Ridge, TN, USA, 1987.
49. Cardile, D. System of Systems Conceptual Design Methodology for Space Exploration. Ph.D. Thesis, Politecnico di Torino, Torino, Italy, 2013.
50. Carletti, N. Planetary Rover Mobility on Loose Soil: Terramechanics Theory for Side Slip Prediction and Compensation. Master’s Thesis, Politecnico di Milano, Milano, MI, Italy, 2016.
51. Callina, A.; Krenn, R.; Schäfer, B. On the treatment of soft soil parameter uncertainties in planetary rover mobility simulations. J. Terramechanics 2016, 63, 33–47.
52. Gao, J.; Gao, H.; Ding, L.; Guo, T.; Deng, Z. Linear normal stress under a wheel in skid for wheeled mobile robots running on sandy terrain. J. Terramechanics 2017, 70, 49–57.
53. Irani, R.A.; Bauer, R.J.; Warkentin, A. Modeling a Single-Wheel Testbed for Planetary Rover Applications. In Proceedings of the ASME 2010 Dynamic Systems and Control Conference DSCC 2010, Cambridge, MA, USA, 12–15 September 2010; pp. 181–188.
54. Ishigami, G.; Kewlani, G.; Iagnemma, K. Statistical Mobility Prediction for Planetary Surface Exploration Rovers in Uncertain Terrain. In Proceedings of the IEEE International Conference on Robotics and Automation, Anchorage, AK, USA, 4–8 May 2010; pp. 588–593.
55. Kaveh-Moghadam, N. Experimental Investigation of Lunar Prototype Wheel Traction Performance on Deformable Terrain. Master’s Thesis, McGill University, Montreal, QC, Canada, 2010.
56. Kinugasa, T.; Kuwagi, K.; Leadbeater, T.W.; Gargiuli, J.; Parker, D.J.; Seville, J.P.; Yoshida, K.; Amano, H. Three-dimensional dynamic imaging of sand particles under wheel via gamma-ray camera system. J. Terramechanics 2015, 62, 5–17.
57. Michaud, S.; Schneider, A.; Bertrand, R.; Lamon, P.; Siegwart, R.; Winnendael, M.V.; Schiele, A. SOLERO: Solar-Powered Exploration Rover. In Proceedings of the 7th ESA Workshop on Advanced Space Technologies for Robotics and Automation (ASTRA), Noordwijk, Holland, 19–21 November 2002.
58. Moreland, S.; Skonieczny, K.; Inotsume, H.; Wettergreen, D. Soil Behavior of Wheels with Grousers for Planetary Rovers. In Proceedings of the Aerospace Conference, BigSky, MT, USA, 3–10 March 2012; pp. 1–8.
59. Otsuki, M.; Otsu, K.; Sugimura, S.; Oya, T.; Honda, T.; Murakami, R.; Kubota, T. Development of Planetary Exploration Rover with Advanced Mobility and Intelligence. In Proceedings of the International Symposium on Artificial Intelligence, Robotics and Automation in Space, Saint-Hubert, QC, Canada, 17–19 June 2014.
60. Skonieczny, K.; Moreland, S.J.; Wettergreen, D. A Grouser Spacing Equation for Determining Appropriate Geometry of Planetary Rover Wheels. In Proceedings of the IEEE/RSJ International Conference on IEEE, Intelligent Robots and Systems (IROS), Algarve, Portugal, 7–12 October 2012; pp. 5065–5070.
61. Smith, W.; Melanz, D.; Senatore, C.; Iagnemma, K.; Peng, H. Comparison of discrete element method and traditional modeling methods for steady-state wheel-terrain interaction of small vehicles. J. Terramechanics 2014, 56, 61–75.
62. Thüer, T. Mobility Evaluation of Wheeled All-Terrain Robots. Ph.D. Thesis, ETH Zurich, Zürich, Switzerland, 2009.
63. Xiao, W.; Zhang, Y. Design of manned lunar rover wheels and improvement in soil mechanics formulas for elastic wheels in consideration of deformation. J. Terramechanics 2016, 65, 61–71.
64. Yoshida, K.; Mizuno, N.; Ishigami, G.; Miwa, A. Terramechanics-Based Analysis for Slope Climbing Capability of a Lunar/Planetary Rover. In Proceedings of the 24th International Symposium on Space Technology and Science, Miyazaki, Japan, 30 May–6 June 2004.
65. Yoshikawa, H.; Oda, T.; Nonaka, K.; Sekiguchi, K. Modeling and Simulation of Wheel Driving Systems Based on Terramechanics for Planetary Exploration Rover Using Modelica. In Proceedings of the 12th International Modelica Conference, Prague, Czech Republic, 15–17 May 2017; pp. 901–907.
66. Zhou, F.; Arvidson, R.E.; Bennett, K.; Trease, B.; Lindemann, R.; Bellutta, P.; Iagnemma, K.; Senatore, C. Simulations of mars rover traverses. J. Field Robot. 2014, 31, 141–160.
67. Liu, H.; Lei, Y.; Fu, Y.; Li, X. Multi-objective optimization study of regenerative braking control strategy for range-extended electric vehicle. Appl. Sci. 2020, 10, 1789.
68. Deb, K.; Pratap, A.; Agarwal, S.; Meyarivan, T. A fast and elitist multiobjective genetic algorithm: NSGA-II. IEEE Trans. Evol. Comput. 2002, 6, 182–197.
69. Sutha, S.; Thyagarajan, T. Eigenstructure Assignment Based Multiobjective Dynamic State Feedback Controller Design for MIMO System Using NSGA-II. In Proceedings of the 2010 International Conference on Modelling, Identification and Control, Kayama, Japan, 17–19 July 2020; pp. 870–875.
70. Kallio, S.; Siroux, M. Energy analysis and exergy optimization of photovoltaic-thermal collector. *Energies* 2020, 13, 1–29.

71. Fang, L.; Qin, S.; Xu, G.; Li, T.; Zhu, K. Simultaneous optimization for hybrid electric vehicle parameters based on multi-objective genetic algorithms. *Energies* 2011, 4, 532–544.

72. Zeng, L.; Hu, J.; Pan, D.; Shao, X. Automated design optimization of a mono tiltrotor in hovering and cruising states. *Energies* 2020, 13, 1–20.

73. Jayakumar, P.; Melanz, D.; MacLennan, J.; Senatore, C.; Iagnemma, K. Stochastic Modeling and Uncertainty Cascade of Soil Bearing and Shearing Characteristics for Light-Weight Vehicle Applications. In Proceedings of the 7th Americas Regional Conference of the ISTVS 2013, Tampa, FL, USA, 15 September 2013.

74. Meirion-Griffith, G.; Spenko, M. A modified pressure-sinkage model for small, rigid wheels on deformable terrains. *J. Terramechanics* 2011, 48, 149–155.

75. Kim, K.-J.; Sim, B.-S.; Yu, K.-H. Stochastic Identification of Terrain Parameter for Lunar Exploration Rover Using a Bayesian Approach. In Proceedings of the 17th international conference on control, Automation and Systems, Jeju, Korea, 18–21 October 2017.

76. Zhang, R.; Pang, H.; Dong, W.; Li, T.; Liu, F.; Zhang, H.; Li, J. Three-dimensional discrete element method simulation system of the interaction between irregular structure wheel and lunar soil simulant. *Adv. Eng. 2020*, 148, 102873.

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